TRANSACTIONS

of the

American Foundrymen's Society

Proceedings of the

Fifty-Seventh Annual Meeting

Chicago, Ill.

May 4-8, 1953

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TABLE OF CONTENTS

	Page
Proceedings of the 57th Annual Meeting	vi
President's Annual Address	xii
Report of the Treasurer	xvi
Financial Statements	xvii
Report of the Secretary	xviii
Report on Safety & Hygiene and Air Pollution Activities	xx
Report of the Technical Director	xxi
Membership Report	xxiv
Minutes of Board Meetings	xxxvi
Tenth Chapter Officers Conference	xxxvii
Outstanding Opportunities for the Foundry Industry	1
A Survey of the Shell Molding Method of Casting Production	7
Efficient Methods of Production of Machine Tool Castings	29
American Experiences with Basic Cupola Melting	52
Factors Which Determine Riser Adequacy and Feeding Range	61
The Study of Vertical Gating	81
Twelfth Annual Report on Investigation of Properties of Steel Sands at Elevated Temperatures	96
Effects of Melting Furnace Atmosphere on Casting Properties and Annealability of Malleable Iron	107
Risering of Gray Iron Castings	131
Solidification of Cylinders	142
Veining Tendencies of Cores	150
Report of AFS Flowability Committee	156
Deformation of Green Molding Sand	159
Progress Report on Shell Molding Materials Testing Committee	166
The Influence of Molding Materials on the Incidence of Hot Tears	167
Casting Magnesium Alloys in Shell Molds	184
Development of Foundry Sand Reclamation	191
Reclamation of Sand by Pneumatic Dry Scrubbing	196
Dry Reclamation of Molding Sand for Steel Castings	200
A Wet Method of Sand Reclamation	206
Sand Reclamation in a Steel Foundry	211
Sand Reclamation with the Combination System	222
Graphitization in the Malleable Iron Industry	228
Performance of Cast and Rolled Steels in Relation to the Problem of Brittle Fracture	243
Conveyor Molding Standards	263
Mechanics of Foundry Mechanization and Improved Methods	272
Induced Flow Process of Baking Oil-Bonded Cores	279
Influence of Furnace Atmosphere on the Annealing of Malleable Iron	291
Feeding Range of Joined Sections	302
Factors Involved in Making a Sand Mixture	309
Magnesium Sand Casting Alloys Containing Thorium	315
Some Factors Affecting Spherical Type Macro-Gas Porosity in 85-5-5-5 Bronze	327
Agar Gels in Plaster-Bonded Investment	347
A Study of Segregation in Bronze Using a Radioactive Technique	352
Developing Standard Data Tables for Core Finishing	355
Resins and Sands for Shell Molding	367
Gun Placed Silica Cupola Lining	378
Trends For the Relation of Chill Test Depth and Carbon Equivalent of Gray Cast Irons	387
Trends in Dust Control—Past, Present and Future	394
Progress Made in Fluidity Testing of Molten Metals During the Last Ten Years	399
Mechanical Properties of Cast Titanium-Carbon Alloys	411
Gating to Control Pouring Rate and Its Effect on the Casting	416
Hardening Characteristics of Induction Heated Ductile Iron	422
A General Look at Non-Ferrous Molding Sands	432
Velocities and Volume Rates of Metal Flow in Gating Systems	439

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TABLE OF CONTENTS

(Continued from preceding page)

Page

Solidification at Corner and Core Positions 451
Use of Standards in Controlling Costs 457
Air Pollution Control Equipment for Melting Operations in the Foundry Industry 467
Correlation of Air Furnace Bottom Temperature to Refractory and Operating Practice in a Cupola-Air Furnace Duplex System 479
Ramming of Molding Sands 490
Magnesium Alloy Permanent Mold and Semi-Permanent Mold Castings 502
Prediction of Mechanical Properties from Chemical Composition for Fully-Annealed Ductile Cast Iron . 510
Notes on Air Furnace Design and Operation 516
Are Your Costs Reliable? 523
Acid Cupola Slags and Some Relationships with Melting Conditions 527
Evaluation of Mold Materials for Titanium Castings 540
Phosphorus Removal in Basic Cupola Operation 546
Properties of Graphitic Nickel Alloy Castings 552
Wage Incentive for Cost and Production Control for Indirect Operators 561
Positive Benefits of a Safety Program 570
Effect of Sand Flowability on Casting Surface Finish 574
Research in Pattern Coating Produces Quality Castings 580
A Technique for Casting Titanium 587
Advantages of Insulating an Acid Electric Furnace Roof 593
Patterns for Shell Molding 595
A Simplified Cost System for Small Jobbing Foundries 600
Practical Heat Transfer—An Interpretive Report 603
Practical Foundry Applications of Principles of Ratio Gating 623
Multiple Molding in a Malleable Shop 628
Construction and Operation of Coal-Fired Air Furnaces 634
Carbon as a Refractory 640
Fundamentals of Heat Treatment of Gray Cast Iron 643
Stress Relief Heat Treatment of Gray Cast Iron 646
Annealing Iron Castings for Machinability 651
Hardening Gray Cast Iron with Minimum Distortion 654
Flame Hardening of Cast Iron 657
Air Pollution and Public Relations 662
Effect of Casting "Fiber" on Mechanical Properties of Aluminum-4 Per Cent Copper Alloys . . . 664
Effects of Magnesium on Eutectic Modification and Graphitization of Certain Fe-C Alloys 674
Preliminary Survey of Some Metallurgical Bases for a Titanium Castings Industry 678
Fundamentals of Riser Behavior 686
Foundry Noise and Its Control 694
Engineering Aspects of Centrifugal Casting 701
Characteristics of High Strength Magnesium Casting Alloy ZK61 719
Pattern Equipment for Shell Molding 729
Trends in Cupola Dust Collecting Systems 731
Refractories for Indirect-Arc Electric Furnaces for Non-Ferrous Melting 733
Centrifugal Casting of Aluminum Alloys 744
Subject Index 763
Author Index 768

Officers

of the

American Foundrymen's Society

(incorporated)

(Fiscal Year Ending June 30, 1954)

**President,* Collins L. Carter, Albion Malleable Iron Co., Albion, Mich.

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G. Ewing Tait, Dominion Engineering Works, Ltd., Montreal, Quebec, Canada

Staff Officer

Secretary-Treasurer, Wm. W. Maloney

---

* Member, Executive Committee

SUMMARY OF PROCEEDINGS OF THE 57th ANNUAL MEETING

The 57th Annual Convention of the American Foundrymen's Society, a "paper convention," was held in Chicago, May 4-8, 1953 without exhibits. A total of 3,528 attended the 57th Annual Convention of AFS, the largest attendance on record for any non-exhibit year.

Credit for the excellent reception and hospitality received by those attending is given to the Chicago Chapter of AFS under the chairmanship of Fred B. Skeates, Link-Belt Co. This Committee excelled in its splendid work and hospitality as host to the thousands of members and guests attending the Convention.

During the 5-day Convention 41 technical sessions were held with 86 papers being presented. In addition, there were two Brass and Bronze Shop Course Sessions, three Gray Iron Shop Course Sessions and three Sand Shop Course Sessions.

A Symposium on Sand Reclamation was presented and sponsored jointly by the Sand, Steel and Gray Iron Divisions. A Round-Table Luncheon was sponsored by each Division. The Charles Edgar Hoyt Lecture was given by James H. Smith, General Manager, Central Foundry Division, General Motors Corp., Saginaw, Mich., who spoke on "Outstanding Opportunities for the Foundry Industry."

A summary of the sessions held follows:

Monday, May 4, 10:00 A.M.

BRASS AND BRONZE SESSION

Presiding—F. L. Riddell, H. Kramer & Co., Chicago.

Co-Chairman—J. G. Dick, Canadian Bronze Co., Ltd., Montreal.

Recorder—W. L. Rudin, Elesco Smelting Corp., Chicago.

"Properties of Graphitic Nickel Alloy Castings," J. T. Eash and G. L. Lee, International Nickel Co., Inc., Bayonne, N. J.

BRASS AND BRONZE RESEARCH IN PROGRESS:

"Mold Interface Reactions," W. B. Scott, National Bearing Div., American Brake Shoe Co., Meadville, Pa.

"Effect of Casting Defects on Physical Properties," B. N. Ames, New York Naval Shipyard, Brooklyn.

"Metal Penetration in Cores," R. B. Fischer, Ingersoll Rand Co., Phillipsburg, N. J.

"Manganese Bronze Test Bars," R. A. Colton, Federated Metals Div., American Smelting & Refining Co., Barber, N. J.

Monday, May 4, 10:00 A.M.

LIGHT METALS SESSION

Presiding—A. T. Peters, The Dow Chemical Co., Bay City, Mich.

Co-Chairman—W. D. Danks, Howard Foundry Co., Chicago.

Recorder—M. Chamberlin, The Dow Chemical Co., Bay City, Mich.

"Magnesium Sand Casting Alloys Containing Thorium," K. E. Nelson, The Dow Chemical Co., Midland, Mich.

"Characteristics of High Strength Magnesium Casting Alloy ZK61," J. W. Meier, Department of Mines and Technical Surveys, Ottawa, Canada.

Monday, May 4, 10:00 A.M.

MALLEABLE SESSION

Presiding—W. G. Ferrell, Auto Specialties Mfg. Co., St. Joseph, Mich.

Co-Chairman—L. E. Roby, Peoria Malleable Casting Co., Peoria, Ill.

"Mechanics of Foundry Mechanization," R. J. Anderson, Belle City Malleable Iron Co., Racine, Wis.

"Multiple Molding in a Malleable Shop," J. G. Kropka, Chain Belt Co., Milwaukee.

Monday, May 4, 12:00 Noon

LIGHT METALS ROUND TABLE LUNCHEON

Presiding—F. P. Strieter, The Dow Chemical Co., Midland, Mich.

"Casting Magnesium Alloys in Shell Molds," N. Sheptak, The Dow Chemical Co., Midland, Mich.

Subject: *"Shell Molding."*

Discussion Leaders: *Aluminum:* J. L. Schmieder, Jr., Oberdorfer Foundries, Inc., Syracuse, N. Y. *Magnesium:* A. J. Marotta, Utica Radiator Corp., Utica, N. Y.

Monday, May 4, 2:00 P.M.

BRASS AND BRONZE SESSION

Presiding—W. M. Ball, Jr., R. Lavin & Sons, Cincinnati.

Co-Chairman—F. L. Riddell, H. Kramer & Co,, Chicago.

"FRACTURE TEST SYMPOSIUM"

Panel—C. A. Robeck, Gibson & Kirk Co., Baltimore; H. H. Fairfield, Wm. Kennedy & Sons, Ltd., Owen Sound, Ont.; C. K. Faunt, Christensen & Olsen Foundry Co., Chicago; B. N. Ames, New York Naval Shipyard, Brooklyn.

Monday, May 4, 2:00 P.M.

EDUCATIONAL SESSION

Presiding—G. J. Barker, University of Wisconsin, Madison.

Co-Chairman—W. J. Hebard, Continental Foundry & Machine Co., East Chicago, Ind.

Subject: *"Job Training in the Foundry."*

Speakers—E. R. Andrews, Hyde Windlass Co., Bath, Maine; T. Miller, Great Lakes Foundries & Machine Corp., Ludington, Mich.

Monday, May 4, 2:00 P.M.

MALLEABLE SESSION

Presiding—C. O. Schopp, Link-Belt Co., Indianapolis, Ind.

Co-Chairman—N. N. Amrhein, Federal Malleable Co., West Allis, Wis.

"Air Furnace Design and Operation," J. E. Rehder, Canada Iron Foundries, Ltd., Montreal.

"Effect of Melting Furnace Atmosphere on Casting Properties and Annealability of Malleable Iron—Malleable Research Progress Report," E. A. Lange and R. W. Heine, University of Wisconsin, Madison.

Monday, May 4, 4:00 P.M.

LIGHT METALS SESSION

Presiding—W. A. Mader, Oberdorfer Foundries, Inc., Syracuse, N. Y.

Co-Chairman—D. L. LaVelle, American Smelting & Refining Co., Barber, N. J.

Recorder—J. L. Schmieder, Jr., Oberdorfer Foundries, Inc., Syracuse, N. Y.

"Magnesium Alloy Permanent Mold and Semi-Permanent Mold Castings," M. E. Gantz, Jr., E. M. Gingerich and R. T. Woods, Aluminum Company of America, Cleveland.

"Agar Gels in Plaster-Bonded Investment," H. Rosenthal and S. Lipson, Frankford Arsenal, Philadelphia.

"Effect of Casting Fiber on Mechanical Properties of Aluminum-4% Copper Alloys," W. D. Walther, C. M. Adams and H. F. Taylor, Massachusetts Institute of Technology, Cambridge, Mass.

Monday, May 4, 4:00 P.M.

MALLEABLE SESSION

Presiding—W. D. McMillan, International Harvester Co., Chicago.

Co-Chairman—J. Dvorak, Eberhardt Mfg. Div., Eastern Malleable Iron Co., Cleveland.

"Influence of Furnace Atmosphere on the Annealing of Malleable Iron," J. T. Bryce, A. Hernandez and F. B. Rote, Albion Malleable Iron Co., Albion, Mich.

"Gating to Control Pouring Rate and Its Effect on Castings," F. J. McDonald, Central Foundry Div., General Motors Corp., Saginaw, Mich.

Monday, May 4, 4:00 P.M.

SAND SESSION

Presiding—B. H. Booth, Carpenter Brothers, Inc., Milwaukee.

Co-Chairman—H. W. Dietert, Harry W. Dietert Co., Detroit.

Recorder—F. P. Goettman, Standard Sand Co., Grand Haven, Mich.

"A General Look at Nonferrous Molding Sands," C. A. Robeck, Gibson & Kirk Co., Baltimore, and C. A. Sanders, American Colloid Co., Chicago.

"Factors Involved in Making a Sand Mixture," D. C. Williams, and D. F. Baker, Ohio State University, Columbus.

"Deformation of Green Molding Sand," Committee Report, W. G. Parker, General Electric Co., Elmira Foundry, Elmira, N. Y.

Monday, May 4, 8:00 P.M.

BRASS AND BRONZE SHOP COURSE

Presiding—R. J. Keeley, Ajax Metal Div., H. Kramer & Co., Philadelphia.

Co-Chairman—H. M. St. John, Crane Co., Chicago.

Subject: *"Furnace Operation and Practice."*

"Indirect Arc and Resistance Furnaces," B. W. Schafer, Detroit Electric Furnace Div., Kuhlman Electric Co., Bay City, Mich.

"Low and High Frequency Induction Furnaces," F. T. Chestnut, Ajax Electrothermic Corp., Ajax Park, N. J.

"Coal and Coke Fired Furnaces," R. H. Stone, Vesuvius Crucible Co., Swissvale, Pittsburgh.

Monday, May 4, 8:00 P.M.

GRAY IRON SHOP COURSE

Presiding—W. Holden, Eaton Mfg. Co., Vassar, Mich.

Co-Chairman—E. J. Burke, Hanna Furnace Corp., Buffalo.

Subject: *"Melting Conditions in the Cupola,"* D. E. Krause, Gray Iron Research Institute, Columbus.

Monday, May 4, 8:00 P.M.

SAND SHOP COURSE

Presiding—F. S. Brewster, Harry W. Dietert Co., Detroit.

Co-Chairman—K. L. Landgrebe, The Wheland Co., Chattanooga.

Subject: *"Pinholes in Castings,"* Ray Cochran, R. Lavin & Sons Co., Chicago; L. E. Emery, Marion Malleable Co., Marion, Ind., and G. R. Gardner, Aluminum Company of America, Cleveland.

Tuesday, May 5, 10:00 A.M.

HEAT TRANSFER SESSION

Presiding—C. E. Sims, Battelle Memorial Institute, Columbus, Ohio.

Co-Chairman—J. B. Caine, Foundry Consultant, Cincinnati.

"Practical Heat Transfer—An Interpretive Report," AFS Heat Transfer Research, W. S. Pellini, Naval Research Laboratory, Washington, D. C.

Tuesday, May 5, 10:00 A.M.

LIGHT METALS SESSION

Presiding—T. D. Stay, Reynolds Metals Co., Cleveland.

Co-Chairman—P. J. Petto, Jr., Arrow Aluminum Castings Co., Cleveland.

Recorder—P. J. Petto, Jr.

"Practical Foundry Applications of Principles of Ratio Gating," L. L. Lucas, The Wagner Manufacturing Co., Sidney, Ohio.

"Velocities and Volume Rates of Metal Flow in Gating Systems," W. H. Johnson, H. F. Bishop and W. S. Pellini, Naval Research Laboratory, Washington, D. C.

"A Study of Vertical Gating—Light Metals Research Progress Report," J. H. Jackson, J. G. Kura and K. Grube, Battelle Memorial Institute, Columbus, Ohio.

Tuesday, May 5, 10:00 A.M.

BRASS AND BRONZE SESSION

Presiding—W. B. Scott, National Bearing Div., American Brake Shoe Co., Meadville, Pa.

Co-Chairman—R. A. Colton, Federated Metals Div., American Smelting & Refining Co., Barber, N. J.

Recorder—J. F. Klement, Ampco Metal, Inc., Milwaukee.

"A Study in Segregation in Bronze Using a Radioactive Technique," W. C. Winegard, University of Toronto, Toronto, Canada.

"Some Factors Affecting Spherical Type Macro Gas Porosity in 85-5-5-5 Bronze," R. B. Fischer, Ingersoll-Rand Co., Phillipsburg, N. J.

Tuesday, May 5, 10:00 A.M.

MALLEABLE SESSION

Presiding—C. F. Semrau, Illinois Malleable Castings Co., Chicago.

Co-Chairman—F. A. Czapski, Chicago Malleable Castings Co., Chicago.

"Graphitization in the Malleable Iron Industry," H. A. Schwartz, W. K. Bock and J. D. Hedberg, National Malleable & Steel Castings Co., Cleveland.

Tuesday, May 5, 10:00 A.M.

PATTERN SESSION

Presiding—V. C. Reid, City Pattern Foundry & Machine Co., Detroit.

Co-Chairman—H. J. Flaaten, Midwest Pattern Co., Minneapolis.

"Patterns for Shell Molding," R. Olson, Production Foundry & Pattern Co., Chicopee, Mass.

"Pattern Equipment for Shell Molding," O. C. Bueg, Arrow Pattern and Engineering Co., Erie, Pa.

Tuesday, May 5, 12:00 Noon

BRASS AND BRONZE ROUND TABLE LUNCHEON

Presiding—B. A. Miller, Baldwin-Lima-Hamilton Corp., Philadelphia.

Subject: *"Better Understanding—Better Castings."*

"A Brief Study of Fluid Flow in a Small Cylindrical Casting," Motion Picture, courtesy of Brass and Bronze Ingot Institute. Presented by J. G. Kura, Battelle Memorial Institute, Columbus.

Tuesday, May 5, 12:00 Noon

MALLEABLE ROUND TABLE LUNCHEON

Presiding—R. P. Anderson, Belle City Malleable Iron Co., Racine, Wis.

Co-Chairman—W. A. Kennedy, Grinnell Co., Inc., Providence, R. I.

"How Far Should a Malleable Foundry Go in Sand Control," E. E. Woodliff, Foundry Sand Service Engineering Co., Detroit.

Tuesday, May 5, 2:00 P.M.

EDUCATIONAL SESSION

Presiding—E. M. Strick, Erie Malleable Iron Co., Erie, Pa.

Co-Chairman—R. W. Schroeder, University of Illinois, Chicago.

Subject: *"The Foundry Industry and Formal Education."*

Discussion Leaders: B. D. Claffey, Acme Aluminum Alloys, Inc., Dayton, Ohio; F. G. Sefing, International Nickel Co., Inc., New York, N. Y.

Tuesday, May 5, 2:00 P.M.

HEAT TRANSFER SESSION

Presiding—E. C. Troy, Foundry Engineer, Palmyra, N. J.

Co-Chairman—W. S. Pellini, Naval Research Laboratory, Washington, D. C.

"Solidification of Cylinders—Heat Transfer Research Progress Report," V. Paschkis, Columbia University, New York.

"Solidification at Corner and Core Positions," F. A. Brandt, H. F. Bishop and W. S. Pellini, Naval Research Laboratory, Washington, D. C.

Tuesday, May 5, 2:00 P.M.

LIGHT METALS SESSION

Presiding—J. H. Jackson, Battelle Memorial Institute, Columbus.

Co-Chairman—H. Rosenthal, Frankford Arsenal, Philadelphia.

Recorder—M. J. Berger, Armour Research Foundation, Chicago.

"A Technique for Casting Titanium," O. W. Simmons and H. R. McCurdy, Frankford Arsenal, Philadelphia.

"Mechanical Properties of Cast Titanium-Carbon Alloys," O. W. Simmons and R. E. Edelman, Frankford Arsenal, Philadelphia.

Tuesday, May 5, 2:00 P.M.

SAND SESSION

Presiding—F. Carl, Fabricast Div., General Motors Corp, Bedford, Ind.

Co-Chairman—G. P. Phillips, International Harvester Co., Chicago.

Recorder—V. C. Reid, City Pattern Foundry & Machine Co., Detroit.

"Resins and Sands for Shell Molding," M. F. Drumm, Monsanto Chemical Co., Springfield, Mass.

"Progress Report, Shell Molding Materials Testing," G. A. Conger, Cambria Foundry & Engineering Div., Stevens Mfg. Co., Ebensburg, Pa.

Tuesday, May 5, 4:00 P.M.

LIGHT METALS SESSION

Presiding—W. A. Dean, Aluminum Co. of America, Cleveland.

Co-Chairman—R. A. Lubker, Armour Research Foundation, Chicago.

Recorder—P. D. Frost, Battelle Memorial Institute, Columbus.

"Evaluation of Mold Materials for Titanium Castings," R. M. Lang, J. G. Kura and J. H. Jackson, Battelle Memorial Institute, Columbus.

"Preliminary Survey of Some Metallurgical Bases for a Titanium Castings Industry," R. F. Malone, H. T. Clark, and W. L. Finlay, Rem-Cru Titanium, Inc., Midland, Pa.; and S. V. Arnold, Watertown Arsenal, Watertown, Mass.

Tuesday, May 5, 4:00 P.M.

PATTERN SESSION

Presiding—E. T. Kindt, Kindt-Collins Co., Cleveland.

Co-Chairman—J. W. Costello, American Hoist & Derrick Co., St. Paul.

"What Research Offers in a Pattern Coating," C. J. Berg, The Sherwin-Williams Co., Chicago.

Tuesday, May 5, 4:00 P.M.

SAFETY, HYGIENE AND AIR POLLUTION SESSION

Presiding—F. A. Patty, General Motors Corp., Detroit.

Co-Chairman—J. M. Kane, American Air Filter Co., Louisville.

Recorder—W. N. Davis, American Foundrymen's Society, Chicago.

"What Are Future Air Pollution Prospects for the Foundry Industry?" J. M. Kane, American Air Filter Co., Louisville.

"Know Your Problem for Your Community," R. T. Greibling, Air Pollution Control Assn., Pittsburgh.

"Air Pollution Control Equipment Available for the Foundry Industry," R. T. Pring, American Wheelabrator & Equipment Corp., Mishawaka, Ind.

Tuesday, May 5, 7:00 P.M.

CANADIAN DINNER

Presiding—J. J. McFayden, Vice-President, Galt Malleable Iron Co., Galt, Ont.

Tuesday, May 5, 7:00 P.M.

EDUCATIONAL DINNER

Presiding—Collins L. Carter, President, Albion Malleable Iron Co., Albion, Mich.

"A Chapter Educational Activity Program," H. E. Gravlin, Jr., Ford Motor Co., Dearborn.

Tuesday, May 5, 8:00 P.M.

BRASS AND BRONZE SHOP COURSE

Presiding—R. J. Keeley, Ajax Metal Div., H. Kramer & Co., Philadelphia.

Co-Chairman—H. M. St. John, Crane Co., Chicago.

Subject: *"Furnace Operation and Practice."*

"Gas and Oil Fired Furnaces," J. L. Stroman, Stroman Furnace & Engineering Div., The Petersen Oven Co., Franklin Park, Ill.

"Furnace Practices," M. G. Deitl, Schaible Foundry & Brass Co., Cincinnati.

Tuesday, May 5, 8:00 P.M.

GRAY IRON SHOP COURSE

Presiding—H. H. Wilder, Vanadium Corp. of America, Detroit.

Co-Chairman—E. J. Burke, Hanna Furnace Corp., Buffalo.

"Foundry Calculations and Essential Records," K. H. Priestley, Vassar Electroloy Products, Inc., Vassar, Mich.

Tuesday, May 5, 8:00 P.M.

SAND SHOP COURSE

Presiding—H. Brown, Solar Aircraft Co., Des Moines, Iowa.

Co-Chairman—R. H. Olmsted, Whitehead Bros. Co., Conneaut, Ohio.

Subject: *"Evaluation of Molding Processes."*

Introduction: H. Brown, Solar Aircraft Co., Des Moines, Iowa.

Green Sand Molding: C. E. Maddick, Massey-Harris, Ltd., Brantford, Ont.

Permanent Mold or Die Casting: W. B. Scott, American Brake Shoe Co., Meadville, Pa.

Dry Sand Molding: G. W. Anselman, Beloit Foundry Co., So. Beloit, Ill.

Shell Molding: J. B. Stazinski, General Electric Co., Lynn, Mass.

Core Sand Molding: G. P. Antonic, Motor Castings Co., Milwaukee.

Wednesday, May 6, 10:00 A.M.

GRAY IRON SESSION

Presiding—J. S. Vanick, International Nickel Co., Inc., New York.

Co-Chairman—F. J. Dost, Sterling Foundry Co., Wellington, Ohio.

Recorder—C. A. Johnson, Armour Research Foundation, Chicago.

"Risering of Gray Iron Castings—Progress Report on Gray Iron Research," W. A. Schmidt and H. F. Taylor, Massachusetts Institute of Technology, Cambridge.

"Efficient Methods of Production of Machine Tool Castings," Official Exchange Paper from Institute of British Foundrymen—G. W. Nicholls, Modern Foundries, Ltd., Halifax, England.

Wednesday, May 6, 10:00 A.M.

SAFETY, HYGIENE AND AIR POLLUTION SESSION

Presiding—J. G. Liskow, American Air Filter Co., Louisville.

Co-Chairman—W. W. Dodge, Caterpillar Tractor Co., Peoria.

Recorder—W. N. Davis, American Foundrymen's Society, Chicago.

"Progress Report—AFS Safety, Hygiene and Air Pollution Program," J. R. Allan, International Harvester Co., Chicago.

"Noise in the Foundry," H. T. Walworth, Lumbermen's Mutual Casualty Co., Chicago.

Wednesday, May 6, 10:00 A.M.

SYMPOSIUM ON SAND RECLAMATION

Presiding—J. A. Rassenfoss, American Steel Foundries, E. Chicago, Ind.

Co-Chairman—R. H. Jacoby, The Key Co., East St. Louis, Ill.

Recorder—J. B. Caine, Foundry Consultant, Cincinnati.

"Reclamation of Sand by Pneumatic Dry Scrubbing," H. W. Meyer, General Steel Castings Corp., Granite City, Ill.

"Development of Foundry Sand Reclamation," C. E. Wenninger, National Engineering Co., Chicago.

"Dry Reclamation of Molding Sand for Steel Castings," J. A. Cannon, Duncan Foundry & Machine Works, Inc., Alton, Ill.

Wednesday, May 6, 12:00 Noon

PATTERN ROUND TABLE LUNCHEON

Presiding—A. F. Pfeiffer, Allis-Chalmers Mfg. Co., Milwaukee.

Co-Chairman—H. K. Swanson, Swanson Pattern & Model Works, E. Chicago, Ind.

"Pattern Problems—Questions and Answers."

Wednesday, May 6, 2:00 P.M.

SYMPOSIUM ON SAND RECLAMATION

Presiding—T. W. Curry, Lynchburg Foundry Co., Lynchburg, Va.

Co-Chairman—G. M. Etherington, American Brake Shoe Co., Mahwah, N. J.

Recorder—R. W. Bennett, Walter Gerlinger, Inc., Milwaukee.

"A Wet Method of Sand Reclamation," R. H. Shurmer and P. C. Will, The Hydro-Blast Corp., Chicago.

"Sand Reclamation in a Steel Foundry," H. H. Johnson, R. Y. McCleery and G. A. Fisher, National Malleable and Steel Castings Co., Sharon, Pa.

"Sand Reclamation with the Combination System," G. H. Curtis, Nichols Engineering & Research Corp., New York.

Wednesday, May 6, 2:00 P.M.

SAFETY, HYGIENE AND AIR POLLUTION SESSION

Presiding—J. G. Liskow, American Air Filter Co., Louisville.

Co-Chairman—W. W. Dodge, Caterpillar Tractor Co., Peoria, Ill.

Recorder—W. N. Davis, American Foundrymen's Society, Chicago.

Subject: *"Trends of Dust Control—Past, Present and Future."*

Discussion Leaders: K. J. Caplan, Consultant, St. Louis; K. M. Smith, Caterpillar Tractor Co., Peoria; A. G. Granath, National Engineering Co., Chicago.

Wednesday, May 6, 4:00 P.M.

GRAY IRON SESSION

Presiding—H. A. Deane, Campbell, Wyant & Cannon Foundry Co., Muskegon, Mich.

Co-Chairman—G. L. Richter, Farrel-Birmingham Co., Ansonia, Conn.

Recorder—W. M. Spear, Worthington Corp., Harrison, N. J.

"Dephosphorization of Iron—Basic Cupola Operation," J. E. Bolt, Lt. USNR, Bureau of Ships, Washington, D. C.

"Acid Cupola Slags and Some Relationships with Melting Conditions," S. F. Carter and Ralph Carlson, American Cast Iron Pipe Co., Birmingham, Ala.

Wednesday, May 6, 4:00 P.M.

REFRACTORIES SESSION

Presiding—C. H. Wyman, Burnside Steel Foundry Co., Chicago.

Co-Chairman—R. A. Witschey, A. P. Green Fire Brick Co., Chicago.

"Insulating an Electric Furnace Roof," C. C. Spencer, Electric Steel Castings Co., Indianapolis.

"Correlation of Air Furnace Bottom Temperature to Refractory and Operating Practice in a Cupola-Air Furnace Duplex System," F. W. Jacobs and J. S. Lawrence, Jr., Texas Foundries, Inc., Lufkin, Texas.

"Refractories for Indirect Arc Electric Furnaces for Non-Ferrous Melting," M. G. Dietl, Schaible Foundry & Brass Co., Cincinnati.

Wednesday, May 6, 4:00 P.M.

TIMESTUDY AND METHODS SESSION

Presiding—Dean Van Order, Burnside Steel Foundry Co., Chicago.

Co-Chairman—J. J. Farkas, Cincinnati Milling Machine Co., Cincinnati.

"Wage Incenitve for Cost and Production Control for Indirect Operators," C. J. Pruet, McWane Cast Iron Pipe Co., Birmingham, Ala.

Wednesday, May 6, 7:00 P.M.

ANNUAL BANQUET

Presiding—I. R. Wagner, President, AFS.

The Annual AFS Banquet of the Society's 57th Annual Convention was called to order by President I. R. Wagner. Following singing of the National Anthem by those in attendance, President Wagner introduced Past President Fred J. Walls, Chairman of the AFS Board of Awards who presented Honorary Life Memberships and AFS Gold Medal Awards as follows:

HONORARY LIFE MEMBERSHIPS IN AFS

Awarded to George John Barker, Professor of Mining and Metallurgy at University of Wisconsin, *"For outstanding contributions to the Society and the Castings Industry in the education of young engineers for greater appreciation of the industry, the casting process and cast products as engineering materials."*

Awarded to I. Richards Wagner, Electric Steel Castings Co., Indianapolis, Ind., *"In recognition of his services to the American Foundrymen's Society as its President, 1952-53."*

THE WM. H. MCFADDEN GOLD MEDAL

Awarded to William John Grede, President, Grede Foundries, Inc., Milwaukee *"For outstanding public service bringing great credit upon and broad recognition for the entire Foundry Industry."*

THE JOHN H. WHITING GOLD MEDAL

Awarded to Daniel E. Krause, Executive Director, Gray Iron Research Institute *"For outstanding contributions to the Society and the Foundry Industry in the field of Ferrous metallurgy and research."*

THE JOSEPH S. SEAMAN GOLD MEDAL

Awarded to William Romanoff, H. Kramer & Co., Chicago *"For exceptional contributions to the Society and its Brass and Bronze Division over many years."*

THE PETER L. SIMPSON GOLD MEDAL

Awarded to James H. Smith, General Manager, Central Foundry Division, General Motors Corp., Saginaw, Michigan *"For outstanding contributions to the application of engineering principles in foundry practice, and as the 1953 Charles Edgar Hoyt Annual Lecturer."*

After presentation of Awards, President Wagner introduced the speaker of the evening.

Speaker: Dr. George S. Benson, President, Harding College, Searcy, Arkansas—*"Education for Free Enterprise."*

Thursday, May 7, 8:00 A.M.

PAST PRESIDENTS' BREAKFAST

Presiding—Walton L. Woody, Past President.

Thursday, May 7, 10:00 A.M.

A.F.S. ANNUAL BUSINESS MEETING

Presiding—AFS National President, I. R. Wagner.

President Wagner called the meeting to order as the Annual Business meeting of the American Foundrymen's Society. President Wagner then presented the President's Annual Address. See page xii.

Following this presentation President Wagner called on Secretary-Treasurer Wm. W. Maloney who announced the 1953 AFS Apprentice Contest winners as follows:

Wood Patternmaking Division
1st—Richard Sautel, Universal Pattern Works, Rockford, Ill.
2nd—William Ryerson, Guilford Pattern Works, Rockford, Ill.
3rd—Wm. M. Zimmer, Jr., Arnette Pattern Co., Granite City, Ill.

Metal Patternmaking Division
1st—George M. Smith, Automotive Pattern Co., Detroit
2nd—Philip D. McDonald, Caterpillar Tractor Co., Peoria
3rd—Walter Lewandowski, Ford Motor Co., Dearborn, Mich.

Gray Iron Molding Division
1st—Wm. E. Morehead, Caterpillar Tractor Co., Peoria
2nd—John J. Diaz, Brown & Sharpe Mfg. Co., Providence, R. I.
3rd—Davey Gambcorta, Olney Foundry Div., Link-Belt Co., Philadelphia

Steel Molding Division
1st—Robert J. Luckenbill, Dodge Steel Co., Philadelphia
2nd—Marcel Lalonde, Canadian Car & Foundry Co., Ltd., Montreal, Canada.
3rd—Ghislain Lefrancois, Canadian Car & Foundry Co., Ltd., Montreal, Canada.

Non-Ferrous Molding Division
1st—Joseph LoPatriello, Airesearch Mfg. Co., Los Angeles
2nd—Carlo Gonzales, Airesearch Mfg. Co., Los Angeles
3rd—Gaston Vigneault, Miller's Brass Foundry, Reg'd., Three Rivers, Que., Canada.

The Society arranged to have the five first-prize winners present at the Convention and to receive their awards in person. The first-prize winners were called to the platform and President Wagner presented each with the first prize, a check for $100.00 and an engraved Certificate of Award prefaced by a few words of commendation and encouragement.

President Wagner then called on Secretary-Treasurer Maloney who reported on the nomination of Officers and Directors for the coming year and stated that no additional nominees had been received in accordance with the procedure prescribed in Art. X of the Society By-Laws. He therefore cast the unanimous ballot of the membership of AFS for the election of the following:

President (to serve one year):
Carter L. Collins, Albion Malleable Iron Co., Albion, Mich.

Vice-President (to serve one year):
Frank J. Dost, Sterling Foundry Co., Wellington, Ohio.

Directors (to serve three years):
E. C. Hoenicke, Eaton Manufacturing Co., Detroit, Mich.
M. J. Lefler, Oliver Corp., South Bend, Ind.
C. V. Nass, Beardsley & Piper Div., Pettibone Mulliken Corp., Chicago, Ill.
V. F. Stine, Pangborn Corp., Hagerstown, Md.
G. Ewing Tait, Dominion Engineering Works, Ltd., Montreal, Quebec, Canada.

Director (to serve one year):
I. R. Wagner, Electric Steel Castings Co., Indianapolis, Ind.

President Wagner next introduced James H. Smith who presented the Charles Edgar Hoyt Annual Lecture.

Thursday, May 7, 11:00 A.M.

CHARLES EDGAR HOYT ANNUAL LECTURE

"Outstanding Opportunities for the Foundry Industry," James H. Smith, General Manager, Central Foundry Div., General Motors Corporation, Saginaw, Mich.

Thursday, May 7, 12:00 Noon

GRAY IRON ROUND TABLE LUNCHEON

Presiding—W. W. Levi, Lynchburg Foundry Co., Radford, Va.

"Casting Defects," W. A. Hambley, Chas. A. Krause Milling Co., Birmingham, Mich.

Thursday, May 7, 12:00 Noon

STEEL ROUND TABLE LUNCHEON

Presiding—J. B. Caine, Fdry. Consultant, Cincinnati.

Co-Chairman—V. E. Zang, Unitcast Corporation, Toledo, Ohio.

Subjects: *"Hot Tear Investigation—AFS Steel Research Progress Report,"* C. H. Wyman, Burnside Steel Foundry Co., Chicago.

"Effect of Casting Defects on Service Performance."

"What Are Your Troubles?—Let's Discuss Them."

Thursday, May 7, 2:00 P.M.

PLANT AND PLANT EQUIPMENT SESSION

Presiding—J. Thomson, Continental Foundry & Machine Co., East Chicago, Ind.

Co-Chairman—H. W. Johnson, Wells Manufacturing Co., Skokie, Ill.

"Core Making Machines," L. B. Koenig, J. I. Case Co., Racine.

"Mechanization in Core Making," H. C. Weimer, Beardsley & Piper Co., Chicago. (Motion Picture.)

Thursday, May 7, 2:00 P.M.

FOUNDRY COST SESSION

Presiding—R. L. Lee, Grede Foundries, Inc., Milwaukee.

Co-Chairman—C. E. Westover, Westover Engineers, Milwaukee.

"Use of Standards in Controlling Costs," William Busby, Texas Foundries, Inc., Lufkin, Texas.

"Are Your Costs Reliable?" R. T. Lewis, Keen Foundry Co., Griffith, Ind.

"A Cost System for Small Jobbing Foundries," R. W. McDonnell, Letson & Burpee, Ltd., Vancouver, B. C.

Thursday, May 7, 2:00 P.M.

REFRACTORIES SESSION

Presiding—W. R. Jaeschke, The Whiting Corp., Harvey, Ill.

Co-Chairman—R. P. Schauss, Werner G. Smith, Inc., Chicago.

"Gun Placed Silica Cupola Linings," T. E. Barlow and P. D. Humont, International Minerals & Chemical Corp., Chicago.

"Effects of Operating Practice on Refractory Life in the Periodic Air Furnace," C. F. Semrau, Illinois Malleable Iron Co., Chicago.

"Carbon as a Refractory," J. V. Nolan and T. J. Wilde, National Carbon Co., New York.

Thursday, May 7, 2:00 P.M.

SAND SESSION

Presiding—J. B. Caine, Foundry Consultant, Cincinnati.

Co-Chairman—W. S. Pellini, Naval Research Laboratory, Washington, D. C.

Recorder—P. E. Kyle, Cornell University, Ithaca, N. Y.

"Ramming of Molding Sands," H. W. Dietert and A. L. Graham, Harry W. Dietert Co., Detroit.

"Casting Surface Finish and Flowability of Sand," H. H. Fairfield and James McConachie, William Kennedy & Sons, Ltd., Owen Sound, Ont.

"Report of Flowability Committee," J. B. Caine, Foundry Consultant, Cincinnati.

Thursday, May 7, 4:00 P.M.

GRAY IRON SESSION

Presiding—D. E. Krause, Gray Iron Research Institute, Columbus.

Co-Chairman—F. T. McGuire, Deere & Co., Moline, Ill.

Recorder—D. S. Eppelsheimer, Missouri School of Mines, Rolla, Mo.

"Trends for the Relation of Chill Test Depth and Carbon Equivalent of Gray Cast Irons," E. A. Loria, The Carborundum Co., Niagara Falls, N. Y.

"Hardening Characteristics of Induction Heated Ductile Iron," E. P. Rowady, General Electric Co., Fort Wayne, Ind.; W. J. Murphy and J. F. Libsch, Lehigh University, Bethlehem, Pa.

Thursday, May 7, 4:00 P.M.

SAFETY, HYGIENE AND AIR POLLUTION SESSION

Presiding—J. W. Young, International Harvester Co., Chicago.

Co-Chairman—M. F. Biancardi, Allis-Chalmers Mfg. Co., Milwaukee.

Recorder—W. N. Davis, American Foundrymen's Society, Chicago.

"Positive Benefits of a Safety Program," F. W. Shipley, Caterpillar Tractor Co., Peoria.

Question and Answer Panel—AFS Safety Committee.

Thursday, May 7, 4:00 P.M.

STEEL SESSION

Presiding—C. B. Jenni, General Steel Castings Co., Eddystone, Pa.

Co-Chairman—J. R. Goldsmith, Crane Co., Chicago.

Recorder—W. R. Punko, Wehr Steel Co., Milwaukee.

"The Influence of Molding Materials on the Incidence of Hot Tearing," J. M. Middleton, British Steel Castings Research Association, Sheffield, England. Paper presented by C. W. Briggs, Steel Founders' Society of America, Cleveland.

"Progress Made in Fluidity Testing of Molten Metal During the Last Ten Years," A. I. Krynitsky (Retired), National Bureau of Standards, Washington, D. C. Paper presented by J. H. Schaum, National Bureau of Standards, Washington, D. C.

Thursday, May 7, 4:00 P.M.

TIMESTUDY AND METHODS SESSION

Presiding—J. A. Westover, Westover Engineers, Milwaukee.

Co-Chairman—H. R. Williams, Williams Management Engineering, Milwaukee.

"Developing Standard Data Tables for Core Finishing," L. L. Martin, Sterling Foundry Co., Wellington, Ohio.

"Conveyor Molding Standards," E. C. Reid, Ford Motor Co. of Canada, Ltd., Windsor, Ont.

Thursday, May 7, 7:00 P.M.

A.F.S. ALUMNI DINNER

Presiding—Walter L. Seelbach, Superior Foundry, Inc., Cleveland.

Speaker: "Israel and Its Industry," H. Bornstein, Deere & Co., Moline, Ill.

Thursday, May 7, 8:00 P.M.

GRAY IRON SHOP COURSE

Presiding—W. W. Levi, Lynchburg Foundry Co., Radford, Va.

Co-Chairman—L. L. Clark, Armour Research Foundation, Chicago.

Subject: *"Practical Aspects of Carbon Control in the Cupola,"* A. J. Dublo, The Sterling Foundry Co., Wellington, Ohio.

Thursday, May 7, 8:00 P.M.

SAND SHOP COURSE

Presiding—R. H. Jacoby, The Key Co., East St. Louis, Ill.

Co-Chairman—F. E. Kurtz, Electric Steel Castings Co., Indianapolis.

Subject: *"Hot Properties of Molding Sands and Their Relation to Casting Defects."*

Steel: H. W. Meyer, General Steel Castings Corp., Granite City, Ill.

Malleable: B. C. Yearley, National Malleable & Steel Castings Co., Cicero, Ill.

Friday, May 8, 10:00 A.M.

GRAY IRON SESSION

Presiding—G. A. Timmons, Climax Molybdenum Co., Detroit.

Co-Chairman—R. A. Clark, Electro Metallurgical Div., Union Carbide & Carbon Co., Detroit.

Recorder—T. Giszczak, Central Foundry Div., GMC, Defiance, Ohio.

"Panel Discussion on Heat Treatment of Gray Iron for the Practical Foundryman."

"The Fundamentals of Heat Treatment of Gray Cast Iron," J. E. LaBelle, Detroit Diesel Engine Div., GMC, Detroit.

"Stress Relieving Gray Iron," J. H. Schaum, National Bureau of Standards, Washington, D. C.

"Annealing Iron Castings for Machinability," A. W. Demmler, Campbell, Wyant & Cannon Foundry Co., Muskegon, Mich.

"Through Hardening of Gray Iron," A. A. Armstrong, Thompson Products, Inc., Cleveland.

"Flame Hardening Cast Iron," M. R. Scott, Detroit Flame Hardening Co., Detroit.

Friday, May 8, 10:00 A.M.

SAND SESSION

Presiding—E. C. Zirzow, Werner G. Smith, Inc., Cleveland.

Co-Chairman—H. W. Meyer, General Steel Castings Corp., Granite City, Ill.

Recorder—C. C. Sigerfoos, Michigan State College, East Lansing, Mich.

"Induced Flow Process of Baking Oil-Bonded Cores," C. T. Marek, Purdue University, Lafayette, Ind., and R. J. Wimmert, Air Reduction Corp., Union, N. J.

"Veining Tendencies of Cores—Committee 8-J Report," H. H. Fairfield, William Kennedy & Sons, Ltd., Owen Sound, Ont.

"Sand Research—Progress Report," H. Robinson, Cornell University, Ithaca, N. Y.

Friday, May 8, 10:00 A.M.

STEEL SESSION

Presiding—A. H. Suckow, Symington-Gould Corp., Depew, N. Y.

Co-Chairman—K. Fritz, Bucyrus-Erie Co., So. Milwaukee.

Recorder—C. C. Reynolds, Massachusetts Institute of Technology, Cambridge, Mass.

"Feeding Range of Joined Sections," E. T. Myskowski, H. F. Bishop and W. S. Pellini, Naval Research Laboratory, Washington, D. C.

"Fundamentals of Riser Behavior," C. M. Adams and H. F. Taylor, Massachusetts Institute of Technology, Cambridge.

Friday, May 8, 2:00 P.M.

GRAY IRON SESSION

Presiding—H. L. Ullrich, Sachs-Barlow Foundries, Newark, N. J.

Co-Chairman—G. P. Phillips, International Harvester Co., Chicago.

Recorder—J. H. Schaum, National Bureau of Standards, Washington, D. C.

"Effect of Magnesium on Eutectic Modification and Graphitization of Certain Fe-C Alloys," H. W. Weart and R. W. Heine, University of Wisconsin, Madison.

"Prediction of Mechanical Properties from Chemical Composition for Fully-Annealed Ductile Cast Iron," C. C. Reynolds, C. M. Adams, and H. F. Taylor, Massachusetts Institute of Technology, Cambridge.

Friday, May 8, 2:00 P.M.

STEEL SESSION

Presiding—A. W. Demmler, Campbell, Wyant & Cannon Foundry Co., Muskegon, Mich.

Co-Chairman—J. Neff, American Steel Foundries, E. Chicago, Ind.

Recorder—C. R. Sorensen, National Malleable & Steel Castings Co., Cicero, Ill.

"Performance of Cast and Rolled Steels in Relation to the Problem of Brittle Fracture," W. S. Pellini, F. A. Brandt and E. E. Layne, Naval Research Laboratory, Washington, D. C.

"Engineering Aspects of Centrifugal Casting," J. F. Wallace, Watertown Arsenal, Watertown, Mass.

PRESIDENT'S ANNUAL ADDRESS

I. R. Wagner*

In preparing an Annual Address on the activities and progress of this Society, a President is faced with one of two approaches: Whether to report factually on what has been done, or to point up the problems that have made themselves evident during the year.

Six months ago, I would have said that the problems would be of major importance. I have since come to realize that the major problem of A.F.S. is that its present activities and works should be better understood. . . . and appreciated. Therefore, with your indulgence I shall present a brief summary of what your Society is accomplishing, and leave to later analysts the problems of the industry.

Since the American Foundrymen's Society is a technical society, it is necessary to be factual, not theoretical. Because our activities are of primary value to the individual, we cannot be impersonal. And since A.F.S. is a source of pride and concern to its members, we have an obligation to keep you informed.

It is good to pause once in a while, to sit back and take a look at ourselves. . . . at our aims and purposes and how well we may be serving those who look to us for guidance—and to take stock of our plans for the future. Since we endeavor to serve your interests, give us the benefit of your advice and suggestions.

A.F.S. Is A Technical Society

First, let us look back over the past year and evaluate our adherence to the Society's avowed function: To advance the arts and sciences of metal casting. A.F.S. is recognized primarily as a Technical Society, and its work is directed wholly toward the technical and practical improvement of foundry operations. Our technical work is carried on by eight technical divisions—Steel, Malleable, Gray Iron, Brass and Bronze, Light Metals, Sand, Patternmaking, and Education . . . as well as by a number of General Interest Committees dealing with such subjects as Plant and Plant Equipment, Heat Transfer, Costs, Time-study and Methods, and Refractories.

Over 600 men of the industry—many of them acknowledged experts in their respective fields—serve voluntarily on A.F.S. Technical Committees. It is their willingness to impart something of their experience and knowledge to others that has gained A.F.S. its broad recognition. You cannot discount or minimize the great contributions of these men to foundry betterment . . . yet many otherwise intelligent foundry operators pay little heed to this great wealth of information so readily available.

May I congratulate you on your recognition of the value of these so-called "paper conventions." A glance at the Official Program reveals a rather significant trend away from the strictly scientific and highly theoretical, toward the practical application of fundamental principles of engineering—mechanical, metallurgical, industrial and chemical. It appears that A.F.S. has a real selling job to do at these off-year Conventions among the essentially practical foundry operators.

Speaking of Committee service, it seems not too generally understood that "voluntary service" means just that. Committee members are not hand picked by the National Office Staff; those who serve are either invited by the Committees themselves or else *volunteer* their services to the Division and Committee Chairmen. If you or your men are willing to serve and attend meetings, we urge you to write in and tell us, and not wait for an invitation.

The technical divisions and committees form the cornerstone of A.F.S. technical work. Their members prepare books, develop Convention programs, sponsor and supervise research work, write for AMERICAN FOUNDRYMAN, consult with the staff on foundry problems, present foundry talks before Chapters, Conventions and Regional Foundry Conferences. I know of no more stimulating and rewarding activity in the Society than Committee service. Progressive management should urge participation by key men.

Research

Consider the A.F.S. research projects, for example. Seven years ago we adopted a plan whereby each technical division may initiate for approval a specific research project on some fundamental problem. Bids are taken and definite contracts let by research committees which also supervise progress. At present, seven such research projects are under way:

Hydraulics of Light Metal Flow into Molds—Light Metals Division—Battelle Memorial Institute.

Fracture Test as an Index of Melt Quality—Brass & Bronze Division—University of Michigan.

Fundamentals of Heat Flow During Casting Solidification—Heat Transfer Committee—Columbia University.

Effect of Melting Conditions on the Behavior of Malleable Iron—Malleable Division—University of Wisconsin.

High Temperature Properties of Molding Sands—Sand Division—Cornell University.

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*Member of Board of Directors, Electric Steel Castings Co., Indianapolis, Ind.

This Address was presented during the Annual Business Meeting of the Society at the 1953 AFS Convention, May 7, 1953 in Chicago.

Relation of Cores to Hot Tearing—Steel Division —Cooperating Foundries.

Risering of Gray Iron—Gray Iron Division—Massachusetts Institute of Technology—Cambridge, Mass.

At present, the annual maximum allowed for any one project is $5,000; and, with eight technical divisions, conceivably only eight projects may be sponsored at one time. Personally, I believe the time is near when this program will have to be doubled in both number of projects and allowable funds.

One concrete result of this program is the series of films on "Fluid Flow in Transparent Molds," shown throughout the United States and Canada and in many foreign countries.

Publications

During the past year A.F.S. has carried on a vigorous program of issuing new and revised publications on foundry practice. Seven new books were completed and distributed, bringing to over 50 the number of available titles. Here are the ones produced since last July:

Symposium on Foundry Health, Safety and Air Pollution, presented at University of Michigan—October, 1952.

TRANSACTIONS of 1952 Convention — December, 1952.

Patternmakers Manual—January, 1953.

Glossary of Foundry Terms—April, 1953.

Statistical Quality Control for Foundries—April, 1953.

Recommended Names for Gates and Risers—April, 1953.

Symposium on Safety, Health and Air Pollution, presented at University of Illinois—May, 1953.

In the educational field, A.F.S. now is formulating a more aggressive program at the level of secondary schools and in-plant training, as a necessary corollary to the wonderful job done at the college and university level by the Foundry Educational Foundation. We must keep in mind the needs of that far larger group of foundrymen who, unable to obtain a higher education, still must be adequately trained and given their chance to advance.

We speak of this industry as one of great opportunity; and so it is . . . but there exists at present a wide educational gap that needs to be bridged. The A.F.S. Educational program will be directed toward erecting that bridge, by concentrating at the local level through our Chapters. A full-time Staff director now is being sought to put the program into effect.

Meanwhile, much valuable information has been developed for school and in-plant training purposes by A.F.S. . . . for example, the high school textbook, *Foundry Work*—the *Patternmakers Manual*—the booklets on Apprentice Training and Foreman Conferences—the new *Glossary of Foundry Terms*—the booklet and chart on Gating Definitions, and other publications.

Many A.F.S. Chapters are already carrying on educational courses for foundry workers, stimulating the teaching of foundry work in high schools and trade schools, encouraging more young men to enter this industry of great opportunity. This work must be supported locally by foundry management, not at a national level, because the success of the program will be almost wholly localized. I commend to you plant owners and managers an active interest in the educational work of your A.F.S. Chapter.

Speaking of the Chapters—and of membership—it is my pleasure to report that on May 1, 1953, the Society's membership totaled 11,012, 95 per cent of which is affiliated with 42 regular foundry Chapters and 14 Student Chapters. During the past year three new Chapters were established—Mid-South Chapter at Memphis, Tenn.; one Student Chapter at Brooklyn Polytechnic Institute, and a second Student Chapter at University of Wisconsin.

The holding of Regional Foundry Conferences, sponsored by one or more Chapters, has become a real force. During the past year eight such conferences were held, sponsored by a total of 26 Chapters . . . a remarkable record. I might add, however, that in some cases these well arranged meetings enjoyed somewhat disappointing attendance.

Now the idea of the Regional Conference is to bring to those men who cannot get away to far-distant places, the speakers and foundry experts who address national conventions. These regionals are the local conventions for *your men*—and I strongly urge you to take better advantage of the opportunities they offer, simply as a part of your own in-plant training programs.

Safety, Hygiene and Air Pollution Program

At this 1953 Convention we have arranged, for the first time, three sessions by the Safety and Hygiene and Air Pollution program, one of the most important and far-reaching activities being carried on by A.F.S. today. As you may know, the program was undertaken on recommendation of the National Castings Council, and a fund of $350,000 is being raised on a voluntary subscription basis.

The objectives of the program are three: First, to develop practical engineering standards for the guidance of foundries in controlling external air pollution so as to live as "good neighbors" in their industrial communities; second, to encourage safer foundry practice; and third, to urge constant improvement in foundry working conditions. All of this adds up to one objective . . . making the foundry a better place to work.

How is the industry responding to this vital activity? To date, a total of $75,000 has been pledged . . . hardly sufficient to carry the program through two years of the announced ten-year program. The answer lies in your hands . . . and the warning signals are up and plain to see.

Recently A.F.S. sponsored a safety and health seminar at the University of Wisconsin. Only 26 foundrymen attended . . . yet down the street on that same day, the state legislature was launching an investigation of foundry working conditions! Let me quote from the testimony presented:

"Working in a foundry is the roughest, dirtiest and most thankless job in the world."

Opposing the measure to set up a legislative committee to probe foundry working conditions was the

Wisconsin Manufacturers Association . . . and I am informed that not one representative of the foundry industry was present.

There has been a bill pending in Minnesota to forbid the use of silica flour. Once again, the Ohio legislature is contemplating more rigid air pollution controls. In Illinois, it has been proposed that any plant employing 25 or more people must have a full-time safety director and an active safety program . . . approved by the state department of health. And nationally, the infamous Murray Bill S-268 has been revived in Congress.

These are not isolated cases . . . they are part of the very real trend that led the industry to initiate the A.F.S. program *in your behalf.* Make no mistake about it, our industry's safety record is not good, working conditions in many plants are exceedingly vulnerable, and the entire industry—even the excellent plants—suffers when regulatory agencies make a public example out of the plant that either will or does not know how to operate as a "good neighbor."

We still believe that this industry is capable of setting up its own proper standards of working conditions, and the Safety and Hygiene and Air Pollution committees have gone far in doing so. However, we want and ask your support for this work.

Real progress is being made in constant improvement of the Society's official magazine, AMERICAN FOUNDRYMAN. This enables A.F.S. to carry on many activities which, while 100 per cent on the expense side, have been undertaken in your behalf. So long as we maintain a strong and useful Society, we shall continue to develop a stronger and more useful AMERICAN FOUNDRYMAN.

A.F.S. Building Project

A number of members have asked me, "What about the A.F.S. Building Project?" I am glad to report that the Society now owns a highly desirable site in DesPlaines, Illinois, a suburb northwest of Chicago, and building plans have been approved by the Housing Committee. Construction will begin just as soon as bids can be taken and considered by the Committee, and by the time of the 1954 Convention the National Office will be occupying the Society's own headquarters building.

We have had many disappointments and delays in the project, but one thing stands out in my mind. The Building Project is 100 per cent an example of cooperative Committee decision and action—the kind of democratic procedure that you have a right to expect in the operations of your Society. I can assure you that the completed structure will in every way be a credit to the Society and to the more than 800 individuals, companies and Chapters who subscribed a total of nearly $150,000.

True, the completed building will cost more than that. The A.F.S. Board has agreed to utilize reserve funds to make up any difference between contributions and actual cost, up to a maximum all-inclusive cost of $200,000. We do not intend to reopen general solicitation of funds, but Charter Subscribers are still being enrolled in this wonderful project that Walton Woody launched three years ago.

A moment ago I referred to "orderly Committee procedures." Let me assure you that the Board of Directors of your Society is the real directive force it should be. It's simply wonderful, the way the Directors have assumed their elective obligations. They spend a great deal of time visiting and inspecting the Chapters, keeping in touch with Society developments, counseling with Committees and members, and proposing new ideas or improvements. No President could ask for finer cooperation than has been given me this past year, and I want to express my personal thanks for their loyal, active support.

I am especially proud of one particular job that the Board has carried through this year—namely, the establishment of definite Board policies as proposed by E. C. Horlebein several years ago. Policy committees were set up covering seven activities—Finances, Administration, Technical Activities, Publications, Chapters and Membership, Conventions and Exhibits, and Education at the Training Level.

Membership on these committees was not restricted to the Board, because we wanted a cross-section of opinion. All seven committees met, made recommendations, and the Board approved, for the first time, definite policies which should help greatly in carrying on future A.F.S. work in orderly fashion, with less confusion and misunderstandings.

In addition, the Board set up a special committee to work out a plan for its own reorganization, in line with the Society's growth and increased activities. It is also intended to relieve the President and Vice-President of some of the excessive travel that has been experienced in recent years. The plan, as well as the Board policies adopted, are now in the hands of the By-Laws Committee, and an extensive revision of the Society's by-laws will soon be balloted to the membership.

We believe you will find it possible to approve the proposed changes—especially when I add that these revisions do *not* contemplate increasing the dues.

Recently the Board adopted, for the first time, a definite policy for the investment of any accumulated reserve funds. We are working toward an invested reserve equal to average annual expense over a period of four years, subject to review every two years. The goal at this time is approximately $450,000—and I may tell you that we still have one-third of the goal to make. In other words, the Board believes it more important to maintain needed activities and to accumulate the desired reserve gradually, than to build up unnecessarily large reserves at one full swoop.

Acknowledgments

Certain acknowledgments should be expressed at this time. To the trade associations who are members of the National Castings Council, our sincere appreciation for your cooperation and support, particularly in the Safety and Hygiene and Air Pollution fund raising. Their help has been invaluable, constant and an example of the fine spirit of friendliness that the Council has helped bring about.

To the authors, chairmen and others who have made this convention possible—and especially to the

committees who worked so hard to develop it—the Society certainly owes you a debt of gratitude. The President is in a singular position, partially behind the scenes, to see and understand how much effort goes into the making of a meeting like this.

Our Chapters certainly deserve congratulations for their greatly improved programs, their regional conferences, their increasing educational work—and for their success in building the membership to a total of 11,012 . . . the largest membership by the way, that AFS has ever enjoyed. Perhaps you did not realize that . . . it is true.

To the host of foundrymen who give so much time and thought to Committee activities, I should like to say that the entire industry is indebted to you for much of the data that represent foundry progress.

And finally, but by no means as an after thought, I wish to thank the National Office Staff for their conscientious effort and success in a trying year of many added burdens and activities. When I took office last July, I announced a program of starting nothing new but carrying through to conclusion what had already been initiated. AFS Secretary-Treasurer Wm. W. Maloney told me that, while I did stick to my program, the result has been a terrific year of administrative activity.

To our Technical Director, S. C. Massari, for his consistently fine work in steering our technical activities into real accomplishment . . . to the Editorial and Advertising staff of AMERICAN FOUNDRYMEN for building a constantly better magazine . . . to W. N. Davis, A. A. Hilbron and all the other members of the National Office—my thanks for the part that each of you has played in carrying on the Society's affairs with efficiency and dispatch.

In this address I have tried to convey to you the fact that your Society is active and progressing, and is not just sitting back to count our laurels. We carry a heavy program of activities, yet must remain alert to expand our work within our means. AFS is "big business" today, with an average annual budget of half a million dollars, and administration of such a budget requires study and constant care. The Board and Finance Committee receive regular reports, and our finances are in good shape. AFS is by no means "rich", and every additional activity undertaken requires special financing. That's why outside funds have been necessary to launch the Building Project and to carry on Safety and Hygiene and Air Pollution Work.

In final analysis, whatever the Society has accomplished during the past year is due to the support and good will of the members . . . to your belief in the value of the Society, and your participation in what it stands for. It has been a wonderful experience to serve as your President, and I am confident you will give our new President, Collins Carter, the same degree of friendship and support that you have given to me so generously. *Believe me—I am deeply grateful.*

REPORT OF THE TREASURER

For the Fiscal Year ended June 30, 1953

On July 17, following completion of the annual audit of the Society's financial accounts by George V. Rountree & Co., Certified Public Accountants of Chicago, the Treasurer mailed to all members of the Board complete financial data, including: Auditor's report as of June 30, 1953; Consolidated Statements of Income and Expense for fiscal years 1950-51, 1951-52 and 1952-53, showing budgets and actuals in all cases; Income and Expense Budgets (included in above Consolidated Statements) for the 2-year period 1953-1955, as recommended by the Finance Committee.

1952-53 General

The statements for 1952-53, as audited, reported the following: Income $507,426, or 1.9 per cent more than the $498,000 budgeted; Expense $563,999, or 4.2 per cent more than the $541,210 budgeted. Thus, while an Excess Expense of $43,210 was budgeted for the fiscal year, actual Excess Expense totaled $56,573, or 30 per cent more than budgeted.

Total Income of $507,426, increased $124,503 or 32.5 per cent over the year 1950-51, was the greatest for any non-exhibit year on record. Total Expense of $563,999 was an increase of $132,646 or 30.7 per cent over 1950-51.

1952-53 Income and Expense

The Auditor's report shows Income and Expense in an operational form of statement with all Salaries and Burden items distributed to activities. It will be noted that only two activities (Member Service and American Foundryman) produce a Net Income in a non-Exhibit year, all other activities showing a Net Expense.

A comparison of Net Income and Expense by activities for 1951-52 and 1952-53 follows:

NET INCOME	1951-52	1952-53
Exhibit	$203,262	——
American Foundryman	31,181	$ 25,677
Member Service	185,494	208,749
Miscellaneous	6,889	(297)
Total	$426,826	$234,129
NET EXPENSE	**1951-52**	**1952-53**
General Administration	$ 82,704	$101,318
Technical Activities	49,042	54,985
Chapter Operations	68,326	79,924
General Publications	18,246	22,069
Special Publications	5,353	3,502
Convention	20,542	28,904
Total	$244,213	$290,702
Excess Income (or Expense)	$182,613	($ 56,573)

The following figures showing Per Cent of Income by activities may be of interest:

Per Cent of Income from AFS Activities

	1951-52	1952-53	Two Years 1951-53
Membership Dues	29.5%	47.6%	36.8%
American Foundryman	25.9	41.3	32.1
Conventions & Exhibit	38.4	1.6	23.5
Publications	5.2	8.2	6.2
Miscellaneous	1.0	1.3	1.4
	100.0	100.0	100.0

A comparison of Income and Expense per member in the past two non-Exhibit years shows the following:

	1950-51	1952-53
Total M/S on June 30	9,340	11,033
Total Income	$382,923.00	$507,426.00
Income per Member	$ 40.99	$ 45.99
Total Expense	$431,353.00	$563,999.00
Expense per Member	$ 46.18	$ 51.11

The above may be compared with two Exhibit years:

	1949-50	1951-52
Income per member	$ 63.98	$ 73.24
Expense per member	50.66	55.03

Thus in the past two non-Exhibit years, both Income and Expense per Member have increased: Income by $5.00 per member, Expense by $4.93 per Member.

1952-53 Balance Sheet

The financial position of the Society on June 30, 1953, including all funds, was $57,198 better than one year ago, or an increase of 9.2 per cent. If we consider the General Fund alone, the increase over 1952 was $38,409, or 9.1 per cent.

Cash decreased from $236,715 on June 30, 1952 to $80,300 on June 30, 1953, due to the following investments:

Nov. 1952—$80,000 Cash invested with Harris Trust.
Jan. 1953—$32,000 Cash from Canadian accounts invested with Harris Trust.
May 1953—$35,000 Cash invested with Harris Trust.
Total.....$147,000 Cash invested 1952-53

It is believed that at least 50 per cent of the Cash on hand as of June 30 can also be invested, until 1954 Exhibit revenues commence in December. Also, provision should now be made, through authorization, to invest Exhibit receipts in short term notes as such receipts come in early next year . . . possibly in increments of $10,000.

Investments (General Fund) increased from $220,600 on June 30, 1952 to $332,761 this year. In this connection, attention is called to the AFS policy of a reserve fund "equal to average annual expense over the previous four years, subject to review every two years." The reserve fund is still 33.6% or $168,696 below the investment policy goal.

The four Fund Principals of the Society are analyzed in the auditor's report and show a total of $679,973 or $57,198 more than the total of $622,775 one year ago.

Conclusion

The Society's finances appear to be in good shape for the close of a non-Exhibit year, with the Fund Principal (exclusive of Awards, Building and S&H&AP funds) totaling $459,226 against $268,349 at the close of the previous non-exhibit year 1950-51. Close control of expenditures by the Secretary-Treasurer is advisable, through renewed cost-consciousness by all staff members responsible for expenditures.

Respectfully submitted

Wm. W. Maloney, *Treasurer*

Condensed Statement of Income and Expense

(All Expenses Distributed to Major Activities) Fiscal Years July 1, 1951 - June 30, 1953

INCOME

	Total Income Amount	Per cent
Membership Dues	$394,242.63	32.0
General Publications	25,608.78	2.2
Special Publications	56,796.78	4.6
AMERICAN FOUNDRYMAN	461,900.44	37.5
Conventions & Exhibits (1952-1953)	291,752.00	23.7
TOTAL INCOME	$1,230,300.63	100.0

EXPENSE

	Total Expense Amount	Per cent
Membership Service	$ 60,191.74	5.4
Research	54,851.71	4.9
Other Technical Activities	49,175.46	4.4
General Publications	65,923.98	5.9
Special Publications	65,652.45	5.8
AMERICAN FOUNDRYMAN	405,042.39	36.0
Chapter Operations	148,249.84	13.2
Conventions & Exhibits (1952-1953)	137,935.59	12.3
General Administration	85,955.41	7.6
Retirement Expense	31,282.12	2.8
Safety & Hygiene & Air Pollution (Net, 1 Year)	19,650.20	1.7
TOTAL EXPENSE	$1,123,910.89	100.0
TOTAL INCOME	$1,230,300.63	
TOTAL EXPENSE	$1,123,910.89	
Excess Income Over Expense	$ 106,389.74	

Comparative Condensed Balance Sheets

As of June 30, 1950-51-52-53

ASSETS	June 30, 1950	June 30, 1951	June 30, 1952	June 30, 1953
Cash	$229,497.13	$103,913.45	$297,608.34	$129,920.33
Investment Securities	118,805.60	308,805.60	330,600.00	448,761.14
Inventories	35,354.99	27,817.59	32,470.00	39,667.57
Accounts Receivable	22,011.00	49,975.09	46,376.91	37,662.98
Deferred & Prepaid Items	31,530.03	31,258.11	34,378.06	12,950.68
Furniture & Fixtures (net)	16,451.18	15,858.16	15,559.74	14,616.25
Land and Building	—	—	—	24,341.01
TOTAL	$453,649.93	$537,628.00	$756,993.05	$707,919.96
LIABILITIES				
Current Liabilities	$ 24,867.45	$ 19,736.66	$ 13,771.93	$ 27,946.24
Deferred Income	108,081.29	109,735.45	120,446.02	—
Reserve for Exchange	—	1,344.30	—	—
Fund Principals	320,701.19	406,811.59	622,775.10	679,973.72
TOTAL	$453,649.93	$537,628.00	$756,993.05	$707,919.96

NOTE: Includes all Awards, Building, and Safety & Hygiene funds.

REPORT OF THE SECRETARY

(For the Fiscal Year ended June 30, 1953)

1. Membership

During the past year, AFS membership reached a new high total of 11,033 or a net gain of 967, approximately 9.6 per cent over the total of 10,066 on June 30, 1952. The budget estimate for 1952-53 was 11,000 members, showing a continuing upward trend. The work of the Chapters is pointed up by the fact that the 11,000 goal was set in anticipation of new Chapters which did not materialize.

The membership gain was in almost all classes: Sustaining up 2, Company up 19, Personal up 338, Affiliated up 581, Student & Apprentice up 19. Only the Associate class lost (1), a class including educators and perhaps showing again the need for emphasis on education. The gain of 338 in Personal members is encouraging again, since from this Class comes an increase in Company memberships, through conversions.

Membership gains and losses for five years:

	1948-49	1950-51	1951-52	1952-53
Members gained (all Classes)	1,755	2,316	2,909	1,947
Members lost (all causes)	2,095	2,158	2,049	980
Net Gain (or Loss)	(340)	158	860	967
Avg. gain (Loss) per Month	(28.3)	13.2	71.7	81.0
Total as of June 30	10,063	9,206	10,066	11,033
Rate of Turnover	20.8%	23.4%	20.3%	8.9%

In the past year, 1,533 delinquents were dropped, and 968 or 63 per cent were reinstated. To us, this is most encouraging evidence of greater Chapter attention to holding existing members as well as gaining new ones. It is perhaps the major reason why the Membership Chairman of 21 Chapters received "Target Certificates" this year, against 18 last year and 8 the year before. The certificates were awarded to all Chapters who made their targets on June 10 (prior to the Chapter Officers Conference) as well as on June 30.

Targets are considered as totals only and are not based on classes of membership. More effort will be made in 1953-54 to dramatize target efforts, by posters to display at monthly Chapter meetings, and in AMERICAN FOUNDRYMAN.

As recommended to the Board last year, and approved, Canadian bank accounts for membership dues have been closed out and all Canadian members now pay their dues direct to Chicago.

2. Chapters

One new regular Chapter (Mid-South at Memphis, Tenn.) and one new Student Chapter (University of Wisconsin) were established during the past year. The Brooklyn Polytechnic Institute Chapter, installed in January 1953, was established in June 1953 and so announced. Total regular Chapters today, 42; total Student Chapters, 14 (including the inactive Chapter at University of Minnesota).

During the past year, President Wagner attended four of the nine Regional Conferences held, and Vice-President Carter attended five, including the Northwest and California Conferences. A compilation of Chapter visits (according to our information) by Officers, Directors and Staff shows that all but 6 Chapters were visited at least once during the year.

The 10th Annual Chapter Officers Conference was held at the Sherman Hotel, Chicago, June 18-19, with a total attendance of 102, including 81 Chapter delegates representing all 42 Chapters, including the new Mid-South Chapter. All but 5 Chapters had at least two delegates present, and 2 had three delegates. The total included 38 Chairman, 36 Program Chairman, 5 Secretaries and/or Treasurers. Chairman Collins L. Carter presided and 4 National Directors attended, 5 less than in 1952, which in turn was attended by less Directors than in 1951.

Last year we recommended to the Board, and the Board approved, splitting the Conference into two meetings: in June for Program Chairmen only, and in September for Chairmen only. The idea was to concentrate on subjects of interest to each group and further acquaintanceship. However, when the proposal was announced at the Conference in June, the entire attendance voted for continuance of the present single Conference for Chairmen and Program Chairmen jointly. The Board should determine at this meeting its course for 1954, but we believe that the Conference motion practically amounts to a mandate, at least for the present.

During the year 9 Regional Conferences were held by a total of 23 participating Chapters: Michigan, All-Canadian, Ohio, Purdue, Northwest, Southwest, California, Wisconsin, and Southeastern. The All-Canadian, Southwest and California conferences will not be held in 1953-54. A Niagara Frontier Conference at Buffalo (6 Chapters participating) is scheduled for September. A Metropolitan-Philadelphia-Chesapeake Conference may be held, but has not yet been announced. The New England Regional Conference (non-AFS) again will be held in October.

All major foundry areas in the U. S. and Canada now have established Chapters, with the exception of New England, Pittsburgh and two or three areas in Pennsylvania. AFS services to members in these areas must continue to be less than to others until Chapters are formed there, and these non-Chapter members necessarily will be more vulnerable to loss in the event of a business recession.

3. Convention

A total of 3,528 attended the 57th Annual Convention of AFS in Chicago, May 4-8, the largest attendance on record for any non-exhibit year. This compares with registration records of 2,617 in 1951 and 1,953 in 1949. The registration fees of $2.00 for Members and $5.00 for non-Members were charged in 1953 and no complaints were recorded. Over 1,800 were registered in advance of the meeting, another new high, comparing with 1,330 in 1951. A record total of 276 ladies also registered, compared with 199 in 1951.

No complaints on housing were recorded this year and the Sherman Hotel rendered outstanding cooperation in spite of construction difficulties. Outside of the lobby, the Sherman delivered all and more of what was promised.

It is apparent that expected local foundry attendance at a non-exhibit Convention is not being obtained (at least it was not in 1953), either in firm representation or average number of persons per company. The average in Chicago proper was 3.5 per foundry, and 2 in the Chicago area. Since this Convention program was essentially of practical value, it is obvious that these non-exhibit Conventions must be better sold to foundry management, especially "host Chapter" management.

The local Convention Committees under the general chairmanship of F. B. Skeates, and of Chapter Chairman John Owen, gave the Society excellent cooperation in staging the 1953 annual meeting. The Ladies Committee under co-chairmanship of Mrs. B. C. Yearley and Mrs. R. P. Schauss provided an outstanding program of entertainment. Numerous compliments have been received on the smoothness with which the Convention was run, thanks largely to the efforts of Technical Director Massari.

The technical program was an excellent one throughout, and many sessions were exceptionally well attended, although some of the evening shop operation courses were not too well attended. The Annual Lecture was presented before a full house and was an outstanding contribution. The Annual Banquet, held at the Morrison Hotel, served over 700, another non-exhibit year record, and the hotel went overboard in accommodations, service and the spectacular pouring of flaming brandy for the dessert.

Awards were presented at the Banquet in the same time-saving manner as in 1952, and the same procedure should be followed hereafter. The Alumni Dinner was held in the traditional "stag" manner, as requested by numerous Alumni, and probably will be so continued. The Canadian Dinner as usual was well attended, and some thought is being given in Canada to inviting ladies to these functions in the future.

In view of our 1953 experience, we strongly urge that all future non-exhibit Conventions be of 5-day duration, with the Banquet Wednesday night, so as to allow more time for personal discussions and conferences, as well as for the technical program. No list of registered attendance is prepared for non-exhibit years, requests for such lists being too few to justify the printing expense.

4. American Foundryman

The year 1952-53 was a productive one for AMERICAN FOUNDRYMAN in terms of editorial organization and acceptance, but a disappointing one in terms of advertising revenue. As announced to the Board meeting in March, the editorial staff has been

increased to three (H. F. Scobie, H. J. Wheelock, G. J. Minogue) and greatly strengthened, with duties and responsibilities much better defined. The policy of "planned content" is now being carried out. The appointment of Associate Editors is to be accomplished in some manner acceptable to all.

The advertising income budget for 1952-53, which followed the 3-year budget established in July 1950, was approved on the basis of an excellent year in 1951-52 and in the belief that progress in the first two years would give AMERICAN FOUNDRYMAN the impetus it needed to make the third year of the budget, admittedly high. This belief apparently was too optimistic, for display advertising grossed $197,000 instead of a budgeted $216,000. Total advertising pages increased from 664 to only 675, instead of 796.

Results for the 3-year budget period are both noteworthy and commendable, by any magazine standard. In three years' time we have increased advertising gross by 84.1 per cent (from $107,000 to $197,000), and increased the number of advertising pages annually by 60 per cent (from 425 to 675).

As announced in March, certain steps have been taken to overcome the factors that resulted in failure to make the budget this year. The magazine now is reaching every foundry in the U. S. and Canada, monthly. The mailing date of the magazine is being advanced by two weeks so as to place AMERICAN FOUNDRYMAN in a better position with regard to inquiries received from advertising. New advertising rates become effective for all after the September 1953 issue. Steps are being taken to make the activities of AFS better known in the advertising profession. Our sales representatives are now more experienced and this experience should prove its worth in terms of 1954 advertising contracts, especially approaching an exhibit year.

The circulation of AMERICAN FOUNDRYMAN (i.e., membership) will continue to be audited by the Audit Bureau of Circulations (A.B.C.), in the belief that paid circulation is in general superior to free distribution.

5. Building Project

During the past year the log-jam which impeded building progress was broken and land acquired in DesPlaines, Ill. However, plans drawn up to that time were discarded by the Building Action Sub-Committee during the 1953 Convention, and F. C. Riecks was designated in charge of all engineering details to work out specifications with the AFS Technical Director and the architect. Specifications and drawing now are complete and will go out for bids soon to reputable contractors. We expect to occupy the completed building in June 1954.

To date a total of $148,761.93 has been raised for the Building Fund. In May 1952 the Board agreed to make up the difference between funds raised and the actual cost of the project, up to an all-inclusive maximum of $200,000. It is now evident that the total cost will surpass the maximum set by the Board, to an amount unknown at this time but estimated at between $25,000 and $40,000. At the same time, the Secretary has been instructed to institute a strong campaign to raise additional funds for the building, this campaign to be concluded as far as possible in advance of December 31, 1953.

6. General Administration

During the year seven Policy Committees met and made recommendations which were approved by the Board at meetings in November 1952 and March 1953. Thus established policies now are in effect and in the form of a "Policy Manual." Each member of the Board has received a copy of these policies, which they might well take with them when visiting Chapters during the coming year.

Three meetings of the By-Laws Committee were held during the year in an effort to revise the Society's by-laws in accordance with experience since the last revision (July 1948) and the recommendations of the Board Reorganization Committee, approved by the Board in March 1953. The complete recommendations for revisions as prepared by the By-Laws Committee, differing somewhat from the Board's recommendations last March, will be presented at this meeting for approval, following which they will be submitted to the Society's membership for letter ballot approval.

Several important Staff changes have taken place during 1952-53: Technical Director S. C. Massari resigned effective July 1, 1953, to become General Manager of the Foundry Division of Hansell-Elcock Co., Chicago. To replace him, Hans J. Heine was employed as Assistant Technical Director and acting as technical director, beginning June 29. The employment of H. J. Wheelock as Managing Editor, and G. J. Minogue as Production Assistant on AMERICAN FOUNDRYMAN have already been mentioned. H. F. Scobie has been made Technical Editor, to concentrate solely on the technical content of the magazine, and the Secretary has assumed temporarily the directional title of Editor.

7. Safety and Hygiene and Air Pollution

While covered in a separate report, certain aspects of the Safety & Hygiene & Air Pollution program may be mentioned. Contributed funds to date total approximately $75,000 and strong effort must be made to advance the fund, which now is sufficient for two years' operation at the rate maintained during 1952-53. It is not intended to commence strong solicitation until (a) one or both of the manuals on Dust & Ventilation and on Air Pollution are available for distribution, and (b) the authorized solicitation of additional Building funds has been terminated.

As a result of foundry health conferences held at the Universities of Illinois and Wisconsin, two symposia have been published for the program, and widely distributed. A number of other important publications are in the making, but in several instances the committees are doing a "pioneering" job and the work cannot be hastened unduly. It should be pointed out that the cost of publications produced under the S&H&AP program are charged to General Funds, in order to conserve S&H&AP funds.

During the year, the program sponsored several safety training courses in Chicago, but the attendance at these, as well as the attendance at the two university symposia previously mentioned, was much less than expected. No further safety training courses will be authorized without more tangible advance evidence of local support.

Admittedly, the program needs far more publicity to foundry management before the goal of $350,000 is raised from the industry. While this is only one phase of the Society's need for additional and continuous publicity, it is intended to partially overcome this disadvantage during the coming year.

8. Technical Activities

During the coming year it is our aim to concentrate on our present technical activities, rather than initiate new programs. For example, the approximately $35,000 now being spent on research should be increased as soon as possible to $50,000 or more in keeping with AFS functions as a technical activity.

9. Publications

Technical publications of AFS are covered separately in the report of the Technical Director and in minutes of the Publications Committee. However, certain general problems associated with publications may be mentioned here:

(1) AFS has listed for sale, for several years, publications of foundry interest published by others, on the same discount basis as AFS publications are sold by commercial book dealers. Thus we carry publications of AIME, Penton, SAE and ASM. During the past year we added the "Malleable Handbook" of Malleable Founders' Society. Negotiations are under way to handle cost manuals of Gray Iron Founders' Society.

(2) Special publications of AFS cover Sand, Patternmaking, Education and Training, Costs, Safety & Hygiene, Gating, Casting Defects, Statistical Quality Control, several Bibliographies, and Foundry Practice in Gray Iron, Malleable, Brass & Bronze, and Light Metals. The only AFS publication on Steel foundry practice will be out of print this year, with no plans to reprint.

(3) Greater distribution of TRANSACTIONS is most desirable, requiring greater promotional effort. The present printing of 1,500 copies is being increased, and our goal for this volume will be 2,500 copies sold annually, mostly on pre-publication sales. For example, all Company and Sustaining members (totaling 1,494) should have a copy.

All new AFS publications are recommended by the Publications Committee, which body also sets pricing formulae for AFS publications.

10. Recognition

In reporting a year of intense activity and some real accomplishments, great credit must be given to many: To the President and Vice-President for constant willingness to consult on problems and attend important meetings; to the Directors for their cooperation in Chapter contacts work; to the Chicago Officers for improved programs and aggressive membership work; and to the Divisions and Committees who stimulated all technical activities and presented outstanding programs at the Chicago Convention.

Respectfully submitted,

WM. W. MALONEY, *Secretary*

REPORT ON SAFETY & HYGIENE & AIR POLLUTION ACTIVITIES

(Fiscal Year 1952-1953)

Since W. N. Davis became Director of the Safety & Hygiene & Air Pollution Program in May 1952, a great deal of developmental work has been carried on. Five working committees have been set up on Air Pollution, Dust & Ventilation, Safety, and Noise Abatement, plus a Welding committee which had been previously organized. All working committees have been holding regular semi-monthly meetings, and each committee developed a program at the 1953 Convention in Chicago. The Convention sessions were well attended, indicating interest in the Program.

During the year Mr. Davis has made many trips out of town in working on technical and legislative problems, particularly as to Air Pollution, and studying important foundry installations. Valuable contacts have been made with associated organizations, and at least two publictions issued covering symposia on foundry safety and health at the University of Illinois and the University of Wisconsin.

Some detail of the work of the committees during 1952-53 is as follows:

(1) *Steering Committee*—The Steering Committee last met October 14, 1952, at which time the Chairman of each working committee was made a member of the Steering group. A meeting of the Steering Committee is planned for early this fall and intends to meet twice a year to coordinate and recommend activities for the working committees.

One important function of the Steering Committee is to review new published material prepared by the working committees, before such material is published.

(2) *Safety Committee*—This Committee has completed about 75 per cent of the work involved in revising the booklet, "Recommended Good Safety Practices for the Protection of Workers in Foundries," which should be ready for early printing.

During the past year two training courses were held in Chicago for foundry supervisors, entitled, "Fundamentals of Foundry Safety." While four courses were originally announced, lack of advance interest resulted in canceling two. Nevertheless, the Committee considers the courses successful and intends to continue them during the coming year, so designing them that they can be put on as part of the educational activities of local Chapters. Several Chapters have indicated interest in presenting such a course for their members during the coming year. The courses will be prepared as a package, which may or may not require chairmanship by a member of the Central Office Staff.

(3) *Dust Control and Ventilation Committee*—This Committee has been most active, particularly on their main project, the developing of a manual on dust and ventilation control. The manual is now approximately 60 per cent complete. Photographs have been provided and tables prepared by the Committee personnel and drafting departments of their respective companies. These companies are to be commended for their help in advancing the work, which should also save considerable in the cost of production. The manual is expected to be ready for printing this fall.

The Committee also cooperated with the McIntyre Research Foundation of Canada in developing information on the subjects of silicosis and aluminum therapy for the foundry industry.

(4) *Air Pollution Committee*—This Committee has been holding regular semi-monthly meetings in developing an air pollution manual for the foundry industry. The outline of contents was completed last December, and while the work is constantly progressing, it is realized that the Committee is pioneering on a subject concerning which there is much opinion and relatively little factual data that would warrant publication by AFS as a technical society. An approximate date of publication for this manual is not at present established and some research and many field trips to air pollution installations may have to be made before the job can be finished as it should.

Meanwhile the Committee has produced one pamphlet on air pollution and, due to pressure of the industry for information on air pollution control methods in the metal melting operation, is now working on a second pamphlet dealing with this subject. This pamphlet is expected to be produced soon, after which the Committee will resume full-scale work on the air pollution manual which they predict will take one or two years more to produce.

(5) *Noise Abatement Committee*—While it might be said that this Committee was started because of a desire not to actively promote insistence upon control methods, the industry seems now to be showing more interest in the subject. The Committee has developed some information on foundry noise abatement which will be developed for publication only under the direction of the Steering Committee. At the same time it will be available to any foundry requesting it from the Committee.

(6) *General*—Certain general problems concerning the Safety & Hygiene & Air Pollution Program might be brought to the attention of the Board at this time:

(a) The budget of the Program for 1953-1955, to be presented for the new Board July 24, does not include any major expenditure beyond those of the past year, at least for the current fiscal year. However, it is expected that certain broader expenditures may be incurred in 1954-55 and the budget for that year will necessarily be subject to revision.

(b) During the past years the number of requests for talks on S&H&AP subjects was not as extensive as in the previous year, which has enabled Mr. Davis to do considerable necessary contact work. Neither have there been too many requests for private plant consultation. As originally announced, we do not intend to set up a consulting service, but the fact remains that there are not today any known and fully qualified consultants to render this assistance, outside of those manufacturing equipment and supplies for sale to the foundries. AFS probably cannot fully escape some semi-consulting service in the future.

(c) It is intended that no concentrated solicitation of Safety & Hygiene funds will be made until the current Building Fund solicitation is discontinued, so as to avoid duplicating our fund-raising activities. We do not feel that the S&H&AP Fund solicitation should be actively pressed until either or both the Dust & Ventilation manual and the Air Pollution manual have been published, as concrete evidence of the Program's work and worth.

Respectfully submitted,

WM. W. MALONEY, *Secretary*

ANNUAL REPORT OF TECHNICAL ACTIVITIES

(Fiscal Year 1952-1953)

The following progress report has been prepared covering the period beginning July 1, 1952 and ending June 30, 1953:

General

Each year at this time it has been our practice to take careful inventory so as to eliminate committee members who are inactive and consider the appointment of new ones to take their place. When this information has been received, we will then be in a position to proceed with the compilation of the new roster.

Based upon the number of new special publications which have been published within the last year, and which are actively in process of preparation, and the unusual interest exhibited at the last Convention in the various technical sessions, there can be little doubt that interest in the technical affairs of the Society is at an all-time high.

It is also important to note that the Cupola Research Committee has now been reorganized on a sound basis with H. Bornstein as Chairman. Under the guidance of Mr. Bornstein, we are certain that it will perform to the satisfaction of the entire membership. It has already undertaken the task of preparing a revised edition of the Cupola Handbook, and plans to have this manuscript completed by the first of the year.

Research

A complete report giving pertinent information as to the present status of Society-sponsored research projects has been prepared and transmitted to the Research Committee and the Board of Directors, comprising Messrs. Seelbach, Dietert, Fladoes and Troy. This Committee reviewed the recommendations contained therein and has made their recommendations to the Board of Directors covering appropriations which will be required for the coming fiscal year. A copy of this report is appended hereto.

Convention Program

The success of our 57th Annual Meeting can hardly be questioned, based upon the unusual attendance at all technical sessions and the high quality of technical papers presented. Of a total of 96 papers offered, 87 were approved by the Program and Papers Committees for presentation. Of the papers presented, 34 were preprinted and 10 were published in the February, March and April issues of AMERICAN FOUNDRYMAN. In addition, 10 were preprinted and made available for distribution at the sessions.

Nearly 4000 individual requests were received from members for varying numbers of preprints. Preprinting of Convention papers is an important service to the membership and, in addition to fulfilling the basic obligation of disseminating worthwhile technical information, represents one of the several gratis services to our members.

A total of 78 official functions were scheduled during the Convention, exclusive of committee meetings. It would be our recommendation that technical sessions be terminated at noon of the last day of the Convention, rather than extend them into the afternoon and do an injustice to the authors who present their papers to a diminishing audience.

As a result of considerable discussion during the Technical Correlation Committee meeting, it is also recommended that during the 1954 Annual Convention a sufficient number of tape recorders be rented so as to record pertinent discussion during the course of sessions. This will provide a convenient and time-saving method for subsequently preparing a worthwhile record of the discussions.

In view of the relatively poor attendance at the Educational Dinner for several years, it is our recommendation that this be scheduled as a luncheon rather than a dinner function at future Conventions.

Publications

The annual Transactions for 1952, Vol. 60, comprising approximately 900 pages, was published during December 1952 to all who had ordered it on a pre-publication basis. A total of 1339 copies were shipped, of which 219 were supplied gratis to Honorary and Sustaining Members of the Society on request. A total of 1500 copies were produced, providing additional ones for stock which are being sold at a post-publication price of $8 per copy to members, $15 to non-members. The pre-publication price was established by the Board Publications Committee, namely $6 to members and $15 to non-members.

As a matter of interest and to give a definite measure of the extent of the subsidy necessary to produce the Annual Transactions, this volume cost the Society $12.81 per copy in out-of-pocket expense, exclusive of staff time consumed in editing and production. Because of the cost of this publication, we probably have been overly conservative in the number produced. For the past two years our supply has been completely exhausted within six months after the volume was available. It is recommended, as a service to members and particularly new members who might like back volumes, that the number published be increased by at least 100 copies.

During the fiscal year the following publications were produced and offered for sale:

Symposium on Air Pollution.
A Study of the Principles of Gating (reprint).
Patternmaker's Manual.
Statistical Quality Control for Foundries.
Risering of Gray Iron—Research Report No. 2 (reprint).
Recommended Names for Gates and Risers (booklet).
Recommended Names for Gates and Risers (wall chart).
Glossary of Foundry Terms.
Health Protection in Foundry Practice.
Safety Practices for Protection of Workers in Foundries.
Classification of Foundry Cost Factors (reprint).
Foundry Core Practice, 2d edn. (2d edn.) (reprint).
Charles Edgar Hoyt Annual Lecture, 1952.
Guide to Authors (reprint).
Symposium on Foundry Safety, Health and Air Pollution.

This represents the largest single year's publication program in a number of years. A great deal of credit is due the individual committees responsible for preparation of the manuscripts.

The following books are also in process of preparation by the various committees and individuals responsible for them:

Transactions, Vol. 61 (1953).
Charles Edgar Hoyt Annual Lecture, 1953.
Code of Recommended Good Practices for Metal Cleaning Sanitation (reprint).
Index to Transactions—1941-1950.
Symposium on Sand Reclamation.
Time Study & Methods Manual.
Gray Iron Research Report No. 4.
Die Casting Methods (to be translated under royalty contract with the German publishers).
A Study of the Principles of Vertical Gating.
Symposium on Molding Machines.
Recommended Practices for Precision Investment Casting.
The Cupola and Its Operation (revision of Cupola Handbook).
Cast Metals Handbook (revision).
Sand Research Committee 8-L Progress Report.
College Foundry Textbook.
Recommended Practices for Sand Casting of Light Alloys.
Foundry Research Projects (revised).
Manual on Design and Stress Analysis (Light Metals Division).

It is particularly gratifying to report that publication sales this year are markedly above those of last year. These increased sales are the result of a number of new publications and improved sales effort. Even though the production of these books represents a large financial outlay, they likewise become an important source of revenue under present pricing policies.

Technical Inquiries

These continue to be an important service at National Headquarters, answers reflecting the best available information at our disposal and compatible with the amount of time available to investigate all sources. During the past year 391 technical

inquiries were received. We again emphasize the importance of having a full-time, qualified librarian as a member of the staff so that these replies may be as comprehensive as they could be with definite references prepared by a librarian.

Apprentice Contest

This year the Society again sponsored successful Apprentice Contests in the fields of Wood Patternmaking, Metal Patternmaking, and Molding of Light Metals, Copper-Base, Gray Iron and Steel. A total of 269 entries were received from 92 companies and 10 Chapters, the largest number of entries since 1949. Greater participation in the Contest is largely dependent upon the interest and activity in individual Chapters, who in many instances, are not sufficiently conscious of the contest and the need for careful selection of a Chairman and committee members responsible.

Speaker List

In order to be of assistance to the Chapters in preparing good technical programs, a new list of speaker possibilities is made available each year to all Chapter officers and representatives who attend the Chapter Officers Conference, as well as to others on request. Each year the revision of this list is based in large part upon the Speaker Evalution Reports received from the Chapters during the year, so as to avoid continued listing of speakers who have failed to perform acceptably. New speakers of real merit are added when the list is revised each year.

AFS Film Directory

As an additional service to Chapters, we have again prepared a new and completely revised list of film covering subjects which may be of interest to the Chapters and which are known to be available.

The AFS color-sound film on "Effects of Gating Design on Casting Quality," the third film resulting from research sponsored by the Society at Battelle Memorial Institute under direction of the Light Metals Division, will again be made available on a rental basis to our Chapters and other organizations. As in the past, a rental fee of $20 will be charged for each showing, with the exception of educational institutions. This film had its premier showing at the International Foundry Congress in 1952 and has been loaned to 51 groups.

In closing, the writer wishes to express his deep appreciation to the Board of Directors, to all members of the technical committees, and to the National Office Staff, who in the past 7½ years have been responsible for developing the technical activities of the Society to their present state. In leaving the Society on June 30 of this year, the writer will miss many of these friendly associations and wishes to express sincere gratitude to all who have been so helpful.

APPENDIX—AFS-Sponsored Research Projects

Brass and Bronze Research—The Society entered a contract with the University of Michigan in December of 1947, to develop information on the value of the fracture test as an indication of melt quality of tin bronzes. It was hoped that this research would provide a satisfactory basis of interpretation for the fracture test and thereby supply an inexpensive, rapid and dependable test to serve as a measure of the quality of molten bronze before it was tapped from the furnace and poured into the mold.

The contract was in force for three years, involving a total expenditure of $15,000. It was terminated at the end of the third year to provide time for foundries to actually utilize the test and report on results obtained. At the present time the Committee still feels that this has not as yet been accomplished and steps are being taken to further publicize the test and solicit the cooperation of foundrymen. Until such time as appropriate evaluation has been made in the Foundry itself, the Committee is neither in a position to ascertain whether additional work should be done on this project, or a new project initiated.

It is recommended that until such determinations have been made, no funds be specifically appropriated at this time for Brass and Bronze research, with the express understanding that if during the year the Committee is in a position to make definite recommendations and funds are needed, the Board give consideration to making this money available at that time.

Cupola Research—For the first time in several years we are able to report definite progress by the Cupola Research Committee. We have been fortunate in obtaining H. Bornstein as Chairman and because of his experience and keen interest in this project, we are certain that real progress will be made. Of the total sum originally received from donors, $6180.88 still remains unexpended.

The Committee met on June 3-4 this year, at which time they made detailed plans for preparation of a completely revised edition of the "Handbook of Cupola Operation" and it is planned to complete the manuscript by approximately January 1, 1954. If this is achieved, it will be a genuine accomplishment and a worthwhile contribution to the Industry.

The Committee will take inventory later to determine if it wishes to undertake a research project, specifically delineated, including the mode of accomplishment, funds required, as well as a recommendation to the Board of Directors as to how such funds should be acquired. At the last meeting of the Committee, unanimous approval was given to the expenditure of a nominal sum, namely $200 maximum, to defray the cost of stenographic services by the Chairman in preparing various materials for the committee charged with the preparation of the revised edition of the book.

Gray Iron Research—The research on "Risering of Gray Iron Castings" was initiated in December 1949 at the Massachusetts Institute of Technology and has as its fundamental purpose the development of basic information on the risering of gray iron castings so as to achieve solidity and maximum casting yield. Up to the present time a total of $20,000 has been expended to cover this work and it is believed that a sum of $5000 should be appropriated by the Board for another year's work, beginning December 1953, if at that time the Gray Iron Research Committee can justify a continuation of the investigation from results obtained between now and that time.

An Annual Progress Report covering this research was presented during the Annual Convention in May, creating a great deal of interest. This is now available from the Society as "Gray Iron Research Report No. 4."

Heat Transfer Research—The Heat Transfer Research being conducted at Columbia University for a number of years under the direction of Dr. Victor Paschkis for the Heat Transfer Committee, is creating increasing interest on the part of foundrymen, as evidenced by the growing attendance at sessions before which these progress reports are presented, as well as other papers on the same subject originating from others in the foundry field whose interest has been sufficiently stimulated to cause them to do worthwhile exploratory work.

Although previous appropriations have only been $2000 per year, last year the Board approved an expenditure of $4000. For reasons already enumerated, and the work planned for the coming year, an appropriation of $4000 is again recommended for this fiscal year.

Light Metals Research—The original contract for this project was executed in October 1947 with Battelle Memorial Institute, for a capital sum of $5000 for one year's work. In succeeding years it was deemed necessary to increase the Society's contribution to $6500 and this amount has been made available each year since then. In addition, Battelle has contributed $1500 of its own money each year.

The project comprises a fundamental study of the dynamics of flow of metal into a mold. Up to last year the work was confined to a study of horizontal gating systems; during the past year and at present, efforts are being devoted to a similar study of vertical systems. In addition to annual Progress Reports at the Annual Conventions, three motion picture films have been produced to portray these fundamental behaviors. It is presently anticipated that a fourth film will be available for the Annual Meeting in 1954. During the past year the Technical Director of the Society has personally presented this film before 22 Chapters of the Society. A rapidly growing interest is evidenced, based upon the extent to which foundrymen have applied its findings in the foundry and quickly learned the benefits to be derived.

There is little doubt that no other single activity of the Society has created a greater degree of interest on the part of the members than these research progress reports and the motion picture films. Even though the research is being directed by the Light Metals Division, it is fundamental in character and of equal interest to all other branches of the industry.

Battelle Memorial Institute has again agreed to subscribe $1500 toward this project and, with an additional $6500 which should be appropriated by the Society, an amount of $8000 will again be available for the coming year's work.

Malleable Research—Since December 1950 the Malleable Research Committee has sponsored a research project at the University of Wisconsin, comprising a fundamental study of the effect of melting conditions, primarily furnace atmospheres, on the behavior of the resulting malleable iron. A total of $18,000 has been expended on this research up to the present time.

Three annual research progress reports have been presented at Annual Conventions. For the past two years $6500 a year has been made available for this project. Continuation of the research is recommended by the committee, as the result of a unanimous vote at its meeting June 12. Numerous instances are on record where foundrymen have found the findings of this investigation of real practical value in meeting everyday problems. In view of the foregoing, it is recommended that the Board of Directors include a sum of $6500 to continue this investigation for one year, beginning in December 1953.

Sand Research—The oldest Society-sponsored research is the sand investigation conducted at Cornell University for a number of years. Although up to a year ago the work was largely fundamental and confined exclusively to behavior at elevated temperatures of two or three sands, the work has now been extended to include sand mixtures actually being utilized in the foundry so as to establish the effect of various commonly used additives. The investigation has been expanded to include not only laboratory tests but also the making of actual test castings in an endeavor to correlate laboratory test information with actual behavior in a mold.

By expanding this investigation to include commercially used foundry sand mixtures, the dilatometer which has been in use for a number of years proved to be inadequate, both because of its original design and normal wear through constant usage. As a result, the Chairman of the Board Research Committee, W. L. Seelbach, approved an expenditure not to exceed $1500 for rehabilitation of this equipment, since its replacement with a new dilatometer would amount to in excess of $4000.

Again this year, a complete progress report covering this research was presented at the Annual Convention of the Society. It is recommended that the Board of Directors approve a sum of $5000 to continue this research, as well as to approve the $1500 expenditure which was made for the repair of the dilatometer.

Steel Research—The Steel Research was initiated in May 1948 at Armour Research Foundation and continued at that institution until last year, when it was decided that the investigation should proceed by requesting the assistance of seven cooperating foundries to actually conduct the hot-tear test under operating conditions. The enthusiasm and results obtained have been gratifying. Since the work has been done at the expense of the cooperating foundries, the only Society money spent on this project during the past year was for the core boxes, which were made at Society expense and supplied to each of the foundries.

A preliminary progress report resulting from this cooperative effort was presented by the Chairman of the Committee during the 1953 Annual Convention. Each of the foundries is continuing to work and it is believed that within the next few months some worth-while information will be developed.

Since no immediate need for funds can be foreseen, it is suggested that no fiscal appropriation be authorized. If, however, during the course of the year the Committee either determines that it would like to initiate a new research or requires funds for the prosecution of the present one, it is suggested that the Board at that time give favorable consideration to their request.

Respectfully submitted,
S. C. MASSARI, *Technical Director*

5-Year Comparative Membership Report

As of June 30, 1949-53, Inclusive

	June 30, 1949	June 30, 1950	June 30, 1951	June 30, 1952	June 30, 1953
Sustaining Members	221	197	186	191	193
Company Members	1,380	1,229	1,218	1,259	1,278
Personal Members	7,508	6,571	6,836	7,574	8,471
Student & Apprentice Members	430	550	499	501	567
Honorary Life Members	69	75	71	79	84
International Members	455	424	396	429	440
TOTAL	10,063	9,046	9,206	10,033	11,033
New Members, 12 Months	1,755	1,787	2,316	1,850	1,947
Resignations	303	484	319	193	265
Delinquent Dropped	1,759	2,286	1,807	813	565
Removed by Death	33	34	32	17	32
Net Gain (Loss) for Year	(340)	(1,017)	158	827	967
Members in Chapters	9,253	8,306	8,534	9,320	10,272

AFS Membership Distribution

Chapter	Sustaining	Company	Personal	Personal (Affiliate)	Personal (Associate)	Student & Apprentice	Honorary	Total
Birmingham	6	29	78	164	7	5	3	292
British Columbia	0	11	26	21	6	2	0	66
Canton District	1	27	36	96	0	0	0	160
Central Illinois	2	8	30	135	2	7	1	185
Central Indiana	5	34	51	164	4	10	1	269
Central Michigan	1	13	45	121	7	0	0	187
Central New York	1	28	60	89	3	0	0	181
Central Ohio	3	20	62	87	5	0	3	180
Chesapeake	3	22	58	40	36	1	8	168
Chicago	23	90	215	414	7	20	7	776
Cincinnati	12	43	91	121	7	3	3	280
Corn Belt	0	8	15	56	2	0	0	81
Detroit	10	45	147	254	15	2	10	483
Eastern Canada	6	43	70	185	20	13	0	337
Eastern New York	1	14	24	67	6	0	0	112
Metropolitan	9	67	166	174	28	10	7	461
Mexico City	0	5	33	21	0	4	0	63
Michiana	2	29	67	225	3	1	0	327
Mid-South	0	10	8	38	0	0	0	56
Mo-Kan	1	6	45	9	2	1	0	64
Northeastern Ohio	21	77	142	394	14	8	9	665
Northern Calif.	4	21	100	89	7	1	0	222
No. Ill. & So. Wis.	3	18	24	73	1	0	1	120
Northwestern Pa.	2	23	52	84	1	1	0	163
Ontario	5	68	129	167	3	1	2	375
Oregon	2	8	27	76	3	0	0	116
Philadelphia	14	45	119	144	9	3	4	338
Quad City	5	35	53	158	3	2	2	258
Rochester	1	9	33	47	1	0	0	91
Saginaw Valley	5	16	17	221	2	13	3	277
St. Louis District	1	35	121	125	8	9	0	299
Southern Calif.	4	49	150	97	15	1	0	316
Tennessee	0	16	49	83	2	2	0	152
Texas	4	30	69	75	6	1	2	187
Timberline	1	6	29	24	4	1	0	65
Toledo	1	18	28	72	1	1	1	122
Tri-State	0	12	56	34	2	0	1	105
Twin City	5	24	69	71	8	3	1	181
Washington	0	6	37	8	11	0	1	63
Western Michigan	2	29	45	172	4	2	1	255
Western New York	0	40	83	102	3	6	1	235
Wisconsin	18	76	110	336	10	10	5	565
TOTAL REGULAR CHAPTERS	184	1213	2869	5133	278	144	77	9898
STUDENT CHAPTERS								
Massachusetts Institute of Technology	—	—	—	1	1	25	—	27
Michigan State College	—	—	—	—	—	39	—	39
Missouri School of Mines	—	—	—	—	3	35	—	38
Northwestern University	—	—	—	—	—	4	—	4
Ohio State University	—	—	—	—	—	18	—	18
Oregon State College	—	—	—	—	—	15	—	15
Pennsylvania State College	—	—	—	—	—	33	—	33
Polytechnic Institute of Brooklyn	—	—	—	—	—	16	—	16
Texas A & M	—	—	—	—	—	13	—	13
University of Alabama	—	—	—	—	—	20	—	20
University of Illinois	—	—	—	—	—	62	—	62
University of Michigan	—	—	—	—	—	56	—	56
University of Minnesota (Inactive)	—	—	—	—	—	10	—	10
University of Wisconsin	—	—	—	—	—	23	—	23
TOTAL STUDENT CHAPTERS	—	—	—	1	4	369	—	374
TOTAL ALL CHAPTERS	184	1213	2869	5134	282	513	77	10,272
Foreign	0	23	389	8	20	15	2	457
Non-Chapter	9	65	103	53	30	2	5	267
Members in Military Service	—	—	—	—	—	—	—	37
GRAND TOTAL	193	1301	3361	5195	332	530	84	11,033

Minutes
Special Meeting 1952-53 Board of Directors
Edgewater Beach Hotel, Chicago—July 28, 1952

(1) *ROLL CALL:*

President I. R. Wagner, *presiding*
Vice-President Collins L. Carter

Present:

Directors:

(Terms expire 1953)
J. J. McFayden
W. L. Seelbach
F. W. Shipley
James Thomson
E. C. Troy

(Terms expire 1954)
H. W. Dietert
J. T. MacKenzie
M. J. O'Brien, Jr.

(Terms expire 1955)
M. A. Fladoes
W. J. Klayer
J. O. Klein
A. D. Matheson
H. G. Robertson

Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari

Absent:

Directors: A. L. Hunt
A. M. Ondreyco
J. O. Ostergren

(2) President Wagner took the chair and announced his intention to carry on vigorously the policies and activities set up by the previous Board of Directors, and asked the cooperation of all Directors and Staff members to that end. He announced that it was his intention as President to continue the policy of holding meetings of the Executive Committee only in cases of emergency requiring immediate decision, and that it would not be his intention to screen through the Executive Committee first, all subjects to be discussed in regular meetings of the Board.

(3) The President then announced that the purpose of the special meeting was strictly for organization of the Executive Committee of the Board, in compliance with the Society's by-laws. He asked the Directors to vote by secret ballot for selection of four members of the Board to serve, together with the President and Vice-President Carter, as an Executive Committee of six members under the by-laws. As a result of the balloting, the 1952-53 Executive Committee was announced as follows:

President I. R. Wagner, *Chairman*
Vice-President Collins L. Carter
Director and Past-President Walter L. Seelbach
Director H. W. Dietert
Director M. A. Fladoes
Director F. W. Shipley

There being no further business to be considered, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY
Secretary-Treasurer

Approved:
I. R. WAGNER, *President*
September 4, 1952

Minutes
Special Meeting 1952-53 Executive Committee
Edgewater Beach Hotel, Chicago—July 28, 1952

(1) *ROLL CALL:*

President I. R. Wagner, *presiding*
Vice-President Collins L. Carter

Present:

Director and Past-President Walter L. Seelbach
Director H. W. Dietert
Director M. A. Fladoes
Director F. W. Shipley
Secretary-Treasurer Wm. W. Maloney

(2) The By-Laws of the Society (Art. X, Secs. 1-9) were read, describing the method by which the Nominating Committee shall be appointed to fulfill its functions for the election of a Vice-President and new Directors of the Society at the 1953 AFS Convention.

(3) The names of various candidates were presented from the lists submitted by 21 eligible Chapters, it being reported that 6 eligible Chapters had not submitted the names of candidates and that 13 Chapters were ineligible under the By-Laws.

(4) In accordance with the By-Laws, 7 members were appointed by the Executive Committee to form, together with Past Presidents Walter L. Seelbach and Walton L. Woody, the 1952-53 Nominating Committee, as follows:

Chairman—Past-President Walter L. Seelbach, President, Superior Foundry, Inc., Cleveland.

Past-President Walton L. Woody, Vice-President, National Malleable & Steel Castings Co., Cleveland.

J. W. Horner, Jr., Vice-Pres., Slack-Horner Brass Mfg. Co., Denver, Colo. (Rep. Timberline Chapter and Brass & Bronze)

T. T. Lloyd, Vice-Pres., Albion Malleable Iron Co., Albion, Mich. (Rep. Central Michigan Chapter and Malleable)

W. D. McMillan, Met., International Harvester Co., Chicago. (Rep. Chicago Chapter, and Gray Iron and Malleable)

Earl M. Strick, Fin. Supt., Erie Malleable Iron Co., Erie, Pa. (Rep. Northwestern Pennsylvania Chapter, Malleable and Gray Iron)

Porter Warner, Jr., Vice-Pres., Porter Warner Industries, Inc. Chattanooga, Tenn. (Rep. Tennessee Chapter and Supplies)

E. G. White, Plant Engr., Crouse Hinds Co., Syracuse, N. Y. (Rep. Central New York Chapter, and Gray Iron and Aluminum & Magnesium)

J. A. Wotherspoon, Pres., J. A. Wotherspoon & Son Ltd., Oakville, Ontario, Can. (Rep. Ontario Chapter and Gray Iron)

There being no further business to be considered, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY
Secretary-Treasurer

Approved:
I. R. WAGNER, *President*
September 4, 1952

Minutes
First Meeting 1952-53 Board of Directors
Edgewater Beach Hotel, Chicago—July 29, 1952

(1) *ROLL CALL:*
President I. R. Wagner, *presiding*
Vice-President Collins L. Carter

Present:
Directors:

(Terms expire 1953)
J. J. McFadyen
W. L. Seelbach
F. W. Shipley
James Thomson
E. C. Troy

(Terms expire 1954)
H. W. Dietert
J. T. MacKenzie
M. J. O'Brien, Jr.

(Terms expire 1955)
M. A. Fladoes
W. J. Klayer
J. O. Klein
A. D. Matheson
H. G. Robertson

Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari

Absent:
Directors: A. L. Hunt
A. M. Ondreyco
J. O. Ostergren

(2) Election of Secretary-Treasurer

On recommendation of a Nominating Committee comprising Vice-President Carter as Chairman, Directors Seelbach and MacKenzie, the incumbent Secretary-Treasurer, Wm. W. Maloney, was declared re-elected for the fiscal year 1952-53.

(3) Fixing of Salaries and Compensations

In executive session, with members of the Staff excluded, the Board of Directors established salaries and compensations for various members of the National Office Staff for the fiscal year 1952-53.

(4) Budget of Estimated Income and Expense

The Treasurer presented a Budget of Estimated Income and Expense covering the fiscal year 1952-53, as recommended by the Finance Committee. Each item was discussed in detail in the light of the two-year budget established in July 1951. On motion duly made, seconded and carried, the Budget of Estimated Income and Expense for 1952-53 was approved.

(5) Standing and Special Committes

At the request of the President, motion was duly made, seconded and carried, authorizing the President to appoint standing and special committees in accordance with the By-Laws (Art. VII, Sec. 5).

(6) National Castings Council Representatives

The President recommended, and on motion duly made, seconded and carried, the Board approved the appointment of Vice-President Carter to serve with President Wagner as the two AFS representatives to the National Castings Council. The Secretary was instructed to notify the Secretary of the Council accordingly.

(7) Recommendations of 1951-52 Board of Directors

(a) *Safety & Hygiene and Air Pollution.* The Secretary reviewed briefly steps toward the raising of $350,000 as a ten-year goal for the Safety & Hygiene and Air Pollution Program, stating that a total of approximately $42,000 had been contributed to date. He requested certain revisions in the method of solicitation and on motion duly made, seconded and carried, the Secretary's recommendations were approved.

(b) *Building Project.* No standing recommendations of the 1951-52 Board. The Secretary announced that, in accordance with approval of the Board voted in December 1951, the Secretary had employed an Administrative Assistant in the interest of advancing the Headquarters Building Project.

Past-President Seelbach recalled action by the Board of Directors in meeting May 4, 1952, authorizing the use of AFS reserve funds for financing of the Building Project to conclusion, to a maximum total cost of $200,000. He urged that the Board, at a later date, discuss fully a method for reimbursing the General Reserve Fund for any withdrawals made on behalf of the Building Project.

(c) *Recommendations of the Technical Director.* Certain recommendations of the Technical Director were considered and actions taken as follows:

(1) The Technical Director pointed out that increased work in the casting of titanium alloys had suggested the advisability of changing the name of the Aluminum and Magnesium Division to the Light Metals Divison, so as to include titanium. Without motion, the Technical Director was requested to work out with the Division involved a proper name and acceptance thereof.

(2) The Technical Director requested authority to continue the use of voluntary recorders rather than stenotype reporters at technical sessions of the 1953 Convention. Recommendation accepted on motion duly made, seconded and carried.

(3) The Technical Director pointed out that recommendations of the Publications Committee, and the Budget of Income and Expense for 1952-53 as approved by the Board, did not include the expense of publishing a comprehensive list of Sand Abstracts prepared from all issues of TRANSACTIONS to date, and urged that this publication be included. Director MacKenzie suggested microfilming of the material in lieu of publication. The Technical Director then indicated that he would endeavor to work out some arrangement with the Crerar Library of Chicago whereby the material might be reproduced and made available for AFS use.

(4) The Technical Director requested permission to publish the roster of National Committee Personnel annually instead of biennially, in view of constant changes occurring. Without vote, it was agreed that the recommendation was acceptable.

(5) The Technical Director suggested that some discussion might be held on limiting the number of Annual Convention preprints, such as the assignment of a certain number of preprints to each Division. He pointed out that the preprint cost at the 1952 Convention had exceeded $11,000. Brief discussion followed with no action taken to change the previous method of printing and distribution.

(d) *Distribution of Financial Statements.* The recommendation of retired Director Sefing was discussed, calling for distribution of comprehensive financial statements to the membership. The Directors agreed that the membership should be kept better informed of the Society's finances, but suggested that statements of Income and Expense should cover two-year periods. It was also pointed out that Balance Sheets and Condensed Statements of Income and Expense are published annually in the TRANSACTIONS.

(e) *Annual Lecture.* Motion by Director Eagan calling for the establishment of rules and regulations for the Charles Edgar Hoyt Annual Lecture was discussed and, without vote, the matter referred to the President for further discussion with the Annual Lecture Committee. It was the consensus that a better definition of the purpose of the lecture and the selection of lecturers was worthwhile.

(f) *Recommendations of the Finance Committee.* The following recommendations of the Finance Committee, associated with and/or contingent upon the Estimated Budget of Income and Expense for 1952-53, were acted upon by the Board of Directors:

(1) Recommendation that the method of accounting for dues income and dues refunds to Chapters, on an accrual basis since 1949, be discontinued as recommended by the Staff and the Society's auditors, and that a cash accounting basis be re-established effective July 1, 1952. Recommendation approved on motion duly made, seconded and carried.

(2) Recommendation that the Society's three presently maintained bank accounts with the Royal Bank of Canada at Montreal, Toronto and Vancouver, established for the deposit of Canadian member dues in Canadian funds, be

discontinued and unused cash in such accounts be employed in accordance with the approved investment policy of the Society. Recommendation approved on motion duly made, seconded and carried, the Secretary to notify the Canadian Chapters accordingly.

(3) Recommendation that registration fees for the 1953 Convention in Chicago be the same as in 1951-52, namely, $2.00 for members and $5.00 for guests, with the usual provision for advance registration of Company and Sustaining member employees at the member rate. Recommendation approved on motion duly made, seconded and carried.

(4) Recommendation that unused cash in the Society's general fund as of June 30, 1952, be disbursed as follows: (a) Sufficient amount to be retained in cash for operating funds during the 18-month period, July 1, 1952-January 1, 1954, amount to be determined by the Finance Committee; and (b) balance to be invested in accordance with the approved investment policy of the Society. Recommendation approved on motion duly made, seconded and carried.

(5) Recommendation that commissions for AMERICAN FOUNDRYMAN advertising representatives be revised, effective with the July 1952 issue. No Board action, the matter being left to the discretion of the Secretary.

(6) Recommendation that the 1954 Chapter Officers Conference be held as two separate meetings, Program Chairmen to be invited to a June 1954 meeting, and Chapter Chairmen to be invited to a September 1954 meeting, both on an expense-paid basis as in the past. Recommendation approved on motion duly made, seconded and carried, with the understanding that AFS Directors would be invited primarily to attend the September meeting with Chapter Chairmen. Action included holding the 1953 Chapter Officers Conference on the same basis as in 1952.

(7) Recommendation that ladies entertainment activities at the 1953 Convention of the Society be returned to "Host Chapter" responsibility, subject to agreement by the Chicago Chapter. No Board action, the matter being left for development by the Secretary.

(8) Recommendation that the Student Delegate Plan, inaugurated at the 1952 International Foundry Congress under joint sponsorship of AFS and the Foundry Educational Foundation, be continued biennially in exhibit years in accordance with plans to be worked out jointly by AFS and F.E.F. Recommendation approved on motion duly made, seconded and carried, subject to development of F.E.F. agreement and cooperation.

(9) Recommendation that the Society apply immediately for reclassification under the Internal Revenue Code of 1937 from Section 101 (7) to Section 101 (6), in the interest of income tax considerations of contributors. Recommendation approved on motion duly made, seconded and carried, with the understanding that the effect of such reclassification on the AFS Retirement Plan be studied with reference to Social Security coverage, with possible future recommendations thereon.

(10) Recommendation that the Safety & Hygiene and Air Pollution Program, for purposes of accounting, be charged with all direct salaries of full time Program employees, and a conservative proportion of other salaries and burden items; and that the expense of S&H&AP Program publications, and the income from sales thereof, be accounted for within the general funds of the Society. Recommendation approved on motion duly made, seconded and carried.

(11) Recommendation that display advertising rates in AMERICAN FOUNDRYMAN be studied by the Secretary in terms of increased publication costs toward the end of specific recommendations to the Board of Directors at the November 1952 meeting of the Board. Recommendations accepted, without vote.

In addition to the above recommendations and actions, the following instructions were given the Secretary: (1) That a statement of AFS finances be prepared for use by the Directors in their visits and talks to Chapters during 1952-53; (2) That a statement defining the purposes of the Charles Edgar Hoyt Annual Lecture be prepared for approval by the Board of Directors at the November 1952 meeting.

(g) *Recommendations of the Finance Policy Committee.* Recommendations of the Finance Policy Committee, adopted in meeting July 10, 1952, were presented and discussed in detail. Actions taken by the Board of Directors have the effect of establishing financial policies of the American Foundrymen's Society, superseding any previous informal policies in effect due to precedent and use. Approved by the Board of Directors on motion duly made, seconded and carried.

(Note: All AFS established policies, as adopted, are shown elsewhere in this volume of TRANSACTIONS.)

In addition to the above actions, the Board of Directors, without vote, expressed the following as closely associated with the establishment of financial policies:

(a) It was the consensus of the Board that no permanent long-range financial policy for the Society need be established at this time, the purposes of such long-range planning being considered adequately covered by the policies herein established.

(b) The Board of Directors strongly urged that a "Manual of AFS Policies" be developed by the Secretary following approval of policies by the Board of Directors, and that such manual include definite information on the initiation of new policies and the revision of policies previously established.

(h) *Recommendations of the Technical Policy Committee.* The Technical Policy Committee submitted for consideration by the Board of Directors a recommendation that certain sections of the present AFS by-laws, and certain existing practices accepted as policy by use and precedent, should be set down and approved by the Board of Directors as established policies. In view of the fact that the report of the Technical Policy Committee was not in the form of specific recommendations, no action was taken by the Board. It was the consensus that specific recommendations should be taken up by a Technical Policy Committee and submitted for approval, as established policies of the Society, at a nearby meeting of the Board of Directors.

Concluding the discussions regarding Society policies, Past-President Seelbach urged the Secretary to set up policy committee meetings in the near future so that as many policies as possible could be determined by the Board of Directors at the scheduled November 1952 meeting.

(8) Approval of Resolutions

The Secretary requested Board approval of the following resolutions required for conducting the affairs of the Society, and on motion duly made, seconded and carried, resolutions were approved as follows:

(a) RESOLVED that resolutions required by the Harris Trust & Savings Bank of Chicago, authorizing the withdrawal of Society funds, are hereby approved and the Secretary authorized to certify thereto.

(b) RESOLVED that checks for the withdrawal of funds deposited in the name of the Society with depository banks, including all General Checking Accounts and Interest Savings accounts, and for the disposition of all securities held in the various funds of the Society by the Trust Department of the Harris Trust & Savings Bank of Chicago, shall require the signatures of any two of the following Officers: President, Vice-President, Secretary-Treasurer, Technical Director.

(c) RESOLVED that the Secretary be authorized to maintain a safety deposit box in the name of the Society at the Harris Trust & Savings Bank of Chicago for the safekeeping of Society documents, and that any two of the following have authority to obtain access to such safety deposit box: President, Vice-President, Secretary-Treasurer, Technical Director.

(d) RESOLVED that resolutions required by the Royal Bank of Canada, Montreal, Quebec, and by its subsidiary banks at Toronto, Ontario, and Vancouver, B. C., establishing checking accounts in the name of the Society and authorizing the withdrawal of funds therefrom, are hereby approved and the Secretary authorized to certify thereto.

(e) RESOLVED that the Treasurers of the Eastern Canada, Ontario, and British Columbia Chapters of the Society are authorized to make deposits, in the Royal Bank of Canada and its subsidiary banks, of funds received from Canadian members in payment solely of membership dues in the Society, properly and promptly accounting for same to the Secretary of the Society.

(f) RESOLVED that checks for the withdrawal of funds deposited in the name of the Society with Canadian depository banks shall require the signatures of any two of the following Officers: President, Vice-President, Secretary-Treasurer, Technical Director.

(g) RESOLVED that the Treasurers of the Eastern Canada, Ontario and British Columbia Chapters of the Society shall each give evidence to the Secretary of the Society of the existence of Indemnity Bonds covering each of them individually

and separately, in amounts deemed satisfactory to the Society, premiums thereon to be paid by the respective Chapters.

(h) RESOLVED that the Secretary be authorized to execute all contracts for the administration of Society affairs, subject to specific approval by the Board of Directors. In the case of AFS-sponsored research projects, approval of the project by the Board of Directors includes authority for the Secretary to execute contracts for performance of such projects on a bid basis.

(i) RESOLVED that the Secretary be authorized to reimburse traveling expenses for members of the Society in attendance at any regularly called Board of Directors, Executive Committee, or Technical Committee meeting, with the following exceptions: No expenses shall be paid to Directors or Committee members for attendance at meetings held during the week of the Annual Convention of the Society, unless specifically authorized by the Board of Directors. When meetings are held in conjunction with other committees or associations, the Secretary is authorized to determine what portion of the expense of such attendance shall be paid by the Society.

(j) RESOLVED that the Secretary be authorized to negotiate the compensations of necessary Staff employees below $6000 per year, and that Finance Committee approval be required on all compensations of $6000 per year or more.

(k) RESOLVED that Indemnity Bonds be maintained covering responsible financial officers of the Society as follows: (1) Blanket Position Indemnity Bond covering all Staff members in the amount of $5,000; (2) additional Indemnity Bonds in the amount of $15,000 each covering the Bookkeeping Staff; (3) additional Indemnity Bonds in the amount of $45,000 each covering the Secretary-Treasurer and Technical Director; (4) additional Indemnity Bonds in the amount of $20,000 each covering the President and Vice-President of the Society. All premiums for such Indemnity Bonds to be paid by the Society.

(9) New Business

(a) *Chapter Contacts Committee.* The Secretary was instructed to prepare for Vice-President Carter, as Chairman of the Chapter Contacts Committee, a letter indicating items to be stressed by members of the Committee on Chapter visits, such letter to be developed prior to October 1.

(b) *Director Certificates.* At the suggestion of Director Troy, the Secretary was instructed to proceed with the development of an appropriate certificate in recognition of the service of retiring Directors of the Society.

(c) *Chapter Request.* The Secretary presented a request of the Northeastern Ohio Chapter seeking reimbursement in the amount of $300 covering over-expense in staging a reception in Cleveland on behalf of International Study Tour participants prior to the International Foundry Congress. It was pointed out that no other Chapter or local foundry group, participating in similar pre-Congress receptions, had made a similar request, and it therefore was the concensus of the Board that reimbursement could not be undertaken for one without doing the same for all. On motion duly made, seconded and carried, the Secretary was instructed to decline the request with regrets.

(d) *Bonding of Chapter Treasurers.* The Secretary stated that a request had been made at the 1952 Chapter Officers Conference that the Society consider blanket national bonding of all Chapter Treasurers and that he prepare information on the cost. It was the consensus of the Board that the national organization should not interfere with such internal affairs of the Chapters and, without vote, the Secretary was instructed to proceed accordingly.

(e) *Centennial of Engineering.* The Secretary presented a request from the Centennial of Engineering, to be celebrated in Chicago in the fall of 1952, for some fund or contribution to help defray expenses of the Centennial. Several Directors pointed out that a number of AFS member companies had been approached by the Centennial and had already agreed to contribute to the event either financially or by active participation. Without vote, the Secretary was instructed to notify the requesting organization accordingly.

(f) *Saugus Iron Works.* The Secretary presented briefly the suggestion of a member that AFS should consider participation in the restoration of the Saugus Iron Works in Saugus, Massachusetts, where the first casting in America was made. It was pointed out that the work now is being sponsored largely by the American Iron and Steel Institute in conjunction with the Historical Society of Massachusetts. The Directors declined to authorize financial participation at this time.

(g) *Offer of Medal Award.* The Secretary reported conversations held with past AFS Director Thomas W. Pangborn, who had expressed willingness to consider the endowment of an AFS Medal Award, subject to approval by the Society. It was the firm consensus of the Board that Mr. Pangborn should receive the thanks of the Society for his generosity and interest, and on motion duly made, seconded and unanimously carried, the President and Secretary were instructed to contact Mr. Pangborn on behalf of the Board of Directors.

The Secretary pointed out that the by-laws are not entirely clear as to whether acceptance of proferred awards by the Society should be made by the Board of Directors or by the Board of Awards. It was the consensus of the Board of Directors that the initiation and acceptance of all Society awards is a province of the Board of Directors, but that the granting of all awards to members of the industry is a province of the Board of Awards.

(h) *1955 Convention.* Director Klein presented an official invitation from the Texas Chapter for AFS to hold the 1955 Convention of the Society in Houston, Texas. He pointed out the Texas Chapter had previously issued its invitation for 1953 but, in view of the selection of Chicago for that year, was now renewing the invitation for 1955. The Secretary pointed out that similar desires for the 1955 Convention had also been received from the New England Foundrymen's Association in Boston and the Eastern Canada Chapter of AFS in Montreal. Action on the three invitations was deferred until a later date.

(i) *Retirement Trustees.* A letter from Past Director B. L. Simpson, as Chairman of the Retirement Trustees, was presented recommending that the AFS Secretary-Treasurer be made a signator on bank resolutions for the withdrawal of funds in the Retirement Trust Account, two signatures of trustees now being required for all such withdrawals. Without vote, the matter was referred to the Retirement Trustees for decision and action.

(10) 1952-53 Nominating Committee

For the information of the Board, President Wagner reported that the Executive Committee, in special session the afternoon of July 28, had appointed seven members of the Society to serve with the two immediate Past Presidents as the 1952-53 Nominating Committee, as follows:

Chairman, Past-President Walter L. Seelbach, President, Superior Foundry, Inc., Cleveland.

Past-President Walton L. Woody, Vice-President, National Malleable & Steel Castings Co., Cleveland.

J. W. Horner, Jr., Vice-Pres., Slack-Horner Brass Mfg. Co., Denver, Colo. (Rep. Timberline Chapter and Brass & Bronze)

T. T. Lloyd, Vice-Pres., Albion Malleable Iron Co., Albion, Mich. (Rep. Central Michigan Chapter and Malleable)

W. D. McMillan, Met., International Harvester Co., Chicago. (Rep. Chicago Chapter, and Gray Iron and Malleable)

Earl M. Strick, Fin. Supt., Erie Malleable Iron Co., Erie, Pa. (Rep. Northwestern Pennsylvania Chapter, and Malleable and Gray Iron)

Porter Warner, Jr., Vice-Pres., Porter Warner Industries, Inc. Chattanooga, Tenn. (Rep. Tennessee Chapter and Supplies)

E. C. White, Plant Engr., Crouse-Hinds Co., Syracuse, N. Y. (Rep. Central New York Chapter, and Gray Iron and Aluminum & Magnesium)

J. A. Wotherspoon, Pres., J. A. Wotherspoon & Son, Ltd., Oakdale, Ontario, Can. (Rep. Ontario Chapter and Gray Iron)

(11) Next Board Meeting

President Wagner announced that the next meeting of the Board of Directors would be held November 17-18 at The Cloister, Sea Island, Ga., and that the Secretary in due time would seek reservation and attendance information from the members of the Board.

(12) Schedule of Events

The President presented, for the information of the Board, a Schedule of Official Events for the fiscal year 1952-53 and urged that all place the pertinent dates on their calendars.

There being no further business to be considered, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY
Secretary-Treasurer

Approved:
I. R. WAGNER, *President*
September 4, 1952

Minutes
Meeting of AFS Board of Awards 1952-53
Minneapolis—September 29, 1952

(1) *ROLL CALL:*

Past President F. J. Walls, *presiding*
Past President S. V. Wood
Past President Max Kuniansky
Past President W. B. Wallis
Past President E. W. Horlebein
Past President Walter L. Seelbach
Wm. W. Maloney, Secretary

Absent: Past President Walton L. Woody

2. Chairman Walls called the meeting to order and expressed the pleasure of all at meeting with Past President Wood, and thanked him for extending the hospitality of his home for the meeting. Mr. Wood expressed his regrets that illness had prevented his attending recent meetings of the Board of Awards and stated his great pleasure in having the group meet in Minneapolis.

3. The Chairman stated that the meeting was called for preliminary consideration of possible candidates for recognition, and announced that the regular meeting of the Board of Awards would be held in Chicago on Thursday, December 11.

4. Minutes of the 1951 meeting on awards were read, as well as actions by the Board of Directors. It was pointed out that suggestions for revision of the Awards Manual had not been approved by the Board of Directors in two particulars: (1) Changing terminology to substitute the word "Candidate" for the word "Nomination" wherever the latter appears, and (2) eliminating the present provision in the By-Laws that actions of the Board of Awards must be approved by the Board of Directors.

5. It was pointed out that the Awards Manual calls for inviting nominations from the Board of Awards, Board of Directors, the Chairmen of the technical Divisions and General Interest Committees, the Chapters, and the membership (the latter through publication in AMERICAN FOUNDRYMAN). After considerable discussion, it was agreed that names of candidates for recognition at the 1953 Convention would be obtained from previous lists on file, and from additional names presented by the Board of Awards.

It was the consensus that the present Awards Manual should be studied and revised in the light of experience with its use during the past three years.

Members of the Board of Awards then submitted names of additional candidates to be considered at the December 11 meeting.

6. The Secretary presented information on an offer to endow a sixth Gold Medal of AFS and stated that, while the offer had been accepted by the Board of Directors, parallel favorable action by the Board of Awards seemed advisable. On motion duly made, seconded and unanimously carried, the Board of Awards took the following action:

RESOLVED, that the Board of Awards of the Society subscribes whole-heartedly to the action of the AFS Board of Directors in acceptance of a sixth Gold Medal endowment on the same basis as other Gold Medal endowments of the Society.

7. Following discussion as to the number of medal awards to be accepted by the Society, the following recommendation was made on motion duly made, seconded and carried: The Board of Awards unanimously recommends that Gold Medal awards accepted by the Society be limited to a total of six, and that future bequests and similar offers be directed toward the support or founding of other activities or projects of the Society.

8. The Board of Awards discussed the number of medal awards and life memberships to be bestowed in 1953 and on motion duly made, seconded and carried, determined THAT not more than three Gold Medals should be awarded in 1953, and not more than three Honorary Life Memberships, including the AFS retiring President, be granted in 1953.

It was agreed that all awards presented at the 1953 Convention would be presented in the same manner as at the 1952 Convention.

9. Members of the Board of Awards expressed a desire that the present Certificate for Gold Medalists and Honorary Life Members be the same (large) size and made more attractive. The Secretary was requested to proceed accordingly.

10. The Board of Awards recommended preparation of a standard form of plaque, statuette or similar device (but not a medal) for presentation to the Charles Edgar Hoyt Annual Lecturer.

Without vote, the Board of Awards expressed its belief that selection of the Annual Lecturer should not be confined strictly to the AFS membership, and that the subject of the lecture need not be confined strictly to foundry subjects. It was the consensus that the lecture subject might well be of a technical nature in non-exhibit years, and of greater managerial appeal in exhibit years.

11. It was agreed that, promptly after the December meeting of the Board of Awards, steps would be taken to develop a continuous investigation of likely award candidates, such investigations to be carried on by the Board of Awards and the AFS Presidential nominee, to the end that the Board of Awards alone will be responsible for detecting and considering award candidates.

12. There being no further business to be considered, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY
Secretary

Minutes
Meeting of A.F.S. Finance Committee
Cleveland—October 28, 1952

(1) *ROLL CALL:*

Past President Walter L. Seelbach, *presiding*
President I. R. Wagner
Vice-President Collins L. Carter
Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari
Harry L. Parker, Trust Dept.,
Harris Trust & Savings Bank
Paul N. Mitchell, Trust Dept.,
Harris Trust & Savings Bank

(2) Chairman Seelbach called the meeting to order and announced the purpose of the meeting was to receive from the representatives of the Harris Trust and Savings Bank, Chicago, proposals for handling the investment of AFS surplus funds, as called for in the Finance policies of the Society, approved by the Board of Directors July 28, 1952. The Chairman introduced Messrs. Parker and Mitchell to make the presentation.

(3) The members of the Finance Committee asked a number of questions concerning the proposed program, after which Messrs. Parker and Mitchell were excused.

(4) Thereafter the Finance Committee expressed some satisfaction with the proposals presented by the bank representatives, but also expressed a desire to receive a similar proposal from a second investment counsel. The Secretary was instructed to arrange such a meeting with the Finance Committee and Trust Department officials of the Northern Trust Co. in Chicago on November 10, 1952.

(5) There being no further business to be considered, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY
Secretary

Minutes
Fall Meeting 1952-53 Board of Directors
Sea Island, Ga.—November 17-18, 1952

(1) *ROLL CALL:*

President I. R. Wagner, *presiding*
Vice-President Collins L. Carter

Present:
Directors:

(Terms expire 1953)	*(Terms expire 1954)*
J. O. Ostergren	H. W. Dietert
W. L. Seelbach	A. L. Hunt
F. W. Shipley	J. T. MacKenzie
E. C. Troy	A. M. Ondreyco

(Terms expire 1955)
M. A. Fladoes
W. J. Klayer
J. O. Klein
A. D. Matheson
H. G. Robertson

Secretary-Treasurer Wm. W. Maloney
Technical Director S. C. Massari

Absent:
Directors: J. J. McFadyen
M. J. O'Brien, Jr.
James Thomson

(2) Reading of Minutes

Minutes of the first meeting of the 1952-53 Board of Directors, held in Chicago, July 29, 1952, were read as a refresher, such minutes having been previously approved by letter ballot of the Board. Minutes of the Special Meeting of the Board and the Special Meeting of the Executive Committee, both held July 28, 1952, were referred to, but not read, both having previously been approved by letter ballot of the Board. Minutes of the meetings of the Finance Committee held October 28 and November 10, 1952, were read and on motions duly made, seconded and carried, were approved.

(3) Reports of Staff Officers

(a) *Report of the Secretary.* The Secretary reported that membership of the Society on October 31 totaled 10,423, compared with 10,066 on June 30, 1952, leaving 577 members still to be obtained to reach the goal of 11,000 for the current fiscal year. He pointed out that 583 members had been added during the 4-month period, July-October, and a total of 226 members dropped for all causes, a net gain of 357. This compares with a net gain of 115 during the first four months of the previous year.

He stated that most Chapters were showing strong interest in membership targets, that five Chapters had exceeded their year's targets by October 31, but that 10 Chapters were below their June 30 totals. The Directors were asked to stress membership in their Chapter contacts work.

The Secretary stated that a new Chapter was imminent at Memphis, Tenn., the Mid-South Chapter, and would constitute the 42d regular Chapter of the Society. He also reported on Chapter possibilities in other sections of the country.

On AMERICAN FOUNDRYMAN, the Secretary reported gross income of $80,672 for the 5-month period, July-November, 1952 from 274 pages of advertising, compared with 224 pages and $60,600 gross for the same period a year ago, both representing new highs for comparable periods.

The Secretary reported a total of $43,350 in contributions to the Safety & Hygiene and Air Pollution Fund to November 12, and stated that the National Office was carrying on campaign direct with prospective contributors. He announced that new bases for contributions had been arranged, new brochures printed and distributed, and that the committees of the Program were all actively undertaking their assigned work.

Brief reports also were presented on the Building Project, educational activities, staff organization and overall financial matters.

(b) *Report of the Treasurer.* The Treasurer presented statements of Income and Expense, Balance Sheet, Publication Sales, and Official Travel and Expense for the period July 1-October 31, 1952. He pointed out that for the 4-month period, Income totaled $159,626.42 (forecast $146,480) and Expense totaled $181,714.20 (forecast $177,569.20) or an Excess Expense of $22,087.78 compared with the forecast of $31,089.20 Excess Expense. Major variations in items of Income and Expense were discussed in detail, the conclusion being reached that the finances of the Society were well in hand as of October 31.

Separate statement of income and expense for the S&H&AP Fund was also presented showing a balance of $25,544.95 on October 31 (total expense July 1-October 31, $4,455.45 net).

(c) *Report of the Technical Director.* The Technical Director reported that the name of the Aluminum & Magnesium Division has now been changed to the Light Metals Division, as approved by previous action of the Board. He also reported publication and distribution of a new edition of the National Committee Personnel Roster. A report was presented on progress of the 1953 Convention Technical Program and Apprentice Contests. Items of interest include sessions on Shell Molding, a Symposium on Sand Reclamation, and presentation of a Charles Edgar Hoyt Annual Lecture.

On publications, the Technical Director reported that the 1952 TRANSACTIONS of approximately 900 pages was nearing completion with over 1000 copies ordered on pre-publication orders, 219 copies requested by Honorary and Sustaining members, and a total of 1500 copies in printing. He reported the completion of additional publications scheduled for completion during the current fiscal year.

The Technical Director reported that work was being carried on in gray iron, heat transfer, light metals, malleable iron, sand, and steel research, and that a brass and bronze research project probably would be under way soon when approved by the Board.

(4) Reports of Board Committees

(a) *Finance Committee.* The Chairman stated that the Finance Committee had met in Chicago, October 28, to receive proposals from the Harris Trust & Savings Bank of Chicago for the investment of available AFS cash, as called for in the finance policies of the Society approved by the Board. Following that meeting, the Committee had a similar conference on November 10 with representatives of the Northern Trust Co. of Chicago. As a result, the Finance Committee unanimously selected the Harris Trust & Savings Bank to initiate the AFS investment program under the new finance policy, for investment of $300,600, including (a) $220,600 in U. S. Government securities, (b) $48,000 in general reserve fund cash, and (c) approximately $32,000 cash held in three Canadian bank accounts.

On motion duly made, seconded and carried, the actions of the Finance Committee were approved by the Board of Directors.

(b) *Chapter Contacts Committee.* Chairman Carter asked the Directors to report on their contacts with the Chapters and urged them to make their Chapter visits at the earliest possible date. He also requested that they continue to concentrate on membership and program activities.

(c) *Annual Lecture Committee.* The Secretary pointed out that by Board action in July 1952, the Annual Lecture Committee was requested to define its policies for approval from a Committee of the Board. Accordingly, the following recommendations of the Committee were presented:

1. The Charles Edgar Hoyt Annual Lecture Committee shall select each year an individual to be honored with the preparation and delivery of this Lecture during the Annual Convention of the Society.

2. The Lecturer shall preferably be a member of the American Foundrymen's Society, but in any case shall be one associated with the Castings Industry.

3. In the non-exhibit years, this Lecture shall comprise some current technological problem within the Industry, and shall include new developments not yet publicly disclosed.

4. In an exhibit year, the Lecture shall be of a managerial nature or covering broader technological advances in the Industry.

5. Particularly in the case of a technical lecture, since it may involve the results of current research, the Committee shall select the Lecturer two years in advance of the time of delivery.

6. The Committee recommends that the Board of Directors authorize the presentation through the Board of Awards of an Honorary Life Membership in the Society to the Lecturer, as

evidence to him of its appreciation. In the event this recommendation is accepted, the Committee feels that presentation of a Certificate should be made immediately following the Lecture, and the Honorary Life Membership presented at the time of the Annual Banquet when other honors are being bestowed.

7. To impress the nominee of the honor involved, it is recommended that a member of the Committee personally visit the nominee to familiarize him with the importance and type of lecture the Committee desires, as well as other pertinent details relating thereto, including the final date for receipt of manuscript.

It was the consensus of the Board that paragraph 3, should be altered to read: "In the non-exhibit years, this Lecture shall comprise some current technological problem within the Industry, and preferably shall include information not yet publicly disclosed."

On motion duly made, seconded, and carried, the policy recommendations of the Annual Lecture Committee were accepted with the alteration indicated.

It was pointed out that the Committee's recommendation for granting Honorary Life Membership to the Charles Edgar Hoyt Annual Lecturer should be the subject of special action by the Board. Accordingly, on motion duly made, seconded and carried, the Board of Directors agreed to the conferring of Honorary Life Membership on the Annual Lecturer and instructed the Secretary to so inform the Board of Awards.

(d) *Safety & Hygiene and Air Pollution.* Chairman Shipley presented a progress report on the Safety & Hygiene and Air Pollution Program, particularly covering the work of the Steering Committee and the Committees on Safety, Dust & Ventilation, Air Pollution, Welding, and Noise Abatement. He stated that additional subjects for working committees include the development of information on lighting, woodworking safeguards, and the labeling of dangerous materials.

He announced that the first Foundry Safety Course had been held successfully in Chicago, November 6-7, and that the Safety Committee is revising the present Recommended Good Safety Practices for the Foundry Industry. They are also developing the script for a foundry safety film and preparing a program for the 1953 Convention.

The Dust and Ventilation Committee announced production of proceedings of the conference held in April 1952 at the University of Michigan, progress on a manual of dust and ventilation control, and co-sponsorship of foundry health seminars to be held at the University of Illinois and Wisconsin.

For the Air Pollution Committee it was announced that proceedings of the papers presented at the 1952 Convention would be made available in printed form. This Committee is preparing a check list of foundry operations causing air pollution, is revising an air pollution survey, is working on a complete air pollution manual, and is preparing a separate program for the 1953 Convention.

The Welding Committee has completed a code on welding, subject to review by the Steering Committee, although the code will not be produced under the name of American Foundrymen's Society. A final "code" will then be issued as recommended good practice with its terminology revised. It was announced that a Noise Abatement Committee had been organized to study sources of noise and control methods, but not for the present to publicly announce findings or recommendations.

Director Shipley strongly urged that nothing be published by AFS in the nature of Safety & Hygiene and Air Pollution until it has first been approved by the Steering Committee of the program. Director Fladoes suggested the usefulness of a monthly foundry accident report.

(5) Special Reports

(A) *Report of Technical Policy Committee.* Recommendations of the Technical Policy Committee, resulting from a meeting held during the 1952 Convention, were presented and discussed in detail. Actions taken by the Board of Directors have the effect of establishing the following technical policies of the American Foundrymen's Society, superseding any previous informal policies in effect due to precedent and use. Approved by the Board of Directors on motion duly made, seconded and carried, with the congratulations of the Board for a job well done.

(NOTE: All established policies of the Society, as adopted, are shown elsewhere in this volume of TRANSACTIONS.)

(B) *Administration Policy Committee.* Minutes of the meeting of the Administration Policy Committee held in Cleveland October 28, 1952, were read and discussed in detail. Actions taken by the Board of Directors have the effect of establishing the following administration policies of the American Foundrymen's Society, superseding any previous policies in effect due to precedent and use. Approved by the Board of Directors on motion duly made, seconded and carried.

(NOTE: All established policies of the Society, as adopted, are shown elsewhere in this volume of TRANSACTIONS.)

(C) *Housing Committee.* The Secretary presented a detailed report on efforts to obtain a site in DesPlaines, Ill., for a Headquarters Building of AFS. He stated that the suit to force rezoning of the property on Miner St. having been filed October 7, 1952, the City of DesPlaines had responded on November 3 and the hearing was scheduled in the Cook County Circuit Court for December 3.

The Secretary also stated that suggestions had been made by certain AFS members in Michigan favorable toward a piece of property in Ann Arbor owned by one Dr. Owens and adjoining the proposed future engineering campus of the University. It was pointed out that since the original Housing Committee had determined to build a new building and to do so in the Chicago area, any consideration of purchasing the Owens property for remodeling at Ann Arbor, Mich., would have to be determined by a meeting of the full Housing Committee.

The Secretary also recommended that effort be initiated by AFS to invite the Foundry Educational Foundation to obtain quarters in the new Headquarters Building of the Society, in view of the close relationship between educational activities of AFS and F.E.F. He stated that with the purchase of property and design of building now imminent, action should be taken promptly with F.E.F., if acceptable to the Board.

After considerable discussion, motion was made, seconded and carried to table both the Ann Arbor and the Foundry Educational Foundation proposals and suggestions.

(D) *National Castings Council.* President Wagner presented a brief report on the meeting of the National Castings Council held in Cleveland, November 5, and stated that the Council members had reiterated their support of the Safety & Hygiene Program. Another item concerned a strengthening of the industry's representation in Washington under the new national administration.

(I) *1953 Convention.* The Secretary and Technical Director reported on various aspects of the non-Exhibit Convention to be held in Chicago, May 4-8, 1953. He stated that, as usual, the Alumni members would be asked to make their personal hotel reservations direct with the National Office, and that the Staff was seriously considering abandoning the use of a general housing bureau for this annual meeting.

(F) *1953 International Congress.* The Secretary reported on development of several tours for American foundrymen desiring to attend the International Foundry Congress in Paris, September 19-26, 1953, and stated that over 300 persons had indicated interest in the trip. He announced that Exprinter Travel Service, Inc. had been appointed the official tour agents and that all reservations and accommodations would be handled through them with no expense to the Society.

(6) Old Business

(A) AMERICAN FOUNDRYMAN *Advertising Rates.* The Secretary raised the question of advertising rates in AMERICAN FOUNDRYMAN, reminding the Directors that action on rates had been tabled at the July Board meeting pending further investigation by the Staff. Following full discussion, motion was made, seconded and carried that the basic single insertion full page, black-and-white display advertising rates in AMERICAN FOUNDRYMAN be increased by 20 per cent, other rates adjusted accordingly, the effective date to be left to the discretion of the Society Secretary.

(7) New Business

(a) *Mid-South Chapter.* The Secretary presented a petition for formation of a Mid-South Chapter of the Society at Memphis, Tenn., such petition being signed by 65 members of the industry and meeting all other requirements for a Chapter petition. On recommendation of Director Hunt and the Secretary, motion was made, seconded and unanimously carried, approving the petition for formation of the Society's 42d Chapter.

(b) *Restoration of AFS Film.* The Technical Director requested an appropriation not to exceed $1,000 for restoration of the second AFS film, "The Principles of Gating." On motion duly made, seconded and carried, request approved.

(c) *F.E.F. Contribution.* The Secretary presented a letter re-

ceived from the Foundry Educational Foundation reporting action of the trustees for requesting all charter sponsors to contribute in some measure to the finances of the Foundation. Following discussion, on motion duly made, seconded and carried, the Secretary was instructed to inform the Foundry Educational Foundation of its inability to comply with this request, in view of the fact that so many AFS members are already participating in the activities of the Foundation and contributing financially to its success.

(d) *"A Career in Metals" Film.* President Wagner reported a meeting held with the Northwestern Pennsylvania Chapter, at which time it was requested that AFS take over national distribution of the Chapter-developed strip film, "A Career in Metals," the film having already been shown to the Board of Directors. The consensus of the Board was that the film was excellent for showing to boys at the high school and trade school level, but that the Society should not at this time assume the job of national distribution but should assist the sponsoring Chapter to do so itself.

(e) *Educational Recommendations.* The Secretary pointed out that the number of requests for educational assistance at the secondary school had been increasing consistently and presented the following recommendations:

(1) There exists a close and permanent relationship between educational activities at the college and engineering school level and activities at the secondary school level, and that an aggressive program at both levels is a joint concern of both AFS and F.E.F.

(2) At present, by action of their respective governing bodies, AFS and F.E.F. have agreed that F.E.F. will confine itself to the college and engineering school level and that the AFS field is in the secondary school level. The successful work of F.E.F. with a full-time staff devoted exclusively to eduction is well known.

(3) AFS is constantly urging its Chapters to undertake educational work and the requests for assistance from the Chapters and elsewhere are constantly increasing.

(4) The current budget includes a token amount for educational expense, entirely insufficient to undertake a comprehensive program at the secondary school level.

(5) Since the long range success of the F.E.F. program entails recruitment from the secondary schools, it may be questioned whether a program at the secondary school level should be carried on exclusively by AFS, rather than jointly by AFS and F.E.F., or even with educational activities at all levels under F.E.F. In either event, the closest cooperation between AFS and F.E.F. is essential, greater degree than has been possible thus far.

(6) The need for closer cooperation makes highly advisable a prompt decision as to whether F.E.F. should be invited to occupy space in conjunction with the new AFS Headquarters Building.

(7) In view of the close community of interest in educational matters between AFS and F.E.F., it might be logical to consider appointment of the F.E.F. Staff Director as an Educational Advisor to the AFS Board.

(8) Since the Chairman of the AFS Educational Division has recommended the engagement of a full-time Educational Director for the Society, action by the Board is called for on this recommendation.

Following discussion of the above recommendations, on motion duly made, seconded and carried, the Secretary was authorized to employ a ful-time Educational Director for carrying on the Society's educational activities at the secondary school level.

(f) *Directorate Reorganization.* The President and Secretary referred to action taken previously by the Board, approving a recommendation of the Administration Policy Committee "that the present size and representation of the Society's directorate should be studied with the view to securing better regional representation." It was emphasized that the demands upon the time of the President, Vice-President and Secretary for Chapter visits, and the growing size of the membership, made such action imperative.

The Secretary then presented, as directed by the President, a suggested plan for reorganization of the AFS Directorate, the plan involving an increase in the number of Directors, a change in the selection of Directors, establishment of Chapter groups to include non-Chapter foundry areas, establishment of Regions including several Chapter groups, with Regional Vice-Presidents governing each region. It was pointed out that such a plan conceivably would involve less burden upon the President and Vice-President but more official use of their time, better and more consistent Chapter representation on the Board, and better geographical representation on the Board at all times.

President Wagner announced the appointment of a Board Reorganization Committee consisting of M. A. Fladoes, Chairman; F. W. Shipley, and A. L. Hunt. The Secretary was instructed to arrange a meeting of the Committee in the near future to consider the suggested plan.

(g) *Death of Past-Director E. N. Delahunt.* The Secretary announced with regret the receipt of information stating that Past-Director E. N. Delahunt had died suddenly in Montreal on November 4, 1952. In an expression of the sincere regrets of the Directors, the following resolution was moved, seconded and unanimously adopted:

> WHEREAS in the death of Edward N. Delahunt of Montreal on November 4, 1952, the Society and its Canadian Chapters lost simultaneously a true friend and respected associate, and
>
> WHEREAS he had long served the Eastern Canada Chapter as a charter member, Director, and Chairman, and always as a trusted advisor, and
>
> WHEREAS, his service on the Board of Directors during the years 1947-50, and both his local and national activities on behalf of the Society, exemplified the spirit of comradeship that has long existed among all foundrymen in America, and
>
> WHEREAS he possessed a rare combination of sound judgment, fair play, and the encouragement of others, now therefore be it
>
> RESOLVED that this tribute be made a part of the official record of the American Foundrymen's Society and that a copy of this resolution be transmitted to his family and to the Eastern Canada Chapter.

(h) *Technical Assistant.* The Secretary pointed out that he had long recommended the hiring of an additional assistant for Technical Director Massari, the original recommendation dating back to November 1945. He urged that action be taken in view of the Society's plans to step up its present activities. Accordingly, on motion duly made, seconded and unanimously carried, the Secretary was authorized to employ a full time Technical Assistant to the AFS Technical Director.

(8) Next Board Meeting

President Wagner announced that the next meeting of the Board of Directors would be held at the Sherman Hotel, Chicago, on Friday, March 20, 1953.

There being no further business to be presented, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY
Secretary-Treasurer

Approved:
I. R. WAGNER, *President*
January 8, 1953

Minutes
Meeting of 1952-53 A.F.S. Board of Awards

Blackstone Hotel, Chicago—December 11, 1952

(1) *ROLL CALL:*

Past-President Fred J. Walls (1945-46), *presiding*
Past-President W. B. Wallis (1948-49)
Past-President Walton L. Woody (1950-51)
Past-President Walter L. Seelbach (1951-52)

Wm. W. Maloney, Board of Awards Secretary

Absent:

Past-President S. V. Wood (1946-47)
Past-President Max Kuniansky (1947-48)
Past-President E. W. Horlebein (1949-50)

(2) Minutes

Minutes of the preliminary meeting of the Board of Awards held September 29, 1952, were read and accepted. It was pointed out that the Board of Awards on September 29 had voted to bestow not more than three Gold Medals in 1953 and that all awards should be presented at the 1953 Convention in the same manner as at the 1952 Convention.

(3) The Secretary informed the Board of Awards of action by the Board of Directors accepting the recommendation of the Annual Lecture Committee that Honorary Life Membership be awarded to the Charles Edgar Hoyt Annual Lecturer.

(4) Number of Medals and Life Memberships

On motion duly made, seconded and carried, the Board of Awards voted to bestow four Gold Medals of the Society and two Honorary Life Memberships in the Society (including the retiring President and the Annual Lecturer, and in addition to Life Memberships for medalists) in 1953.

(5) Selection of Gold Medalists

The meeting being open for receipt of recommendations, the Secretary first presented a list of candidates for Medal awards as submitted by the AFS Divisions and individual members in previous years. Members of the Board of Awards presented additional recommendations for consideration. After full discussion, the following motions were duly made, seconded and unanimously carried, recommending to the Board of Directors the awarding of Gold Medals of the Society in 1953:

(a) *To Wm. J. Grede* (Pres., Grede Foundries, Inc., Milwaukee, Wis.), the Wm. H. McFadden Gold Medal of AFS "for outstanding public service bringing great credit upon and broad recognition for the entire Foundry Industry."

(b) *To Daniel E. Krause* (Exec. Director, Gray Iron Research Institute, Columbus, Ohio), the John H. Whiting Gold Medal of AFS "for outstanding contributions to the Society and the Foundry Industry in the field of Ferrous metallurgy and research."

(c) *To William Romanoff* (Vice-Pres., H. Kramer & Co., Chicago, Ill., the Joseph S. Seaman Gold Medal of AFS "for exceptional contributions to the Society and its Brass and Bronze Division over many years."

(d) *To James H. Smith* (Gen. Mgr., Central Foundry Div., General Motors Corp., Saginaw, Mich.), the Peter L. Simpson Gold Medal of AFS "for outstanding contributions to the application of engineering principles in foundry practice, and as the 1953 Charles Edgar Hoyt Annual Lecturer."

(NOTE: Decision to award the Simpson Medal to the Annual Lecturer for 1953 was made by the Board of Awards following the meeting and in consideration of donor's belief that the Society is committed to bestowing this medal each year).

On motion duly made, seconded and carried, nominations of Gold Medal awards for 1953 were closed.

(6) Selection of Honorary Life Memberships

Following discussion, motions were duly made, seconded and unanimously carried recommending to the Board of Directors that Honorary Life Memberships be awarded in 1953 as follows:

(a) *To George J. Barker* (Prof. of Met. Engrg., University of Wisconsin, Madison, Wis.), Honorary Life Membership in AFS "for outstanding contributions to the Society and the Castings Industry in the education of young engineers for greater appreciation of the industry, the casting process and cast products as engineering materials."

(b) *To I. Richards Wagner,* Honorary Life Membership in AFS "for outstanding service to the Society as President during the year 1952-53."

(NOTE: It should be pointed out that AFS Gold Medalists automatically are accorded the privilege of Honorary Life Membership, citations therefor being same as the medal citations.)

On motion duly made, seconded and carried, nominations for Honorary Life Membership were closed.

The Board of Awards confirmed its decision of September 29 that awards made in 1953, including the awarding of Medal and Life Membership to the Annual Lecturer, be made at the Annual banquet of the Society in the same manner as in 1952.

(7) Investment of Award Funds

The Secretary pointed out that certain award funds have been invested in securities which now have matured and the proceeds from the sale of such securities should be reinvested to best advantage of the Board of Awards and the Society. He pointed out that the Society has recently adopted an investment policy and also called attention to the award donor agreements, all of which include the following clause: "The Board of Awards of the said Association shall control the disposition of the said fund, together with the income therefrom, subject to the terms and conditions herein set forth. The Board of Awards shall invest and in their discretion reinvest the said funds in securities of the kind and quality designated by the laws of the State of Illinois for the investment of trust funds."

On motion duly made, seconded and unanimously carried, the Board of Awards directed that proceeds from the sale of matured award fund securities should be reinvested promptly by the Finance Committee of the Board of Directors in a manner similar to its handling of the general funds cash accumulated by the Society.

(8) General Considerations

The Board of Awards instructed the Secretary to suggest revision of the existing Awards Manual in terms of Board of Awards deliberations during 1951 and 1952, and instructed that the matter of the investment of funds from the sale of matured award fund securities should be included in the Manual in accordance with the above action by the Board of Awards.

It was confirmed that the September 29 agreement of the Board of Awards toward the determination of candidates for 1953-54 would be carried on by the Board of Awards shortly after January 1, 1953, with the retiring President invited to participate.

Members of the Board of Awards expressed their sincere regrets that Past-Presidents S. V. Wood, E. W. Horlebein, and Max Kuniansky had found it impossible to attend the meeting.

Lists of candidates for consideration by the Board of Awards as previously submitted were studied and the Secretary instructed to prepare an up-to-date list for consideration by the 1953-54 Board of Awards.

Respectfully submitted,
WM. W. MALONEY, *Secretary*
AFS Board of Awards

Approved:
F. J. Walls, *Chairman*
January 27, 1953

Minutes
Meeting of AFS Board of Directors
Hotel Sherman, Chicago—March 20, 1953

(1) *ROLL CALL:*
President I. R. Wagner, *presiding*
Vice-President Collins L. Carter

Directors:
(Terms expire 1953)
Walter L. Seelbach
Frank W. Shipley
James Thomson
E. C. Troy

(Terms expire 1954)
Harry W. Dietert
Albert L. Hunt
J. T. MacKenzie

(Terms expire 1955)
M. A. Fladoes
W. J. Klayer
J. O. Klein
A. D. Matheson

Wm. W. Maloney, Secretary-Treasurer
S. C. Massari, Technical Director

Absent
Directors:
J. J. McFadyen
J. O. Ostergren
M. J. O'Brien, Jr.
A. M. Ondreyco
H. G. Robertson

2. The President announced that a quorum had been established.

(3) Reading of Minutes

Minutes of the meeting of the Board of Directors held November 17-18, 1952 were briefed as to major items, these minutes having been previously approved by letter ballot of the Board. Minutes of the meeting of the Board of Awards held December 11, 1952 were also briefed and 1953 recipients of Gold Medal and Honorary Life Membership awards announced, these minutes having been previously approved by letter ballot of the Board.

Minutes of the meeting of the Nominating Committee held December 12, 1952 were also briefed and names of the Officer and Director nominees announced.

(4) Reports of Staff Officers

A. *Report of the Secretary*

The Secretary reported that the membership on February 28, 1953 totaled 10,821 and predicted a total of at least 11,000 by March 31, compared with the year's goal of 11,000 by June 30, 1953. He stated that great credit was due the Membership Chairman of those Chapters reporting an increase in membership.

On motion duly made, seconded and carried, the Board approved a resolution congratulating those Chapters who have made their targets thus far this year.

The Secretary reported a total of 42 regular and 13 Student Chapters, the University of Minnesota Student Chapter having been dropped because of inactivity since June 30, 1951. He reported installation of Student Chapters at Polytechnic Institute of Brooklyn in January, and the University of Wisconsin the same month.

In reporting on AMERICAN FOUNDRYMAN, the Secretary stated that advertising revenue grossed $137,266 from 472 pages of advertising for the first nine months of the current fiscal year, compared with $114,321 from 422⅝ pages the same period a year ago. He stated, however, that advertising prospects for 1953 did not seem to be exceptional and indicated that the rate of growth in AMERICAN FOUNDRYMAN advertising, as noted during the past two years, is now declining.

He announced that the Dwight Early & Sons organization of publishers representatives was retained in January to handle the mid-Western territory solicitation for the magazine. He also reported that increased advertising rates as voted by the Board of Directors in November 1952 were going into effect with the April 1953 pre-Convention issue, advertisers of record being protected at the old rates through the September issue only.

B. *Report of the Treasurer*

The Treasurer presented Income and Expense statements as of February 28, 1953 showing total income of $327,300, compared with forecasted income of $309,468 for the period; total expense of $345,262, compared with forecast of $354,555; or an excess of expense totaling $17,961 for the period, compared with a forecasted $45,087. It was pointed out that the budgeted excess expense for the fiscal year to June 30, 1953 is $43,210. Major items of departure from the budget of income and expense were analyzed.

The Balance Sheet as of February 28, 1953 reported the reduction of cash from $222,093 on October 31, 1952, to $135,740 on February 28; increase in investments from $220,600 to $299,519; both reflecting increased investments under the investment policy adopted previously. Individual variances in the Balance Sheet were discussed.

Income and Expense Statement for the Safety & Hygiene and Air Pollution Program reported total contributions of $71,391 through February 28, 1953; total expense of $21,922 since inception of the Program; total income $675 from miscellaneous sources; fund balance $50,143 on February 28.

A separate statement of Publication Sales showed income of $29,981 from all publications on February 28, 1953 . . . including $19,719 from special publications or 29 per cent greater than a year ago; $10,011 from general publications, and $251 in royalties from the high school textbook "Foundry Work" published by John Wiley and Sons.

Director Dietert urged that consideration be given the printing of more copies of the annual TRANSACTIONS so that this valuable material can be more widely disseminated. President Wagner suggested that the Chapters be urged to see that TRANSACTIONS sets are placed in local engineering libraries for reference purposes.

In conclusion, the Treasurer stated that the finances of the Society as of February 28, 1953 were in normal shape for a non-exhibit year, with income holding up as expected and expense under constant supervision and control.

C. *Report of Technical Director*

The Technical Director reported that most technical committees had been active during the fiscal year, particularly in development of the 1953 Convention program. Major items of the Convention program were briefed.

The report stated that four special publications have been published thus far this year—Symposium on Air Pollution, Principles of Gating, Patternmakers Manual, and a reprint of Gray Iron Research Report No. 2—with other publications practically completed: Statistical Quality Control, Recommended Names for Gates and Risers, Glossary of Foundry Terms. A book on Precision Investment Casting is nearing completion and the College Foundry Textbook is now about 50 per cent complete. The cooperation of E. T. Kindt of Kindt-Collins Co., Cleveland, in promoting the sale of the Patternmakers Manual, was mentioned specifically by the Technical Director for commendation.

Each of the AFS-sponsored research projects were discussed briefly, covering Brass & Bronze, Gray Iron, Heat Transfer, Light Metals, Malleable Iron, Sand, and Steel. The Technical Director also stated that effort was now being made to revive the Cupola Research Committee and that H. Bornstein of Deere and Co. had agreed to serve as Chairman, the first task of the Committee being a completely revised edition of the Cupola Operations Handbook.

Director Dietert urged that results of the highly technical heat transfer research be presented to the membership in a more practical manner, perhaps through the use of strip films or charts. Another Director strongly urged that the AFS research projects be more strongly publicized through the Chapters.

(5) Reports of Board Committees

A. *Finance Committee*

The Treasurer presented a statement of reserve fund investments showing a total of $296,315 reinvested through the investment agent account of the Society with the Harris Trust and Savings Bank, Chicago. Present reserve fund investments

include $50,600 or 17.08 per cent of the total in U. S. Government securities; $30,187.50 or 10.19 per cent in Canadian Government securities; $182,874.52 or 61.71 per cent in Industrial Bonds; and $32,653.32 or 11.02 per cent in Industrial Stocks.

It was pointed out that while the original plan presented by the Harris Trust contemplated approximately one-third of the total in Industrial Stocks, it is the intention to build up to that point gradually over a period of years as the market seems to indicate to the advisors of the investment agent account that it is advisable to do so.

Director Dietert suggested that all evidence of Society invested securities be microfilmed and stored in some distant city against the possibility of disaster, and that the Finance Committee consider setting up a second investment agent account with some bank in some distant city with the same possibility in mind. President Wagner stated that the matter would be given fullest consideration.

The Secretary pointed out that $135,000 in cash was still being held in the Society's checking account at the Harris Trust and Savings Bank, and recommended that $35,000 could now be transferred to the investment agent account for investment. On motion duly made, seconded and carried, the recommendation was accepted.

B. *Chapter Contacts Committee*

Vice-President Carter presented a report of Chapter Contacts made by the Directors and urged all to make their Chapter visits as soon as possible. He congratulated the Directors on the conscientious job to date and stated that the Chapters look forward to these Director visits.

C. *Safety & Hygiene and Air Pollution Program*

Chairman Hunt presented a progress report on Safety & Hygiene and Air Pollution, including safety training sessions held by the Safety Committee; safety & hygiene and air pollution seminars held at the University of Illinois and University of Wisconsin; progress in development of a Dust Control and Ventilation Manual; and an Air Pollution Manual; steps toward development of a Recommended Good Welding Practice Manual; and studies being made in the matter of industrial noise abatement.

The Chairman reported that several Chapters have inquired about sponsoring safety training courses and stated that the Safety Committee is also working on revision of the present Recommended Good Safety Practices Manual for the foundry industry.

The report presented information on activities of Program Director W. N. Davis in connection with state legislation considered adverse to the foundry industry. It also reported numerous trips by the Program Director in carrying on activities for the program. Director Dietert suggested that AFS might consider preparation of a film on air pollution, comprising parts of other available films.

Director Troy suggested a questionnaire on the matter of allergies to rosin binders, it being pointed out that this subject was considered in a paper at the University of Illinois seminar in February.

(6) Special Reports

A. *Housing Committee*

The Secretary presented a detailed report on steps taken to acquire a site for the AFS Headquarters Building, and announced that a site had been purchased and is now owned by the Society in Des Plaines, Ill. Floor plans now are being worked out between the Staff and F. C. Riecks of Ford Motor Co. so that specifications can be drawn up and construction bids taken at as early a date as possible.

The Secretary stated that, in view of increased building costs during the past 12 months, some doubt existed as to the total cost of the building as originally planned, but that the Action Committee was considering increased construction costs in development of final plans. In response to a request for a total outlay covering construction, moving, furnishing, etc., the Secretary stated that, without additional Board action, the Committee was considering that the total cost of construction, landscaping, parking area, land and architect fee could not exceed the $200,000 figure voted by the Board of Directors in May, 1952, without further approval by the Board.

B. *Report of Publications Policy Committee*

Recommendations of the Publications Policy Committee, resulting from a meeting held January 8, 1953, were presented and discussed in detail. Actions taken by the Board of Directors have the effect of establishing Publications policies of the American Foundrymen's Society, superseding any previous informal policies in effect due to precedent and use. Publications policies approved by the Board of Directors on motion duly made, seconded and carried, with the thanks of the Board for a job well done.

(NOTE: All established Society policies, as approved, are shown elsewhere in this volume of TRANSACTIONS.)

C. *Report of Convention and Exhibits Policy Committee*

Recommendations of the Convention and Exhibits Policy Committee, resulting from a meeting held on January 16, 1953, were presented and discussed in detail. Actions taken by the Board of Directors have the effect of establishing the following Convention and Exhibits policies of the American Foundrymen's Society, superseding any previous informal policies in effect due to precedent and use. Approved by the Board of Directors on motion duly made, seconded and carried, with the congratulations of the Board for accomplishing a difficult task.

(NOTE: All established Society policies, as approved, are shown elsewhere in this volume of TRANSACTIONS.)

D. *Reports of Chapter and Membership Policy Committee*

Recommendations of the Chapter and Membership Policy Committee, resulting from a meeting held on January 30, 1953, were presented and discussed in detail. Actions taken by the Board of Directors have the effect of establishing the following Chapter and Membership policies of the American Foundrymen's Society, superseding any previous informal policies in effect due to precedent and use. Approved by the Board of Directors on motion duly made, seconded and carried, with congratulations of the Board for undertaking and completing a task long needed.

(NOTE: All established Society policies, as approved, are shown elsewhere in this volume of TRANSACTIONS.)

The Board also considered the Committee's recommendation that Chapter policy regarding the accumulation of Chapter reserve funds be based on total operating expense of a Chapter over a period of two to five years, excess funds to be expended by the Chapters on activities within the purposes of the Society. It was pointed out that such a recommendation follows closely the established investment policy of the National Society, but it was also realized that the Chapters should continue to have sole jurisdiction over accumulated Chapter funds. Therefore, the Board directed the Secretary to present the Committee's recommendation to the Chapters strictly for Chapter consideration.

E. *Report of Policy Sub-Committee on Educational Work as Applied to Training Programs* .

Recommendations of the Educational Policy Sub-Committee, resulting from a meeting held on February 26, 1953, were presented and discussed in detail. Actions taken by the Board of Directors have the effect of establishing the following Educational policies of the American Foundrymen's Society, superseding any previous informal policies in effect due to precedent and use. Approved by the Board of Directors on motion duly made, seconded and carried, with the congratulations of the Board for a thoughtful job well carried through.

(NOTE: All established Society policies, as approved, are shown elsewhere in this volume of TRANSACTIONS.)

F. *Report of the Board Reorganization Committee.*

A report of the Board Reorganization Committee, resulting from a meeting held on January 26, 1953, was presented and discussed in detail. It was understood that all actions taken by the Board of Directors in respect to reorganization of the Board would require revision of the existing AFS By-Laws before taking effect, a meeting of the By-Laws Committee being scheduled for April 7, 1953. It was recommended that members of the Board Reorganization Committee plan to attend the meeting of the By-Laws Committee, if possible.

Minutes of the Board Reorganization Committee meeting, as revised and approved by the Board of Directors and in such form referred to the By-Laws Committee, are briefed as follows:

1. All AFS Chapters to be grouped in Chapter groups for constant group representation on the Board. (Approved).

2. Chapter groups to be organized into five regions with a regional Vice-President in each. (Approved).

3. The AFS Directorate to be increased from 18 to 24 including the President, Vice-President, immediate past-President and 21 Directors. (Approved with provision that the increased Directorate is intended to recognize desires for Board representation by the Chapters, Technical Divisions, and other foundry

interests, plus the need to select outstanding men regardless of geographical location.)

4. A Nominating Committee of eight with one representative from each of the five regions, two immediate past-Presidents, plus the incumbent AFS President, the senior past-President being the Chairman. (Approved with provision that the Nominating Committee be appointed, as now, by the AFS Executive Committee of the Board.)

5. The Nominating Committee, as now, to name five Directors from the various Chapter groups in accordance with a prearranged regional representation. (Approved.)

6. The Board of Directors to appoint two Directors-at-Large annually for three-year terms, with a total of six Directors-at-Large as a permanent set-up. (Approved.)

7. Regional Vice-Presidents to be appointed by the Board for terms of one year each, recommended by the Directors within each respective region and selecting one of their own number. (Approved.)

8. The AFS Executive Committee to include: President, Vice-President, immediate Past President, and three Regional Vice-Presidents elected by the Board on nomination of a Board nominating committee, none of the latter three to serve two years successively. (Approved.)

9. Newly elected Officers and Directors of AFS to take office immediately following their election at the Annual Business Meeting of the Society. (Approved.)

In addition to the above actions, to be placed before the By-Laws Committee as recommendations of the Board of Directors, the following were discussed and accepted for guidance of procedures under the Board reorganization plan:

1. Continue the Chapter Contacts Committee but with each Director assigned to Chapter groups within their Regions but not necessarily within their Chapter groupings.

2. Any future Chapters of AFS to be incorporated into the proposed and accepted outline for Regions and Chapter groups.

3. Industrial advisors of regular Chapters to be, for the most part, the official contacts with the Student Chapters of AFS, plus nearest National Directors where convenient.

4. Board meetings to be held at least twice a year, preferably in July and January, although admittedly it may become advisable to increase this number in some years.

5. A Convention program procedure to be worked out to clarify whether the retiring President or newly elected President should preside at the Annual Banquet or Annual Business Meeting of the Society and that an agreed procedure should govern at all future Conventions.

6. It was urged that the Chairman of the Board Reorganization Committee should sit in with the By-Laws Committee at the time that By-Laws revisions are being considered.

President Wagner thanked the Board Reorganization Committee for the considerable time and thought given to its recommendations, and expressed the feeling that the reorganization would be a very forward looking step in the Society's operations and administration.

G. *Report on 1953 Convention*

The Secretary and Technical Director jointly presented a report on progress of the 1953 Convention, and stated that special bulletins would be issued to the Board of Directors on various events. The Technical Director presented to each Director a detailed program of technical sessions, authors, papers and session Chairmen, as developed to date.

The President announced that he was asking the Secretary to arrange a special luncheon for the Board of Directors on Wednesday, May 6, to which the Officers and Directors elect would be invited, for the purpose of getting better acquainted, with no official business to be transacted.

H. *Report on* AMERICAN FOUNDRYMAN

The Secretary presented a detailed report on AMERICAN FOUNDRYMAN, describing certain steps already taken and others to be taken to strengthen the publication and make it still more desirable as an advertising medium, including the following:

1. Increased sales administration and control. (Already in effect.)

2. The 20 per cent increase in advertising rates, approved in November 1952, to become effective with the April 1953 issue, and advertisers of record protected through the September 1953 issue. (Rates and effective dates already announced.)

3. Greater publicity on the Society's activities, directed toward the advertising profession. (To be put into effect immediately after the Convention.)

4. Revision of editorial policy, involving greater direction and planning of editorial content. (To begin immediately, but not fully effective until after the April and May Convention issues.)

5. Reorganization of the editorial staff. (Already in effect.)

6. Appointment of Associate Editors in each of the seven technical divisions of the Society, also Associate Editors on the West Coast, in Canada and in New England. (Associate Editors to be appointed at the earliest possible date.)

7. Increase in the number of practical foundry articles published, a section for new foundrymen, occasional articles of definite management interest, greater recognition of foundry personalities . . . all looking toward maximum reader interest. (These matters to run concurrent with the new "planned content" program.)

8. Increased horizontal coverage of the foundry industry through increased circulation support. (This step, taken with the approval of the President and Vice-President, now in effect.)

9. Additional steps to increase the number of inquiries received as a result of advertising and editorial articles in the magazine, such as the "Products Parade" section of the April 1953 issue. (No timetable, but a constant goal.)

10. Careful consideration of the advantages or disadvantages of the present publication dates of AMERICAN FOUNDRYMAN, and possibly some future revision thereof. (Being studied.)

In conclusion, the Secretary pointed out that all matters concerning AMERICAN FOUNDRYMAN should now be considered in the same light as any commercial publication, and that steps for its improvement should be resolved on that basis. The Secretary presented an organization chart being put into effect in line with the itemized report presented.

J. *Report on Meeting with F.F.M.A.*

Past President Seelbach reported on a special meeting between AFS officials and the members of the Foundry Facings Manufacturers' Association on February 11 in Chicago, attended by President Wagner, Vice-President Carter, Mr. Seelbach, Secretary Maloney, Technical Director Massari, and S&H&AP Director Davis. Mr. Seelbach reported that the purpose of the meeting had been to iron out some evident misunderstandings between AFS and F.F.M.A. and that in his estimation this objective had been well accomplished. Mr. Seelbach felt that future relations between the two groups would now be improved.

(7) Next Board Meeting

President Wagner announced that the next meeting of the AFS Board of Directors would be the annual Board meetings scheduled for July 23-24.

There being no further business to be considered, the meeting was declared adjourned.

Respectfully submitted,
WM. W. MALONEY
Secretary-Treasurer

Approved:
I. R. Wagner, *President*
June 2, 1953

Tenth Annual Chapter Officers Conference
Hotel Sherman, Chicago – June 18-19, 1953

PROGRAM

Thursday, June 18

9:30 AM Call to Order .....Chairman Carter
9:32 AM Welcome .....Chairman Carter
9:35 AM Introductions .....By the Conference
9:47 AM Sketching in the Background .....Pres. Wagner
9:52 AM AFS as a Technical Society .....Tech. Dir. Massari
10:17 AM Developing a Chapter Program....By the Conference
11:45 AM The S. & H. & A.P. Program of AFS..Director Davis
12:15 PM Luncheon
1:30 PM Chapter Educational Programs....By the Conference
3:15 PM The AFS Organization Setup.....Sec'y. Maloney
3:45 PM The AFS Headquarters Building.....Pres. Wagner
4:00 PM Report on AMERICAN FOUNDRYMAN..Tech. Ed. Scobie
4:25 PM The Nominating Committee.....Sec'y. Maloney
4:35 PM Chapter Contacts by Directors..Vice-Pres.-Elect Dost
5:00 PM Adjournment
5:15 PM Social Hour
6:30 PM Conference Dinner
Presiding: Chairman C. L. Carter
Guest Speaker: Caserio, former Golden Gloves Champion.
Subject: "The Trials of a Golden Gloves Boxer."
8:30 PM Special Meetings

Friday, June 19

9:00 AM Call to Order .....Chairman Carter
9:03 AM Keeping Up the Membership.....By the Conference
10:20 AM Exhibit Conventions .....Exhibit Mgr. Hilbron
10:55 AM The Chapter Chairman's Job .....Chairman Carter
11:30 AM The Shakeout .....Chairman Carter
12:40 PM Resolutions .....The Conference
12:45 PM Final Adjournment
1:00 PM Luncheon

ATTENDANCE

Directors and Guests

Participating in the 10th Annual Chapter Officers Conference in addition to those mentioned, were the following national officers: Martin J. Lefler, Oliver Corp., South Bend, Ind.; C. V. Nass, Beardsley & Piper Div., Pettibone Mulliken Corp., Chicago; G. Ewing Tait, Dominion Engineering Works, Ltd., Montreal, Que.; James Thomson, Continental Foundry & Machine Co., East Chicago, Ind., and Robert E. Kennedy, AFS Secretary Emeritus, Wilmette, Ill.

Guests included: J. R. Allan, International Harvester Co., Chicago; H. Charles Esgar, Foundry Educational Foundation, Cleveland; Professor Roy W. Schroeder, University of Illinois, Chicago, and Hans J. Heine, AFS Assistant Technical Director.

Chapter Representation

Chapter Officers present were chiefly chairmen and program chairmen (usually also vice-chairman). They were:

BIRMINGHAM—Chairman Biddle W. Worthington, McWane Cast Iron Pipe Co., Birmingham, Ala. Vice-Chairman Edwin E. Pollard, Alabama Pipe Co., Anniston, Ala.

BRITISH COLUMBIA—Chairman William R. Holeton, B. C. Research Council, University of British Columbia, Vancouver, B. C., Canada. Vice-Chairman Howard H. Havies, Vivian Diesels & Munitions Ltd., Vancouver, B. C., Canada.

CANTON DISTRICT—Chairman Robert A. Epps, Stoller Chemical Co., Akron, Ohio. Vice-Chairman Alfred S. Morgan, Babcock & Wilcox Co., Barberton, Ohio.

CENTRAL ILLINOIS—Chairman Henry Felten, Peoria Malleable Castings Co., Peoria, Ill. Program Chairman Burton L. Bevis, Caterpillar Tractor Co., Peoria, Ill.

CENTRAL INDIANA—Chairman Dallas F. Lunsford, Perfect Circle Corp., Hagerstown, Ind. Vice-Chairman Fred E. Kurtz, Electric Steel Castings Co., Indianapolis.

CENTRAL MICHIGAN—Chairman John E. Wolf, Midwest Foundry Co., Coldwater, Mich. Secretary-Treasurer Gerald Strong, Homer Foundry Corp., Coldwater, Mich.

CENTRAL NEW YORK—Chairman John A. Feola, Crouse-Hinds Co., Syracuse, N. Y. Vice-Chairman Joseph Gibson, Sweets Foundry Inc., Johnson City, N. Y.

CENTRAL OHIO—Chairman C. W. Gilchrist, The Cooper-Bessemer Corp., Mt. Vernon, Ohio. Program Chairman Raymond M. Meyer, Ohio Steel Foundry Co., Springfield, Ohio. Secretary N. H. Keyser, Battelle Memorial Institute, Columbus, Ohio.

CHESAPEAKE—Chairman William H. Baer, U. S. Navy Dept., Bureau of Ships, Washington, D. C.

CHICAGO—Chairman John A. Rassenfoss, American Steel Foundries, East Chicago, Ind. Vice-Chairman Robert L. Doelman, Miller and Company, Chicago.

CINCINNATI DISTRICT—Director John D. Sheley, Asst., The Black-Clawson Co., Hamilton, Ohio. Vice-Chairman Harry F. Greek, The Hill & Griffith Co., Cincinnati.

CORN BELT—Chairman Earl White, Paxton-Mitchell Co., Omaha, Neb. Vice-Chairman Bert J. Baines, Omaha Steel Works, Omaha, Neb.

DETROIT—Chairman Harry E. Gravlin, Ford Motor Co., Dearborn, Mich. Vice-Chairman Claude B. Schneible, Claude B. Schneible Co., Detroit.

EASTERN CANADA—Chairman John G. Hunt, Asst. Dominion Engineering Works, Ltd., Montreal, Que. Vice-Chairman Claude Bourassa, Archer-Daniels-Midland Co., Montreal, Que.

EASTERN NEW YORK—Chairman Edwin S. Lawrence, General Electric Co., Schenectady, N. Y.

METROPOLITAN—Chairman Bernard N. Ames, U. S. Naval Shipyard, Brooklyn, N. Y. Vice-Chairman Charles Schwalje, Worthington Corp., Harrison, N. J.

MEXICO—Chairman Frank Madrigal, Fundidora de Aceros Tepeyac, S. A., Mexico City, D. F. Vice-Chairman Pedro Gomez, Fundicion de Acero Electrico, The Teziutlan Copper Co., S. A. Mexico, D. F.

MICHIANA—Chairman Leslie Pugh, Casting Service Corp., La-Porte, Ind. Program Chairman R. A. Payne, The Sterling Brass Foundry, Elkhart, Ind. Director Robert H. Greenlee, Auto Specialties Mfg. Co., St. Joseph, Mich.

MID-SOUTH—Chairman Earl Kreunen, Wm. C. Ellis & Sons Co., Memphis, Tenn. Secretary-Treasurer M. B. Parker, Jr., M. B. Parker Co., Memphis, Tenn.

MO-KAN—Chairman William N. Chivvis, National Lead Co., Kansas City, Mo. Vice-Chairman Lloyd Canfield, Canfield Foundry Supplies & Equipment Co., Kansas City, Kans.

NORTHEASTERN OHIO—Chairman Stephen E. Kelly, Eberhard Mfg. Div., Eastern Malleable Iron Co., Cleveland. Program Chairman Dave Clark, The Forest City Foundries Co., Cleveland.

NORTHERN CALIF.—Chairman William S. Gibbons, Ridge Foundry, San Leandro, Calif. Vice-Chairman John Bermingham, E. F. Houghton and Company, San Francisco.

No. ILL.-So. WIS.—Chairman Chas. N. Deubner, Yates American Machine Co., Beloit, Wis.

NORTHWESTERN PA.—Chairman Chas. F. Gottschalk, Cascade Foundry Co., Erie, Pa. Vice-Chairman Bailey D. Herrington, Hickman, Williams & Company, Erie, Pa.

ONTARIO—Chairman Alex Pirrie, Standard Sanitary & Dominion Radiator, Ltd., Toronto, Ont. Vice-Chairman F. J. Rutherford, Refractories Engineering & Supplies, Ltd., Hamilton, Ont.

OREGON—Chairman James T. Dorigan, Electric Steel Foundry, Portland, Ore. Vice-Chairman P. J. Laugen, Oregon Steel Foundry Co., Portland, Ore.

PHILADELPHIA—Chairman W. Donald Bryden, Philadelphia Bronze & Brass Corp., Philadelphia. Vice-Chairman Daniel E. Best, Bethlehem Steel Co., Bethlehem, Pa.

QUAD CITY—Chairman Eric Welander, John Deere Malleable Works, East Moline, Ill. Vice-Chairman Wm. Ellison, Thiem Products, Inc., Milwaukee.

ROCHESTER—Chairman Neal F. Clement, Rochester-Erie Foundry Corp., Rochester, N. Y. Vice-Chairman Duncan M. Wilson, Engineered Castings Div., American Brake Shoe Co., Rochester, N. Y.

SAGINAW VALLEY—Chairman F. J. McDonald, Saginaw Malleable Iron Plant, Saginaw, Mich. Vice-Chairman Woodrow Holden, Eaton Manufacturing Co., Vassar, Mich.

ST. LOUIS DISTRICT—Chairman Webb L. Kammerer, Midvale Mining & Manufacturing Co., St. Louis. Vice-Chairman Fred J. Boeneker, Bronze Alloys Co., St. Louis.

SOUTHERN CALIF.—Chairman Hubert Chappie, National Supply Co., Torrance, Calif. Program Chairman Charles R. Gregg, Gregg Iron Foundry, ElMonte, Calif.

TENNESSEE—Chairman William P. Delaney, Eureka Foundry Co., Chattanooga, Tenn. Vice-Chairman Charles S. Chisolm, The Wheland Co., Chattanooga, Tenn.

TEXAS—Vice-Chairman Edward W. Wey, Dee Brass Foundry, Inc., Houston, Texas. Secretary Elmore C. Brown, Whiting Corp., Houston.

TIMBERLINE—Chairman Roger Hageboeck, Electron Corp., Littleton, Colo. Vice-Chairman E. Byron McPherson, McPherson Corp., Denver, Colo.

TOLEDO—Chairman Bernard J. Beierla, E. W. Bliss Co., Toledo, Ohio. Vice-Chairman C. E. Eggenschwiler, Bunting Brass & Bronze Co., Toledo, Ohio.

TRI-STATE—Secretary W. H. Mook, Bethlehem Supply Co., Tulsa, Okla. Vice-Chairman D. W. McArthur, Oklahoma Steel Castings Co., Inc., Tulsa, Okla.

TWIN CITY—Chairman O. J. Myers, Archer-Daniels-Midland Co., Minneapolis. Vice-Chairman Arthur W. Johnson, Northern Malleable Iron Co., St. Paul, Minn.

WASHINGTON—Chairman William L. Mackey, Washington Stove Works, Everett, Wash. Program Chairman William A. Shaug, South Seattle Foundry Co., Seattle, Wash.

WESTERN MICHIGAN—Vice-Chairman John A. VanHaver, Sealed Power Corp., Muskegon, Mich.

WESTERN NEW YORK—Chairman Joseph M. Clifford, Atlas Steel Casting Co., Buffalo, N. Y. Secretary A. J. Heysel, E. J. Woodison Co., Buffalo, N. Y.

WISCONSIN—Chairman A. F. Pfeiffer, Allis Chalmers Mfg. Co., Milwaukee.

Charles Edgar Hoyt Annual Lecture

OUTSTANDING OPPORTUNITIES FOR THE FOUNDRY INDUSTRY

By

James H. Smith*

■ Five particularly important opportunities stand out among many which present themselves to the foundry industry. These lie in the fields of:

1. Molding Methods and Materials.
2. Metallurgy of Cast Iron.
3. Heat Treatment of Cast Iron.
4. Methods Engineering.
5. Educational Programs for the Foundry Industry.

The foundryman has improved sand workability to achieve better molds, truer, cleaner, and more economical castings. He has obtained these results by better understanding of the materials used in the preparation of the sand and improved equipment for distribution and mulling of the clays.

New sand-conditioning resins for the foundry industry are now available and the next few years should see some very important developments which will improve conventional green-sand molding methods.

A new molding technique has been developed called pressure molding which utilizes pressures up to 500 psi as compared with pressures of 50 psi or less being used at the present time. This method produces a very smooth and accurate casting. Proponents of this molding method are very enthusiastic regarding the future of the process. I would not be surprised if, within a few months, some plants were in production, using these high-pressure molding machines with resin additives to the molding sand.

Resin Function

The function of these resin additives is to improve the workability of the sand through uniform clay distribution, eliminating any clay balls. The result is increased flowability of the sand with uniformly hard molds on both vertical and horizontal mold surfaces, producing castings needing a minimum of cleaning. The trend in molding-sand technique is definitely in the direction of reduced cleaning time and closer dimensional tolerances.

The shell molding and core-making process needs no introduction to you foundrymen. I consider the shell process one of the greatest technical developments of our time in the foundry industry. In my estimation, it may revolutionize foundry molding techniques to the point where it will be more economical to produce most small castings, and some large castings, by the shell-mold process as compared with conventional, green-sand molding.

Complete mechanization of the molding operation on high-production runs is possible using this process. Several machines which accomplish this are already on the market.

The shell process is usually considered a precision casting process. It is usually thought of as being between green-sand molding and the lost wax technique. At Central Foundry Division we are approaching the shell program from the standpoint of producing a better casting for less money and, at the same time, holding the dimensional tolerances to closer limits than with our present green-sand method. We are not promising tolerances to the degree where machining can be entirely eliminated. Some of the claims which have been made in published articles regarding tolerances have been very misleading. We know that it is possible to hold very close limits on castings made in experimental laboratories using laboratory methods, but we know also that the same results are very difficult to obtain in high-volume production, using practical manufacturing methods. There is every reason to believe that sometime in the future, as the technique is further developed, it will be possible to cast many surfaces to much closer dimensional tolerances.

Resin Cost a Drawback

The cost of the phenolic resins used to provide the bond material for the silica grains in the construction of the shell is the main reason for the process not being more widely used. There seems to be little chance of a reduction of the present high cost of phenolic resin because of the stable price of phenol. Manufacturers are striving to discover suitable resin substitutes and resin extenders. Our research laboratory has a continuous program to try to find a substitute for phenolic resin or a material which can be blended with phenolic resin to produce a less expensive mix. If it is possible to use a material which would bring the price of the bonding material down to 10¢ or 15¢ a pound, I

* General Manager, Central Foundry Division, General Motors Corporation, Saginaw, Mich.

believe most everyone would use the shell process for the production of small castings. Even at the present high resin cost, many castings can be produced at a cost comparable to green-sand molding. This is particularly true where cores can be eliminated by the use of the process.

(At this point, a motion picture of the shell process, was shown.)

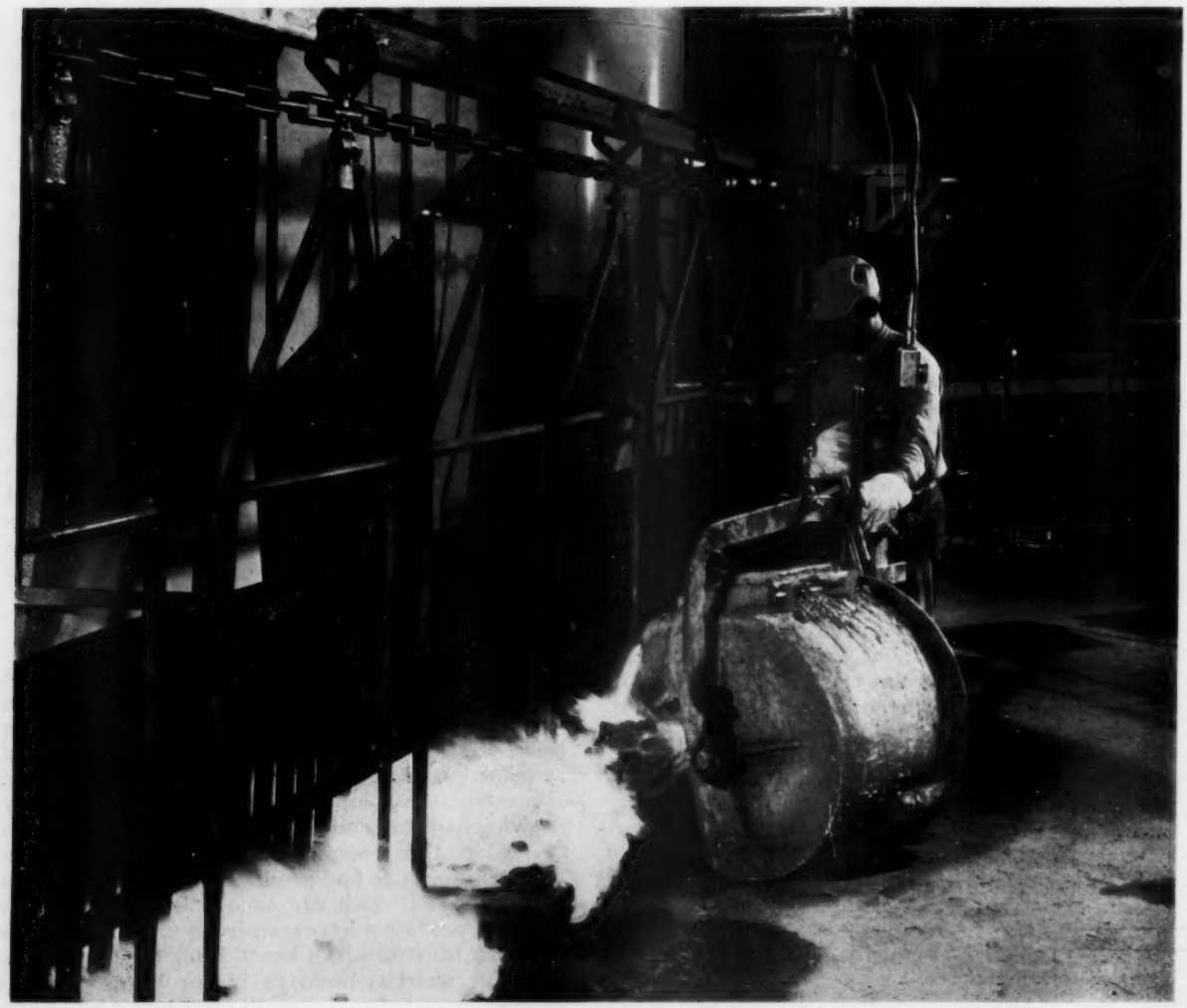
At our Saginaw Malleable Iron Plant, where quite a few castings are made in shell molds, we use a continuous pendulum-type conveyor with shell-making machines set parallel to the conveyor.

The shell-making machine is an integrated mechanism operating in cycles and consisting of a gas-fired, radiant-burner oven, a rollover box containing the sand-resin material, and the pattern mounted on a hinged arm. Adjacent to this machine is a vibrator-actuated container which holds dry resin used for gluing the shells together, a fixture for knocking out the top of the pouring sprue, and a jolt-squeeze machine which is used in the gluing operation to press the halves of the shell together.

At the start of the cycle, the hot patterns, both cope and drag, are lowered onto a rollover box which contains a sand resin material from which the shells are made. The rollover box and pattern are then inverted for about 20 seconds which is the approximate time needed for enough of the material to adhere to the hot pattern to make a shell about 5/16 of an inch thick.

When the shell is ready for curing, the rollover box returns to its original position. The shell and pattern mechanically enter the gas-fired, radiant-burner oven for curing. The amount of curing time will vary with the thickness of the shell—a shell 5/16 of an inch thick requires approximately 40 seconds.

After the shell is cured, the door of the oven rises and the mechanical arm carrying the pattern and shell moves to a vertical position parallel to the furnace door. Spring-loaded ejector pins release the shells from the pattern. The operator places the drag half on the gluing fixture, and the cope half is placed on the holder which knocks out the top of the pouring sprue. He positions the resin container which vibrates dry resin onto the shell and then places the cope half onto the drag half and touches the controls which automatically move the squeezehead of the gluing ma-



Pouring shells on a continuous-type conveyor, where they remain for short cooling period.



Internal shell-making machine with baking oven located directly behind the operator, conserving movement.

chine into position. The machine presses the halves together for a predetermined length of time sufficient to assure a firmly-bonded complete shell.

The completed shell is placed on the pendulum conveyor and moves to the pouring station. After being poured, the shells remain on the continuous conveyor for a short cooling period. When they arrive at the shakeout screen, a cam-actuated mechanism tips the conveyor plates, and the shell and castings are dumped onto the shakeout screen. The sand is vibrated through the screen, and the castings drop into a hopper below.

We are sure there will be further, rapid development in this interesting technique of making castings.

Metallurgy of Cast Iron

Most of the technical advancement in the ferrous industry during the past century, with the exception of recent years, has been in steel; very little progress was made in the development of cast iron. Recently, engineers and metallurgists working together have made wonderful strides in cast iron metallurgy, in developing new alloys and in improving melting techniques.

One of the developments of this period of research is nodular iron. This product is frequently made by adding magnesium or magnesium alloys to gray iron, which changes the graphite form on solidification from flake to some other pattern, preferably to a round shape similar to that of temper carbon in malleable iron.

A realistic evaluation of the market for this type of iron indicates that it has a wide range of potential engineering applications. The product will have a much larger market if and when it is possible to produce it more economically and with more uniform results. To accomplish this, it is possible that it may be necessary to find a method of making this or similar products in the cupola or some other melting medium. Continuing research is being conducted in this country and abroad, and there is every reason to believe that in the future nodular iron will assume a much more competitive role.

Low-sulphur Gray Iron

Another development, which will definitely have a marked effect on the iron casting industry, is the production of a low-sulphur gray iron. To produce this, molten cupola iron is treated with finely divided particles of calcium carbide. The product possesses superior mechanical properties with a tensile value approaching 50,000 psi and with a Brinell hardness of around 200.

If it has the proper analysis, the calcium carbide-treated metal, with the addition of magnesium oxides, can be used to produce nodular iron; other nodularizing agents, such as rare earth oxides have also been used successfully with this base metal in producing nodular iron.

Comparatively little of this low-sulphur iron has been produced on a production basis; but, no doubt, there will be an increasing demand in the future providing the product can be produced economically.

Development of Pearlitic Malleable

Another cast iron which has been developed in recent years is pearlitic malleable iron. In our plants we make this iron by using the regular base white iron which is melted in a cupola and superheated in an electric furnace. The castings are then malleablized in a continuous, radiant-tube, gas-fired, heat-treating furnace . . . quenched in air at about 1650 F . . . and tempered back to the desired hardness.

During the past fifteen years the use of this product has increased from practically nothing to about 100,000 tons per year. The product has found wide application in the automotive field and particularly in automatic transmissions. It is also used effectively for household appliance parts, diesel engine parts, hand tools, and many other uses. The contribution of this engineering material to the small arms, military truck and ammunition programs during World War II and in the present Korean conflict has been substantial. The future of pearlitic malleable looks very promising; it is constantly replacing forged steel parts, weldments, and stampings.

Heavy-Section Malleable

Another development of the last few years is a product called "bisbo" iron, which was developed in General Motors' research laboratory. This iron is made by adding small amounts of bismuth and boron to white iron base metal and malleablizing to make a pearlitic structure. The purpose of the alloy additions is to prevent formation of graphite on solidification in heavy section castings. Previously, malleable iron castings have been cast in relatively light sections, gen-



New double core box setup, allowing steady production of cores. Operator can now remove filled core box from machine with one hand while inserting empty core box with other hand. Method upped production 42 percent.

erally less than 1½ inches. Castings are now being produced in sections up to 4 inches and free from primary graphite. Since this type of iron can be produced from a standard white iron base metal, it has many possibilities; therefore I can visualize a considerable tonnage of castings with heavy sections being made from this material.

The trend of the designing engineers in the automotive and allied fields is toward more intricate castings with thinner walls. How is the foundryman going to meet this challenge? The most logical way is to be able to pour iron into the mold at a uniformly higher temperature than is being done at present. This can be accomplished by a duplex melting operation, melting down in the cupola and superheating to a constant temperature in an electric furnace. We are about to start this practice in the gray iron foundry where we are making automotive and diesel engine cylinder blocks and heads. We believe that the improvement in metal and temperature control will more than justify the added cost and make it possible for us to meet the evergrowing demand for lighter section castings.

Micro-Chemistry in Infancy

An outstanding development in melting practice in recent years has been the increased use of basic-lined cupolas. To produce some of the new irons which we have been discussing, it appears that basic-lined cupolas will be more widely used as the new irons come into greater demand.

The metallurgical developments of the past few years in the foundry industry have been little short of miraculous. It is difficult to visualize the tremendous effect that minute percentages of certain elements have on the molecular structure and physical properties of iron. The use of micro-chemistry in the foundry industry is in its infancy. The control and effects of small amounts of various elements will play an important role in foundry metallurgy, possibly to even a greater extent than has been witnessed during the past few decades in the steel industry.

The new products which have been the results of these development programs have had a tremendous effect on the economy of the country. They have made it possible for the manufacturers of many items, such as automobiles, household appliances, farm equipment, diesel engines and many others to make a better product at a lower cost. I am sure that with all of the new things on which we are working and with developments of the future we will have the opportunity and will continue to make a substantial contribution to the over-all economy of our country.

Much research work has been conducted in recent years on the heat treatment of various types of iron. Maximum uniformity of structure is always important and is one of the benefits derived from heat treatment. Several of the heat treatments for cast iron which will be used increasingly in the next few years are:

Annealing for Stress Relief definitely increases the life of large and highly-stressed gray iron parts. It is used to a great extent for diesel engine cylinder blocks and heads and similar parts. The temperature used must be kept below the critical, with the temperature and time depending upon the size and form of the castings.

Annealing for Machinability. I believe this type of heat treatment for many gray iron castings will increase extremely rapidly in the next few years. In one of our gray iron foundries, over 80 per cent of the production is annealed for machinability with a resultant increase in the machine shop of 25 to 50 per cent in the output per machine and an increase in tool life of over 100 per cent. This method of annealing is called microstructural control to produce a ferritic matrix. How can any cost-minded management miss this opportunity of cost saving wherever machinability is of prime consideration! For most gray iron castings, an hour at 1350 to 1550 F, depending upon the chemistry of the castings, will produce the desired results.

Quenching for Higher Strength. This type of heat treatment will gain much wider use, as time goes on, due to the fact that there is an increasing demand for higher strength irons. By heating to approximately 1600 F and quenching in oil, followed by a subsequent draw, 60,000 to 80,000 psi tensile irons can be produced. The heating temperature may vary in relation to the chemistry of the iron.

Martempering. This is a relatively new method of heat treatment for castings. Castings are quenched rapidly from above the critical by submerging in a liquid bath and holding at a temperature of about 400 to 500 F. Castings are held in the bath only until they have reached the same temperature as the quenching medium and are then withdrawn and cooled in air. In some instances, a subsequent low-temperature draw is used to further reduce any tendency toward cracking, particularly if the casting shape is intricate. This treatment has been used on a number of finish-machined, pearlitic malleable iron castings with the advantage of minimizing internal strains and preventing warpage. This method will find an increasing number of applications in the future.

Heat treatment is somewhat new for many foundrymen. When they realize the advantages to be gained in increased physical properties and the marked improvement in machining characteristics, I am certain there will be a very decided increase in the heat treatment of iron castings. Certainly there are enough possibilities in this phase of our business to consider it an outstanding opportunity for the future.

Methods Engineering Increases Efficiency

Methods engineering is the analyzing and planning of employee movements to increase their effectiveness. This means the elimination of idle time, the reduction of ineffective movements, such as long body bends, turns, walking, heavy lifting and others. An analysis is also made to determine the advisability of combining operations.

This analyzing and planning not only improves quality and reduces cost, it improves working conditions, relieves monotony and fatigue and results in a much more satisfied and efficient employe. With this accomplished, we are a much better competitor in a highly competitive market which requires a constant effort to provide the customer with a better quality product at lower cost.

For a methods program to be effective and successful requires the full support and cooperation of the entire management group. This is a continuing activity as today's improvements may be obsolete tomorrow because new ideas and technological improvements progressively change our thinking.

[At this point, a short film entitled *"Methods Engineering as Applied to the Foundry Industry"* was shown. Some of the illustrations show what has been achieved through methods engineering.]

Methods engineering is valuable in any industry, regardless of size or kind, and provides an excellent way to realize enormous returns from the time and effort spent. In my opinion, we have only scratched the surface of the unlimited opportunities in methods engineering in the foundry industry.

Foundry Educational Programs

We all know that men and materials are important, and up to this point we have discussed the material and process aspects of foundry opportunities. Machinery and raw material, being on the open market, are available to anyone with the purchasing power to acquire them. What then, makes some organizations more successful than others? It is their greatest asset—people. People of many types and talents make up our entire organization.

During the past few decades, the foundry industry has actively solicited men in the engineering fields in increasing numbers and has used them to good advantage in all phases of the business. As foundrymen, we look toward the engineer for technical and industrial progress.

As I mention engineers, the thought no doubt comes to your mind that I am advocating running all of our plants with engineers or trained technical men. I have no such thought and certainly want that point made clear. The people we have in the industry today are doing and will continue to do a good job. Our objective, however, should not be to do as good as we have—but to improve our position. This means that we must concentrate on training our organization for the present and the future.

The graduating engineer is rarely in a position to make an immediate contribution to the industry or assume the responsibilities of management. He does, in most cases, have a potential greater than had he not had the benefit of this formal education. The trend in the foundry industry is toward the employment of the technical graduate for future positions of responsibility in manufacturing.

In order to interest engineering students in the Foundry Industry, we should do three things: (1) Develop methods to influence students who will one day be specifying and using cast metal products; (2)

Endeavor to influence engineering students to enter the foundry sequence in schools where such a program is available; and (3) Concentrate on making the foundry sequence available in more schools and universities.

Although the engineering enrollments are seriously low, more engineering graduates have entered the foundry industry in the past year than in previous years; however, we still are not getting our share of the graduating engineering talent. Of last year's class, less than 1 per cent chose for their career the fifth largest durable goods industry in the country. There can be only one answer—they do not realize the opportunity the foundry industry has to offer! And, until the last two or three years, we have done little to let them know of these opportunities.

The Foundry Educational Foundation is aggressively tackling this problem. Since the Foundation was incorporated in February of 1947, a total of 990 graduates have entered the foundry industry through the FEF program, and approximately 1,450 students were employed as summer workers. This organization is doing, and I believe will continue to do, an excellent job in furnishing the foundry industry with young men schooled in the fundamentals of foundry engineering. However, they can supply the industry with only a limited number of these people. We must recruit all available talent possible from the graduating classes of the engineering schools to get the additional technical people we will need.

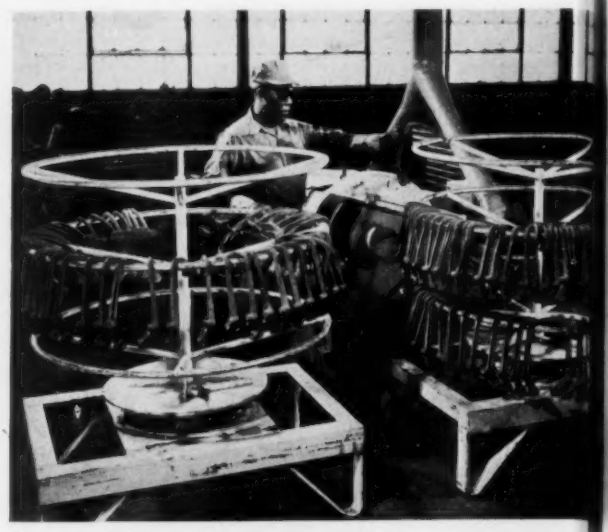
Suggestions

Here are six suggestions which I think will help us attain higher caliber and better-trained organizations:

1. Set up better procedures to select qualified personnel for supervisory positions. This would include selections from within our own organizations as well as new hires.
2. Provide good, workable training programs for all supervisors and technical personnel.
3. Maintain effective apprentice programs to develop the skilled trades necessary, such as patternmakers, tool and die makers, millwrights, electricians, etc.
4. Acquaint more students in the engineering schools with the opportunities in the foundry industry by getting the schools to include foundry educational programs as a part of their curriculum.
5. Try to attract more graduate engineers into the industry to accelerate the development work necessary to keep abreast of competing industries.
6. Support the Foundry Educational Foundation financially.

Certainly it would not be necessary to replace any management employes in the industry to accomplish what I propose. Every year there are in excess of 2,000 supervisors, engineers or others in the management group who leave the industry through retirement, death and for other reasons. Concentrating on better training and placement would certainly tend to improve our organizations tremendously.

I believe that there are unlimited opportunities for improved educational programs for the Foundry Industry, and I would like to emphasize that unless we develop more and better-trained people, we will not receive the maximum benefits from the other opportunities we have been discussing.



After annealing, foot pedals are placed on portable, rotating rack by operator, who grinds and disposes to other rack. Method eliminates fatiguing body movements.



This improved method of shearing gates and shot blasting differential carriers has eliminated two operators and necessity of trucking between the various operations.

I look forward with a great deal of optimism to the future. In our hands lie outstanding opportunities for the foundry industry. This great industry of ours in future years will have engineering markets and stature in the economy of America in direct proportion to what we men in the industry today do in the years immediately ahead of us. We have been told that the future of the foundry industry is bright, but it can only be so as a reflection of our own endeavor. This responsibility is entirely ours, both individually and collectively! If we meet these responsibilities and take advantage of our opportunities, we cannot help being successful in this interesting and dramatic business.

A SURVEY OF THE SHELL MOLDING METHOD OF CASTING PRODUCTION*

By

Bernard N. Ames**

ABSTRACT

This paper summarizes and surveys the shell molding process and is based primarily on data developed in research and production activities at the New York Naval Shipyard. Data are included herein on the mechanics of the process, metallurgical evaluations of the process, mechanization, plant safety, dimensional tolerance, gating and heading, applications and economics. It was concluded that the shell molding process when used on the proper application is an economical foundry technique which permits the utilization of lower grade skills; frequently will improve metal yield; and will reduce machine shop time and cleaning costs.

Introduction

In the past few years shell molding, sometimes referred to as the "C" process, has been recognized as one of the major technological advancements in the foundry industry. It is perhaps the first basic change in the method of mold construction since the inception of the foundry art.

The process, although promising, should not be considered a panacea for all foundry ills. Casting defects resulting from improper gating and heading techniques, malpractices in melting or sand-resin processing, are just as apt to occur in shell molded castings as in conventional sand castings unless sound fundamental principles are applied. However, when the proper casting application is selected and the shell molding method is utilized correctly in production of the casting, the process can be an economic tool geared to high production requirements.

History of the Process

A U. S. Department of Commerce bulletin[1] by R. W. Tindula describes that a method of making thin shell molds and cores for smooth, accurate casting of metals was developed during World War II by Johannes Croning of Hamburg, Germany. A patent application[2] (No. 48679) was filed in the German patent office on Feb. 1, 1944, which described essentially the shell molding process. The patent application was kept in a secret status by the German Government and copied in microfilm by allied technical intelligence at the end of the war. The microfilm is filed as PB-83891 (in German) at the Library of Congress, Washington 25, D. C. It was reported[3] that the process was in production in a number of places in Germany, particularly for the production of cores. It was indicated, for example, that the firm of Haller Werke A. G. in Hamburg-Altona was producing cores for 8-cm hand grenades by the Croning Process, and had reached a capacity of 6000 components per day.

The process itself was examined by W. W. McCulloch[4] a member of a United States technical intelligence team of the Department of Commerce. The detailed report of this inspection was published by the Department of Commerce as Fiat Final Report No. 1168, dated 30 May 1947. As indicated in Tindula's report, the assignment files of the U. S. Patent Office reveal that J. Croning filed applications for patents[5] dated 3 December 1947 assigned to the Crown Castings Corp., Philadelphia, Pa., on foundry molds and cores from preheated sands and plastics and a process for the manufacture of foundry molds and cores. At the present time no patents have been granted on either of these applications. Processes employed by Germany during World War II and up to Jan. 1, 1946 are available to industry and not patentable in the United States or any other allied nation. However, patents may be issued on specific improvements, and an industrial user may not employ such improvements without obtaining a license from the patent holder. However, a company is not charged with infringement until the patent comes into effect on the date of issue. In the interim, intensive development work in this country, which it is estimated has become the subject of hundreds of patent applications, has made this process an economic and useful foundry tool.

Literature Review

Tindula's[1] report lists an extensive bibliography pertaining to shell molds. This bibliography is repeated in part in this paper and recent publications

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NOTE: The opinions or assertions contained herein are those of the author, and are not to be construed as official, or necessarily reflecting the views of the Navy Department. Published with permission of the Navy Department.

* Official Exchange Paper from the American Foundrymen's Society to the Institute of British Foundrymen, presented in Great Britain in June, 1953.

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which have been published since the date of the U. S. Department of Commerce bulletin have been added. Only those articles which it is believed have technological significance are listed. The majority of these articles deal with the mechanics of the shell molding process, the foundry technology involved, metallurgical aspects, mechanization and possible applications.

Raw Materials

The raw materials currently employed in the investment formulation for the shell molding process are generally washed and dried silica sands or bank sand containing a small percentage of clay, and a "B" stage phenol-formaldehyde resin. Occasionally additive materials such as silica or zircon flour (in the range of 5-10 per cent) are employed to improve the surface finish of the casting, particularly in the higher temperature alloys.

Sands which are being utilized for this process range in AFS fineness from 75 to 230, and are either round, angular or subangular in grain shape. The clay content should generally be under 3 per cent, and the sand should be low in metallic oxides or fluxing agents. Sands with a wide distribution (five or six screens) seem to yield superior finishes, as compared with three-screen sands. As a general rule, the finer the sand, the smoother the resultant surface of the casting. Zircon sands can be employed satisfactorily in this process, and will yield a smoother shell mold as compared with the silica sand shell molds. However, this apparent greater degree of smoothness is not fully transmitted to the actual casting and castings manufactured with zircon sands are not superior in surface finish in all alloys to castings made with properly selected silica sands. In addition their relatively high cost per unit volume may make the utilization of zircon sands impractical except for special applications.

Shell molding itself has opened a wide avenue of investigation on the utilization of numerous types of molding materials. In many instances it may be feasible and economical, in order to achieve certain properties, to deviate from the use if silica sands as basic molding mediums and to employ other basic materials.

The amount of resin employed is approximately 5-9 per cent by weight of the sand utilized, and is dependent upon the sand fineness and the amount and fineness of the additive employed. As indicated above "B" stage phenol-formaldehyde resins are being used almost exclusively in shell molding operations in this country. Phenol-formaldehyde resins may be either a single or two-step formulation. In the former case, the necessary excess of formaldehyde to complete the reaction is contained in the formulation, and in the latter case an accelerating material is added, usually hexamethylenetetramine. Approximately 10-15 per cent of the "hexa" is employed. The two-step resins are being utilized almost exclusively since they possess better storage characteristics and shelf life.

In the early stages of shell molding development work at the Laboratory and Shipyard with which the author is associated, it was found necessary to conduct a long-range series of practical resin tests and evaluations with a view toward determining which group of resins performed most satisfactorily as a bonding agent and to establish the range of properties most suitable for over-all shell molding operations. A large number of resin products have been tested, evaluated and catalogued. The majority of these resins were of the phenol-formaldehyde class, one-step or two-step type. Tests were also performed upon resins of the melamine-formaldehyde, urea-formaldehyde, melamine-urea-formaldehyde and epoxy resin types.

The method of test and evaluation consisted of:

a) Mixing the resin under test with the base sand, which was in practically all cases a washed and dried sub-angular grain silica sand of approximately 150 AFS fineness. In the case of the phenol-formaldehyde resins the basic formula for test purposes was 91 per cent silica sand to 9 per cent resin, although this varied in some instances particularly with other resin types. Mixing of the resin-sand mixture was accomplished in a commercial dough mixer for a period of 10 min.

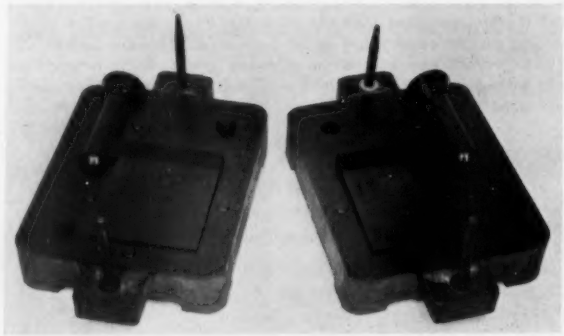


Fig. 1—Wedge pattern plates, 10 x 12 in.

b) Manufacture of a number of molds, using a 10x12-in. test pattern plate (Fig. 1) gated in such a manner as to enhance the tendency of the mold material to cut or wash, and incorporating a number of knife-edge corners for pattern reproducibility evaluations. In molding, the dwell and cure cycles were standardized, except where a specific recommendation was made by the manufacturer as to the most suitable cycle, or where deviations from the above were found to yield superior results. The test molds were inspected and evaluated, during the molding process and after curing, for such characteristics as:

1) Odor
2) Shell thickness
3) Ease of stripping
4) Sharpness and detail
5) Glaze and smoothness
6) Strength
7) Flowability
8) Tendency to "peel" or drop prematurely

During the later production operations in the New York Naval Shipyard Foundry, an intricate production casting was substituted for the test plate, although the method of test and evaluation of molds remained substantially identical.

c) Casting the assembled and bedded test molds in an aluminum alloy (Navy Department Specification

46A1, cl. 3 or Alcoa 356) at 1250 F; cleaning by soft brushing, and evaluating the casting products for:

1) Sharpness and detail
2) Washing and impingement defects
3) Metal penetration
4) Premature mold failure
5) General surface finish

The later tests which made use of the production pattern exhibited a tendency of some resins to promote premature mold breakdown.

The melamine and urea-formaldehyde resins tested in accordance with the above program proved to be inferior to the phenol-formaldehyde types. Molds made with these resins were characterized by rougher surfaces, excessive edge friability, disagreeable and in some instances toxic odors and comparatively low strengths, while the castings, with the exception of those produced by some melamine resins, displayed rougher surfaces and occasional defects due to premature mold collapse. These resins at present are not considered entirely suitable for shell molding. A few preliminary tests with resins of the epoxy group produced inferior shells of poor strength.

The results obtained with the phenol-formaldehyde resins indicated that the most generally satisfactory resins for molds are those with low to moderate flow characteristics, high melting points and fast cure rates. The most important variables affecting resin suitability are flow, cure, cure rates, melt viscosity and melting point. It is entirely conceivable that resins with different physical properties may be required for certain casting designs. Resin producers are currently making a great deal of progress, since the advent of mechanized equipment, in designing resins for specific production applications. Improvements have been effected in cure rates, flow and anti-distortion characteristics to the point where in most instances selected resins from most producers may be interchanged in production without affecting shell or casting quality. Considerable development work is also underway in determining, quantitatively, physical property characteristics such as tensile and flexural strength of various resin-sand mixtures in varying concentrations.

Cores made by the shell molding process may require resins of different characteristics than that employed for molds. In the experience of the author, cores with sharp radii require a resin with longer flow properties to avoid surface tearing. This applies whether the core is made by the "blowing" or "dumping" technique.

Parting Agents

A silicone water emulsion (2-5 per cent by volume) is being utilized almost exclusively on "seasoned" patterns as a mold release agent. The practice employed by the New York Naval Shipyard is to spray this emulsion on the hot pattern plate after four to five mold halves have been made. In some commercial operations the hot pattern face is dipped into the mold release solution.

In the initial development work conducted at the Material Laboratory many compounds were investigated by a practical performance evaluation test for their suitability as a mold release agent. Those compounds investigated were:

a) Paraffin wax
b) Silicone jelly
c) Polyethylene glycol products
d) Silicone oil and water emulsions
e) Polytetrafluoroethylene
f) Butyl stearate
g) Finely divided molybdenum in a carrier

The method of test employed in evaluating the liquid compounds consisted of applying the lubricant to the hot pattern plate surface by brush coating or spraying and subjecting the patterns to standardized and repetitious moldmaking cycles. Observations were then made of the following factors:

a) The relative amount and method of application required for the best performance
b) Ease of stripping
c) Cleanliness of pattern surface after stripping
d) Sharpness of mold edges
e) Mold smoothness
f) Number of successful strips for each application

In the case of the polytetrafluoroethylene product, a preparation which was applied as a semi-permanent fused plastic coat, an additional life test was performed to evaluate this compound.

In general, the silicone group displayed the best performance from the standpoint of ease of application, number of strips per application, cleanliness of molds and patterns, sharpness of molds, lack of carbon deposition, resistance to the cycling temperatures involved, and chemical inertness.

The most favorable results were obtained with silicone water emulsion concentrates. The polyethylene glycol products in alcohol and aqueous solutions proved to be undesirable in that considerable sticking was experienced. The polytetrafluoroethylene coat yielded excellent results from the standpoint of stripping ease over a long run during which more than 175 molds were made and stripped with a single application, but the mode of application is tedious and time-consuming, requiring careful preparation and successive oven-cured coatings. Some difficulty was experienced in obtaining a satisfactory coating on sharp corners. Once applied, however, it is extremely tenacious and resists a 600 F curing cycle satisfactorily. Ultimate local decomposition, however, and the resultant labor in stripping off the remaining coating in order to fuse a new coating on the pattern plate, makes the method generally undesirable. Butyl stearate decomposed readily at working temperatures, accompanied by dense fumes and staining of the patterns. Stripping performance was exceedingly poor. The preparations of finely divided molybdenum in a carrier, applied by spray and by swab, were similarly poor in performance, with considerable mold sticking experienced.

Mechanics of the Process

Basically, the shell molding method utilizes the thermosetting properties of phenolic resins to provide a bond material for silica grains in the construction of a mold. The use of urea and phenolic resins is by no means novel in the production of sand cores and molds. However, shell molding techniques are unique in that neither temper water nor additional bond materials are employed. In fact, at the present state of development, a dry, free flowing mixture is



Fig. 2 — Rollover machine, 18 x 22 in.

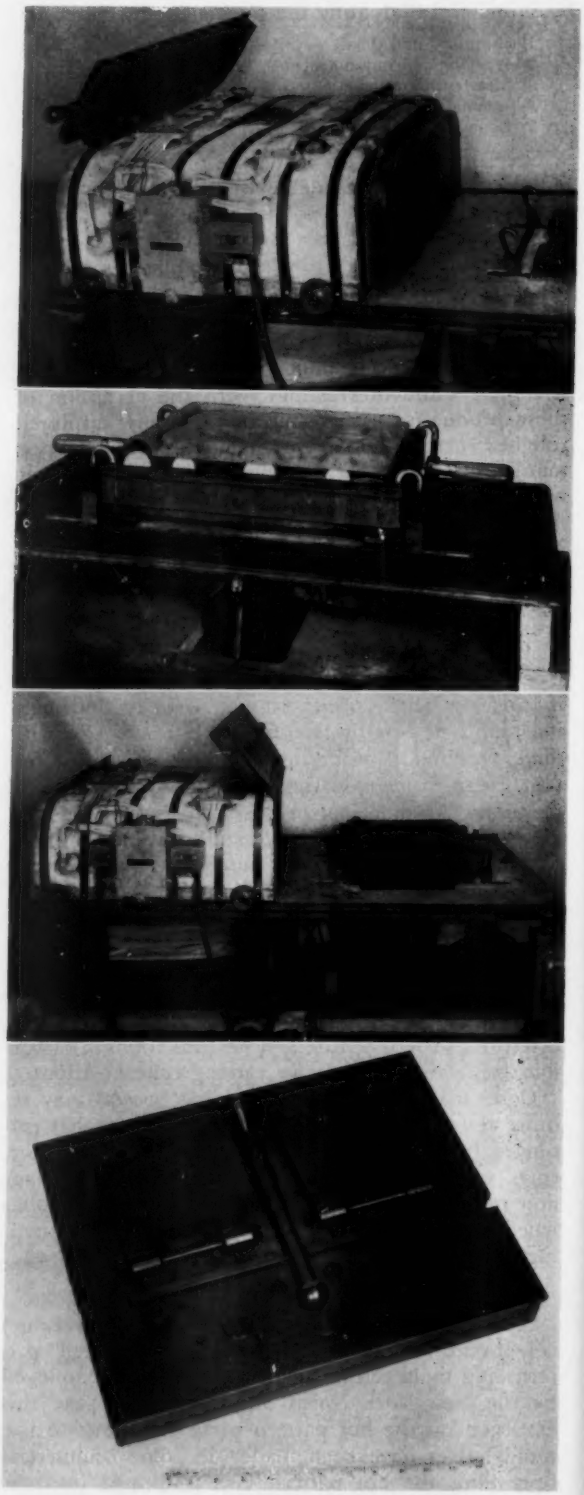
considered essential in the production of shell molds with good surface qualities.

The equipment necessary in the manufacture of shell molds may range from the simple "dump box" rigging as utilized for jobbing operations in the New York Naval Shipyard Foundry and illustrated in Figs. 2, 3, 4 and 5, to highly mechanized and fully automatic machinery. One advantage of the shell molding process is that it is adaptable to highly mechanized operations.

The moldmaking operation is neither involved nor difficult, and should not exceed 2 min. per cycle with the crudest rigging. Metal half patterns and attached runners and gates are utilized which are mounted accurately on metal pattern plates (Fig. 6) in much the same manner as in long-run molding machine practice. A set of beveled bosses on one plate and a corresponding set of cavities on the other make for accurate registration in assembling mold halves, and practically eliminates shifts. In some instances registration grooves deployed around the periphery of the pattern plate are used for assembling mold halves.

The pattern assembly is brought to a suitable temperature for forming the mold in a heating unit or with heating elements. In general, pattern temperatures range from 350 to 500 F for this phase of the process, and may be heated with strip heater units in a dutch-type oven as illustrated in Fig. 3, or by ceramic gas burners or conventional gas burners. Occasionally, heating elements or gas burners are used underneath the plate and employed with a curing oven in order to maintain the desired temperature head of the pattern material so that a continuous operation may be maintained with one pattern plate. In some instances, heating devices underneath the plate are utilized in conjunction with infra-red heating installations for curing the shell mold.

After the pattern plate has attained the desired temperature, it is sprayed with or dipped in a mold release agent and positioned, pattern down, over the dump-box (Fig. 2) which contains the resin-sand mixture. The dump-box is then inverted quickly, which causes the investment to fall and envelop the pattern and plate. The resinous binder material melts and flows under the influence of the heat of the plate, causing the resin-sand mixture to adjust itself closely to the outline of the pattern, building up a uniform coating as the heat penetrates the mass. After approximately a 6-15 sec dwell or contact period for a 3/16



Figs. 3, 4, 5, 6 (top to bottom). Fig. 3—Electric strip heater curing oven. Fig. 4—Stripping a cured mold half. Fig. 5—Oven and stripper table unit. Fig. 6—Bronze double test bar pattern plate, 14 x 18 in.

to ¼-in. thick shell, depending on the practice employed and the casting design, the pattern plate with the coating of set but uncured investment is removed from the dump-box and is ready for the curing operation. It is obvious that in this stage of the process the character of the sand and the flow, melting point, melt viscosity and curing rate properties of the resin are important variables in relation to a specific casting design.

The plate and adhering coating are then placed in a heating unit for curing. For the particular sand and resin combinations employed at the New York Naval Shipyard 1 min at 600 F has been found to be sufficient for curing. At higher or lower temperatures the curing cycle is correspondingly shorter or longer. During curing the resin component in the investment material is converted into a hard, insoluble plastic which holds the silica grains strongly together. After curing, the half-molds are stripped from the plate with the aid of spring return stripping pins, as illustrated in Fig. 4. In some instances it has been found necessary to place the stripped molds on bottom boards immediately and to weight them in order to minimize distortion and to reproduce close dimensional tolerances. Mold halves are closed in a variety of ways such as bolting, pasting, spring clamps or sealing tape, and backed up with a vibrated material such as steel shot or sand for casting. Cores can be produced with a cycle very similar to that employed for molds, the investment generally being blown or dumped into a heated core box, with the excess mold material being slushed out to provide a thin, hollow core.

It is essential that the shell mold or core be cured uniformly and sufficiently. Undercuring may result in some degree of dimensional instability during the casting operation. The control of the curing operation is best determined by control tests of tensile and flexural strength of the investment mixture. Some operators, however, depend on a particular color characteristic in determining the degree of cure when the sand and resin utilized are standardized.

Process Technique

Resin-Sand Blending. The blending of the resin-sand mixture may be accomplished satisfactorily in a variety of ways. Suitable mixtures have been prepared in bakery dough mixers, concrete mixers, pharmaceutical dry blenders and conventional sand mullers. At the New York Naval Shipyard Foundry a conventional type sand muller is utilized for blending. In the operation of this muller for this purpose, the wheels are raised approximately 1 in. from the pan. The pan temperature should not be permitted to reach the point where the resin will flow excessively and start a premature cure. Generally, about 7 min is sufficient time for mulling, although additional time may be necessary if the resin is lumped.

The muller or mixing apparatus, if not totally enclosed, should be provided with a ventilating hood to prevent dusting in the surrounding area. In that regard, some studies were made to determine the effect of wetting agents on investment mixes as a means of reducing the resin dusting and segregation encountered in mixing and moldmaking. Two liquid phenol-formaldehyde resin types were tested by adding 0.25 to 1 per cent to the dry base sand, and mulling, before adding the resin bond. Observations were made on the tendencies of the mixer to dust and segregate during and after the mixing cycle. The results indicated that dusting was inhibited appreciably by the addition of 1 per cent of liquid resin, with little effect noted in quantities of the order of 0.25 per cent. A tendency of the resin-sand mix to lump with liquid phenol-formaldehyde resin was noted but continued mixing abated this condition. Small quantities of kerosene (0.25 per cent) in the formulations inhibited dusting more successfully than equal percentage additions of the liquid phenol-formaldehyde resins.

Other investigators are currently exploring methods of improving resin efficiency by coating the sand grain with a resin film or a polar compound. While none of these techniques are in current use in volume on production operations, it is believed that ultimately significant reductions in the amount of resin utilized may be effected.

Some development work on resin-sand additives was conducted at the New York Naval Shipyard to determine the effect of certain types of additive materials on shell mold and casting quality. The materials tested were of the following general classes:

a) Silicone and zircon flours

b) Iron oxide

The results of a series of moldmaking and casting tests indicated that there was a slight improvement in surface finish on castings in which 10 per cent by weight of silica flour had been added to the shell mold. On medium carbon steel castings zircon flour additions in the order of 7.5 to 10 per cent substantially improved surface quality conditions. Large numbers of production castings have been cast successfully at New York Naval Shipyard in aluminum alloys, gray iron, ductile iron, medium carbon steel and 410 stainless steel in which the indicated zircon flour additions have been made to a base sand of AFS 230 with a resin content of 10.5 per cent.

No significant improvement in surface finish was noted with iron oxide additions up to 5 per cent. However, iron oxide additions did appear to benefit resin-sand mixtures which had a tendency to "peel" when the pattern plate was inverted after the contact period.

Pattern Equipment. The pattern and plate material used principally, to date, in the shell molding process are alloy cast iron, the so-called "non-shrink" steels, and a heat-treatable copper-nickel-silicon alloy. At the New York Naval Shipyard cast iron patterns are preferred and are utilized, wherever possible. Excellent over-all performance was obtained with cast iron patterns, particularly in resistance to "sticking" which is probably due to its high graphite population on the surface. On occasion, hand finished bronze castings were utilized and operated satisfactorily, but proved to be subject to scratching, nicking or swaging, to a degree not normally experienced with iron or steel. Pattern plates were made, depending upon the design, either by hand finishing cast metal patterns and mounting on a machined pattern plate, or



Fig. 2 — Rollover machine, 18 x 22 in.

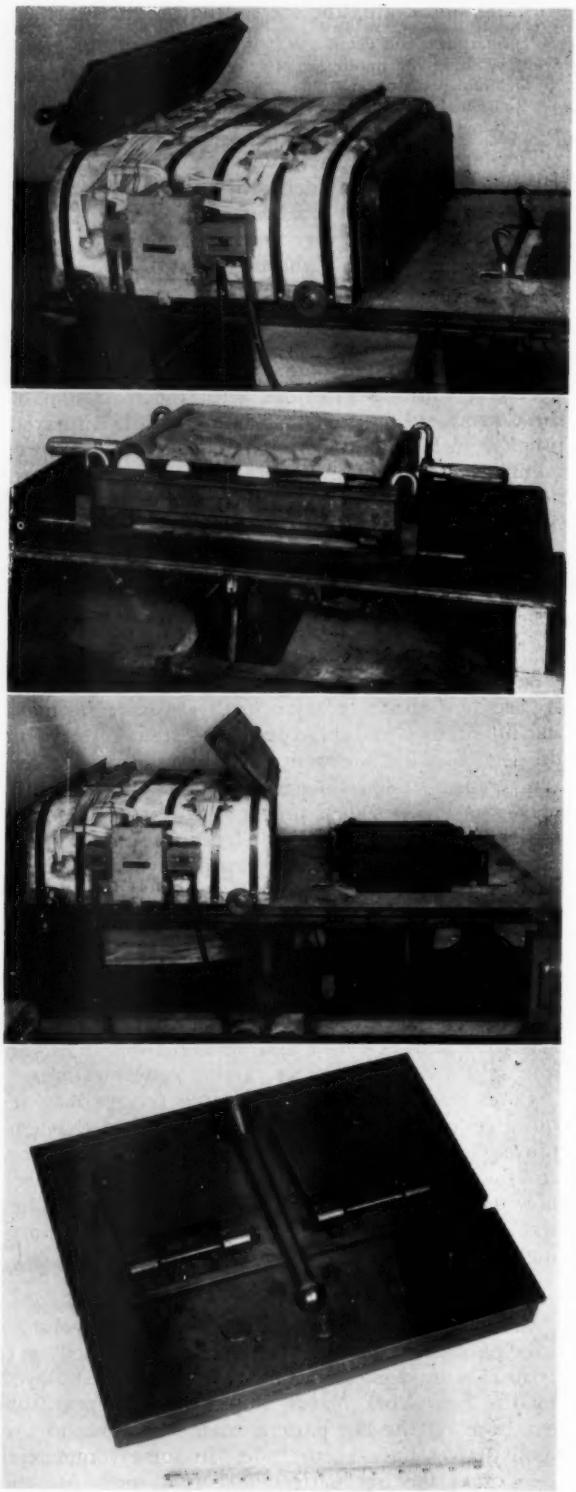
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The pattern assembly is brought to a suitable temperature for forming the mold in a heating unit or with heating elements. In general, pattern temperatures range from 350 to 500 F for this phase of the process, and may be heated with strip heater units in a dutch-type oven as illustrated in Fig. 3, or by ceramic gas burners or conventional gas burners. Occasionally, heating elements or gas burners are used underneath the plate and employed with a curing oven in order to maintain the desired temperature head of the pattern material so that a continuous operation may be maintained with one pattern plate. In some instances, heating devices underneath the plate are utilized in conjunction with infra-red heating installations for curing the shell mold.

After the pattern plate has attained the desired temperature, it is sprayed with or dipped in a mold release agent and positioned, pattern down, over the dump-box (Fig. 2) which contains the resin-sand mixture. The dump-box is then inverted quickly, which causes the investment to fall and envelop the pattern and plate. The resinous binder material melts and flows under the influence of the heat of the plate, causing the resin-sand mixture to adjust itself closely to the outline of the pattern, building up a uniform coating as the heat penetrates the mass. After approximately a 6-15 sec dwell or contact period for a 3/16



Figs. 3, 4, 5, 6 (top to bottom). Fig. 3—Electric strip heater curing oven. Fig. 4—Stripping a cured mold half. Fig. 5—Oven and stripper table unit. Fig. 6—Bronze double test bar pattern plate, 14 x 18 in.

to ¼-in. thick shell, depending on the practice employed and the casting design, the pattern plate with the coating of set but uncured investment is removed from the dump-box and is ready for the curing operation. It is obvious that in this stage of the process the character of the sand and the flow, melting point, melt viscosity and curing rate properties of the resin are important variables in relation to a specific casting design.

The plate and adhering coating are then placed in a heating unit for curing. For the particular sand and resin combinations employed at the New York Naval Shipyard 1 min at 600 F has been found to be sufficient for curing. At higher or lower temperatures the curing cycle is correspondingly shorter or longer. During curing the resin component in the investment material is converted into a hard, insoluble plastic which holds the silica grains strongly together. After curing, the half-molds are stripped from the plate with the aid of spring return stripping pins, as illustrated in Fig. 4. In some instances it has been found necessary to place the stripped molds on bottom boards immediately and to weight them in order to minimize distortion and to reproduce close dimensional tolerances. Mold halves are closed in a variety of ways such as bolting, pasting, spring clamps or sealing tape, and backed up with a vibrated material such as steel shot or sand for casting. Cores can be produced with a cycle very similar to that employed for molds, the investment generally being blown or dumped into a heated core box, with the excess mold material being slushed out to provide a thin, hollow core.

It is essential that the shell mold or core be cured uniformly and sufficiently. Undercuring may result in some degree of dimensional instability during the casting operation. The control of the curing operation is best determined by control tests of tensile and flexural strength of the investment mixture. Some operators, however, depend on a particular color characteristic in determining the degree of cure when the sand and resin utilized are standardized.

Process Technique

Resin-Sand Blending. The blending of the resin-sand mixture may be accomplished satisfactorily in a variety of ways. Suitable mixtures have been prepared in bakery dough mixers, concrete mixers, pharmaceutical dry blenders and conventional sand mullers. At the New York Naval Shipyard Foundry a conventional type sand muller is utilized for blending. In the operation of this muller for this purpose, the wheels are raised approximately 1 in. from the pan. The pan temperature should not be permitted to reach the point where the resin will flow excessively and start a premature cure. Generally, about 7 min is sufficient time for mulling, although additional time may be necessary if the resin is lumped.

The muller or mixing apparatus, if not totally enclosed, should be provided with a ventilating hood to prevent dusting in the surrounding area. In that regard, some studies were made to determine the effect of wetting agents on investment mixes as a means of reducing the resin dusting and segregation encountered in mixing and moldmaking. Two liquid phenol-formaldehyde resin types were tested by adding 0.25 to 1 per cent to the dry base sand, and mulling, before adding the resin bond. Observations were made on the tendencies of the mixer to dust and segregate during and after the mixing cycle. The results indicated that dusting was inhibited appreciably by the addition of 1 per cent of liquid resin, with little effect noted in quantities of the order of 0.25 per cent. A tendency of the resin-sand mix to lump with liquid phenol-formaldehyde resin was noted but continued mixing abated this condition. Small quantities of kerosene (0.25 per cent) in the formulations inhibited dusting more successfully than equal percentage additions of the liquid phenol-formaldehyde resins.

Other investigators are currently exploring methods of improving resin efficiency by coating the sand grain with a resin film or a polar compound. While none of these techniques are in current use in volume on production operations, it is believed that ultimately significant reductions in the amount of resin utilized may be effected.

Some development work on resin-sand additives was conducted at the New York Naval Shipyard to determine the effect of certain types of additive materials on shell mold and casting quality. The materials tested were of the following general classes:

a) Silicone and zircon flours
b) Iron oxide

The results of a series of moldmaking and casting tests indicated that there was a slight improvement in surface finish on castings in which 10 per cent by weight of silica flour had been added to the shell mold. On medium carbon steel castings zircon flour additions in the order of 7.5 to 10 per cent substantially improved surface quality conditions. Large numbers of production castings have been cast successfully at New York Naval Shipyard in aluminum alloys, gray iron, ductile iron, medium carbon steel and 410 stainless steel in which the indicated zircon flour additions have been made to a base sand of AFS 230 with a resin content of 10.5 per cent.

No significant improvement in surface finish was noted with iron oxide additions up to 5 per cent. However, iron oxide additions did appear to benefit resin-sand mixtures which had a tendency to "peel" when the pattern plate was inverted after the contact period.

Pattern Equipment. The pattern and plate material used principally, to date, in the shell molding process are alloy cast iron, the so-called "non-shrink" steels, and a heat-treatable copper-nickel-silicon alloy. At the New York Naval Shipyard cast iron patterns are preferred and are utilized, wherever possible. Excellent over-all performance was obtained with cast iron patterns, particularly in resistance to "sticking" which is probably due to its high graphite population on the surface. On occasion, hand finished bronze castings were utilized and operated satisfactorily, but proved to be subject to scratching, nicking or swaging, to a degree not normally experienced with iron or steel. Pattern plates were made, depending upon the design, either by hand finishing cast metal patterns and mounting on a machined pattern plate, or

by machining from plate or bar stock with a pantagraph unit. The cycling temperatures developed and practiced at the New York Naval Shipyard permitted the use of aluminum sprues, runner bars and risers with cast iron pattern plates. They were attached separately to the pattern plate which permitted easy removal for modification, if necessary. When plate weight was a factor, aluminum patterns and ribbed aluminum pattern plates were utilized satisfactorily. An example of an all-aluminum pattern plate for a motor bracket casting is illustrated in Fig. 7. In this particular casting no draft can be tolerated on the mounting face of the bracket, which represents a 1½-in. draw. As little as ¼° draft can be employed successfully in some pattern plate designs where necessary. As many as 1000 shell mold halves have been made with this aluminum pattern plate, and it still is serviceable. However, it is obvious that in view of the temperatures involved in the cycling, wherever the maximum in tolerances and surface quality are to be obtained, aluminum should not be the alloy selected. Nevertheless, as a trial expedient or for certain casting requirements that do not involve large production runs, aluminum matchplates may be utilized.



Fig. 7—Pattern plate for motor bracket, 18 x 22 in.

The pattern plate is the heart of the shell molding process, and must be well engineered if production difficulties are to be avoided. The availability of the proper machine tools and of personnel skilled in tool and die or metal patternmaking is essential for manufacturing the patterns or effecting quick changes in gating and heading. The pattern plate should be ribbed, and designed with uniform sections and sufficient thickness to retain the proper temperature head for setting the investment on a continuous operating basis. Frequently, on single-stage machines, electric strip heaters or gas burners are incorporated on the underside of the plate. Generally, rivet-type heads are utilized for stripping pins, and the greater the area of each head the more efficient the stripping action, provided they are spaced uniformly around the pattern. Stripping pins should be outside the mold cavity and equipped with properly tempered springs, preferably of stainless steel, or a mechanical arrangement for automatic return after the release of the mold. Bolting lugs have also been employed on patterns manufactured at the New York Naval Shipyard. These are illustrated on the pattern plate in Fig. 7. These lugs are about 5/16-in. high and ¼-in. in diameter, with a generous draft, and can be punched out to accommodate a bolt and nut. These lugs also serve to prevent "drops" and peeling when the pattern plate is removed from the molding machine after the dwell period and inverted to its normal position.

It has been determined that for the aluminum alloys, tin bronzes, cast iron and carbon steels that normal linear shrinkage for the alloy involved may be employed. However, it should be noted that in any precision casting process direct experience involving shrinkage characteristics of different casting designs must be developed since the total shrinkage in castings will differ depending upon design, size and restraint during solidification.

It is the practice at the New York Naval Shipyard Foundry to "season" a new pattern plate. This is accomplished by 1) removing all grease and oil from the pattern with a solvent; 2) heating the pattern to 600 F and holding at that temperature for 3 to 4 hr; 3) removing additional grease and oil on face of pattern with a solvent; 4) applying by rubbing a thin film of silicone grease; 5) reheating pattern to 600 F; and 6) manufacturing several molds which generally are destroyed. Once a pattern plate has been seasoned it is cleaned with a solvent at the end of a shift. To prepare it for operation at the beginning of a shift a 50 per cent solution of a silicone water emulsion is sprayed on the hot face of the pattern.

Gating and Heading. Sound principles of gating and heading cannot be ignored in this casting process, if castings free from objectionable internal defects are to be manufactured. However, the principal difficulty surrounding shell molding in the early stages of development was that new principles of gating had to be adopted. The pouring rate for shell molded castings is considerably increased as compared with green sand castings for the same gating system primarily because of the lessened resistance of the mold wall and increased permeability of the shell. As a result, the best performance for a shell molded gating system in all alloys is obtained with areas of sprues, runner bars and ingates which are considerably less in volume than the system ordinarily applied to the alloy involved in conventional practice. Generally, for aluminum alloys, tin bronzes, malleable, gray and ductile iron, choked ingates and the judicious use of dross catchers must be practiced at all times. In addition, the volume of riser metal may be reduced for some alloys. In general, this will result in an increased yield for shell molded castings as opposed to conventional practice.

Gating methods which provide for pouring molds on end will lend themselves to the best productive arrangement for supporting the molds and pouring off either with or without backup. However, there are instances in the aluminum alloys and tin bronzes for smaller castings which are "treed" and poured vertically where penetration is evidenced on the castings in the lower part of the mold. The casting sur-

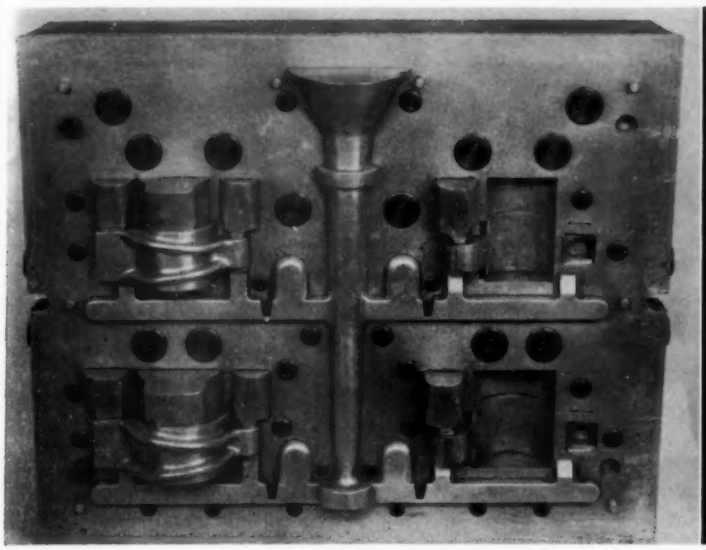


Fig. 8 (above)—Example of typical ductile iron shell mold gating system.

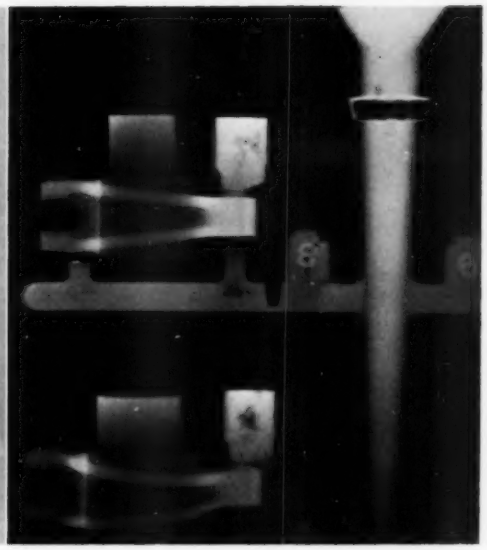


Fig. 9 (right)—Positive print of gammagraph illustrating dross catchers in ductile iron gating system.

face generally improves on castings in this category from the bottom to the top of the mold. In some cases, this has been corrected by adding silica flour to the investment formulation or by drastic modification in the gating system. However, it has been found most expedient to pour these castings horizontally. The medium carbon steels produced in shell molds at the New York Naval Shipyard Foundry are all poured on the flat for optimum results in surface finish.

No specific area relationships for sprue, runner bar and ingate have been finalized for all alloys. Generally, it has been found desirable to taper the downsprue in an area ratio of 4:1. The runner bar should be greater in area of the sprue and the ingate should then be choked. Dross catchers should be provided, of course, in strategic locations in the system for the high drossing alloys.

An example of a shell molding gating system for a ductile iron staging clamp casting, which incorporates extensive choking and dross catching arrangements is illustrated in Fig. 8. The effectiveness of the dross catcher is illustrated in the positive print of a radiograph of this gating system shown in Fig. 9.

An example of increased yield which may be obtained in shell molded castings as compared with green sand castings is shown in Fig. 10, which is a photograph of the master pattern plate arrangement for a 0.45 per cent carbon steel, 12 in. wire rope block sheave wheel. To produce this casting radiographically sound by green sand practice necessitated risers on the periphery or chill inserts in the core around the root of the groove. As illustrated in Fig. 11, this casting is currently being shell molded with a conventional zircon core and no chills.

In addition to producing a radiographically ac-

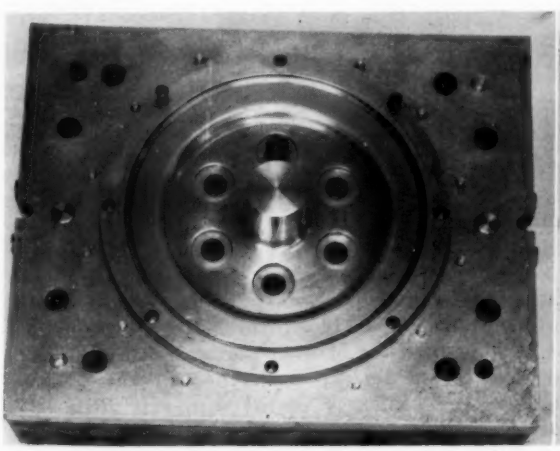


Fig. 10 (above)—Pattern plate, 18 x 22 in., for 12-in. dia. wire rope sheave.

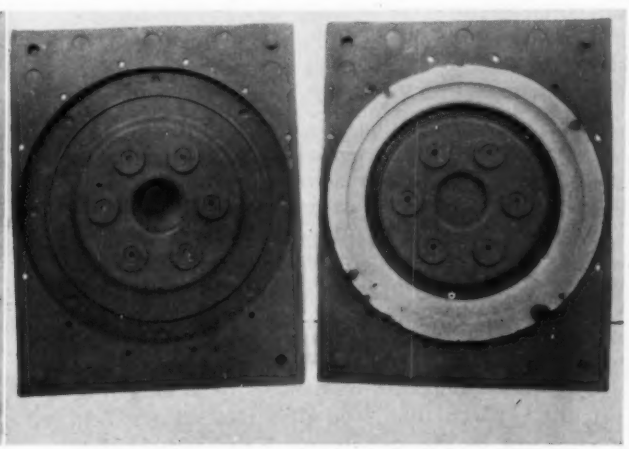


Fig. 11 (right)—12-in. dia. wire rope sheave mold halves, with zircon core.

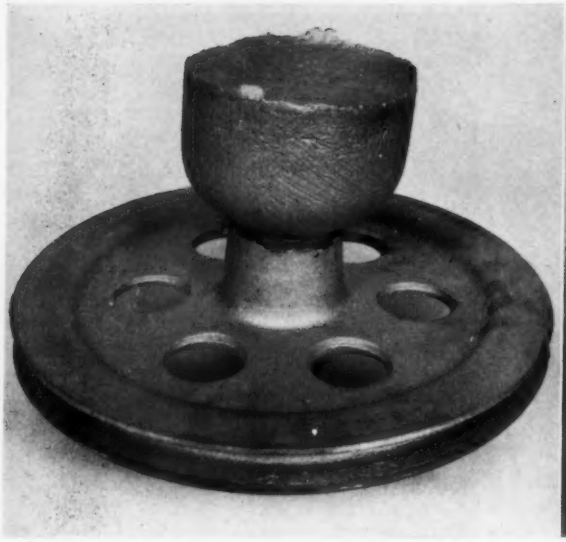


Fig. 12 (above)—12-in. dia. wire rope sheave, as-cast.

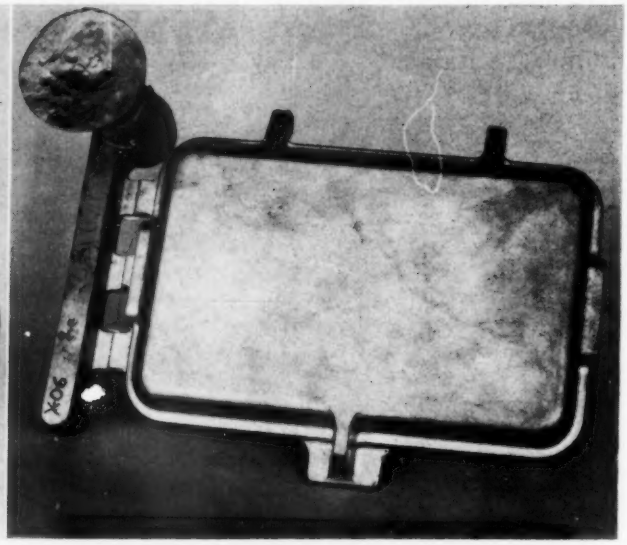


Fig. 13 (right)—5 per cent Si-Al ordnance cover—as-cast.

ceptable casting, all machining has been eliminated with the exception of a facing and boring operation on the hub and a filing operation on the root of the groove. This casting, shown as-cast in Fig. 12, is an illustration of an application of shell molding where improved yield and the elimination of certain machining operations justified the use of this method even though very close tolerances and high surface finish were not particularly important.

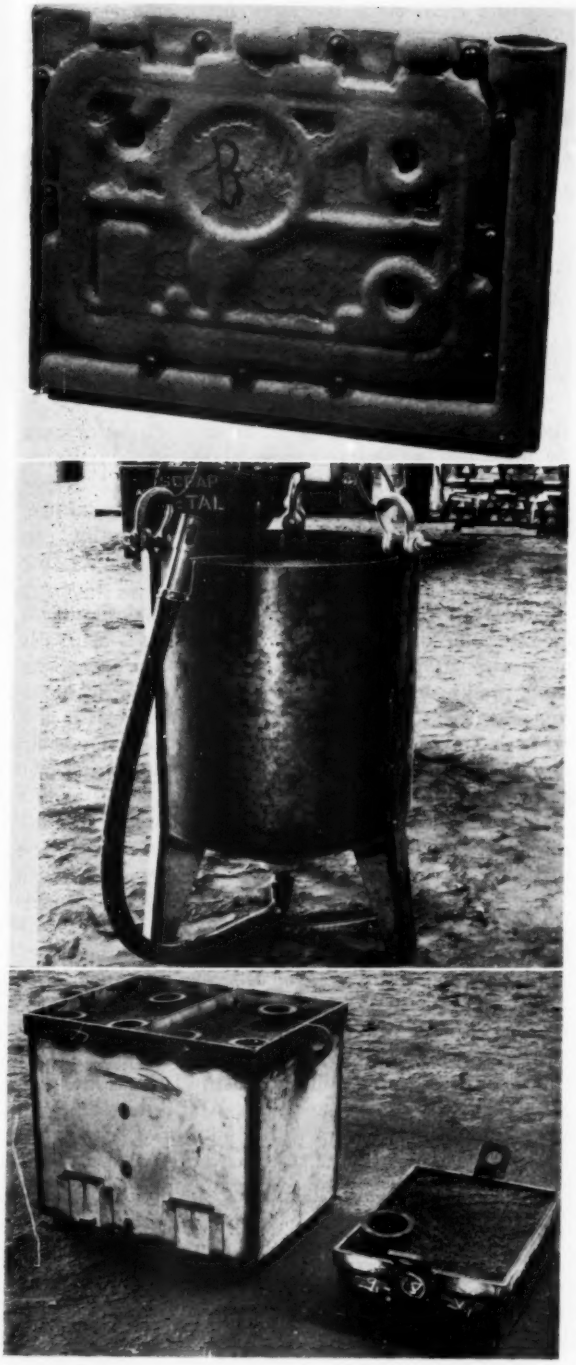
Dimensional Tolerance. The shell molding process can be considered a "precision" casting process. It is generally thought of as being midway between sand casting and the lost-wax technique. However, it is believed that if castings approaching the size of those commonly made in shell molds were manufactured by the lost-wax process, no greater degree of tolerance would be obtained. Tolerances of ± 0.003 to ± 0.005 in./in. are considered commercially feasible in aluminum alloys, tin bronzes and gray iron. On alloys which may require heat treatment such a ductile and malleable iron, steel and some aluminum alloys consideration should be given to the effect of growth and the control of distortion on critical casting dimensions. Generally, a more liberal tolerance requirement is necessary on the larger castings in these alloys.

The parting line dimension will usually present the greatest difficulty in reproducing close tolerances and will generally require a wider tolerance than indicated above. Hence, all master pattern plates should be designed carefully with this in mind, and wherever possible single-draw patterns should be utilized. The manner in which mold halves are closed and backed up will also affect parting line tolerance considerably. Extremely close tolerances may be obtained with internal cores in all alloys and in castings produced in the aluminum alloys. An ordnance cover casting (Fig. 13), cast in aluminum-silicon alloy (No. 43) was held to $\pm .015$ in. on the over-all length and width and to ± 0.005 in. in the width and depth of the gasket groove.

Shell molding, however, has one basic limitation insofar as maintaining close dimensional tolerances are concerned, and that appears to be the inability to achieve O.D. concentricity on a vertically parted cylinder with silica sand shell molds. The O.D. dimension of a shell mold along the parting line, which is held by some mechanical arrangement or by pasting, and acts essentially as a stiffening rib, will frequently produce castings up to 0.035 in. smaller in diameter than the O.D. dimension, 90 degrees from the parting line. In some instances an actual flat along the parting line can be detected in the casting. "Faking" the master pattern, i.e., making a proper allowance for this condition will overcome this deficiency in part. To avoid excessive O.D. eccentricity on vertically parted cylinders, it is essential that close control of resin/sand mixtures, cycling, shell thickness, and method of backing up be exercised. The use of molding materials with high hot strength and flexural strength will also serve to eliminate this deficiency found with silica sands.

The apparent stability of shell molds and cores with regard to resistance to deterioration and moisture absorption, coupled with their structural strength and lack of dimensional change, make them particularly suitable for storage over indefinite periods. Shell molds may be shipped to various plants of a company for pouring, thus reducing duplication of master pattern equipment. There is at least one shell molding installation in this country where shell molds may be procured for pour-off by a conventional sand foundry.

Shell molds generally have a high degree of cold permeability which is apparent by the ease with which smoke can be blown through them. Hence, if a backing material of high permeability is utilized, the gases generated from the decomposing organic binder are adequately vented with very little back pressure. This, coupled with low frictional losses due to the smooth mold face, permits the running of sections which normally would be a problem in conventional sand molds.



Figs. 14, 15, 16 (top to bottom). Fig. 14—Assembled mounting panel mold. Fig. 15—Shot loading tank and valving hose. Fig. 16—Bedding boxes with molds prepared for casting.

Mold Closing. Shell molds may be closed in a variety of ways. Clamping the outside edges with spring wire, bolting, utilizing a heat-setting resin and sealing tape are all practiced. The choice of method will depend primarily on the amount of finning which can be permitted and the degree of tolerance required. It has been found that bolting the molds closely about the casting cavities will yield the best results. Bolting is a practice employed extensively at the New York Naval Shipyard Foundry. A torque-controlled electric nut runner is utilized, and while bolting itself is accomplished expeditiously, the reclamation or cost of the bolts probably makes this the most expensive of all the techniques. However, the results obtained or the requirements for the casting frequently will dictate this method of mold assembly. A mold closed by bolting is illustrated in Fig. 14.

In some instances, no mechanical clamping device is utilized, and the molds are held together closely by the vibrated steel shot back-up material. It has been the general practice to back up molds with steel shot. The prime purpose of the back-up material is to resist deformation of the mold wall under the hydrostatic head of metal pressure. Other materials such as sand and a proprietary angular blasting grit have been used successfully. The use of metal shot, however, presents a critical material handling problem. Such difficulties as shot overheating, removal of fines from the shot, shot loss due to metal spillage, and the safety hazards involved when loose shot is on the floor in the pouring area, have not been fully resolved. Whatever material is used, however, should be vibrated during backing up and should have sufficient permeability, since this factor is an important one in venting mold gases. The 2-ton capacity shot loading tank with discharge hose and valving arrangement utilized for shell molding jobbing work at the New York Naval Shipyard foundry is shown in Fig. 15. The hopper is constructed of welded steel plate with a wire mesh screen attached to the open end for screening out mold fragments and tramp metal during reloading. Lifting pads permit the tank to be positioned over the mold bedding boxes by crane. For bedding welded steel boxes illustrated in Fig. 16 are utilized. The larger box accommodates four to eight vertically positioned molds and incorporates a gate-type discharge and crane lift pads for return of shot to the loading tank. The smaller box holds one horizontally positioned mold. Practically any degree of mechanization can be incorporated into the shot loading and bedding operation, depending upon the volume and different sizes of molds which are processed on a daily basis.

In some instances, when the total weight of the castings poured into shell molds is small, no back-up material is required. Molds of this type may be rocked vertically or postioned horizontally for pouring. Shell molds have also been stacked horizontally and poured, or in some instances positioned vertically and clamped together by pneumatic pressure. In order to obtain bearing surfaces on irregular contours, at the high points, molds are either "struck off" prior to curing or are ground after curing.

Cores. The technology of blowing shell cores of free-flowing resin-sand mixtures has not advanced as rapidly as moldmaking techniques. At the present time no commercial equipment is available to industry for the production of shell cores. All development work in coreblowing is being accomplished with equipment designed or adapted by the foundry in-

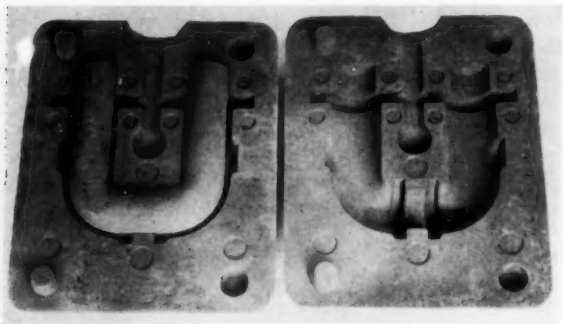


Fig. 17—By-pass mold, with core; cope and drag halves.

volved. The principal difficulties experienced in core-blowing have been resin segregation, vents clogged with cured resin-sand mixtures and core box wear. The resin segregation problem can be resolved satisfactorily by the use of wetting agents. Further development of a suitable procedure for coating sand grains with a resin film will undoubtedly eliminate segregation as a problem in itself. However, the problems of venting and core box wear have not been completely resolved. Figure 17 illustrates a shell core which was produced experimentally by blowing. The investment was blown upward and into a heated booked core box at 30 lb pressure. The core box was positioned and clamped with one open end over the blowhole, the other resting over a slotted air vent in a heat-resisting pad. The blow was of approximately 12-sec duration, after which the loose center portion of the core, unaffected by the heat, was "slushed" out, allowing a $\frac{1}{8}$-in. thick shell to adhere to the internal cavity. The box and adhering shell were cured in an oven at 600 F for 1 min, as in ordinary shell molding practice, after which the box was opened and the shell core stripped out. The cores produced by this technique, although tedious and obviously uneconomical because of the nature of the equipment employed, were uniformly thin, smooth and accurately sized, and in no case did the core produce casting defects through premature collapse, excessive hardness, or gas emission in gun metal bronze, beryllium copper, manganese bronze, gray cast iron or medium carbon steel.

Core Dumping Practice

At the present stage of development of the process core "dumping" practices appear to be more successful and more economical. Shell cores which have been produced by the dumping technique have produced excellent internal surface finishes to close dimensional tolerances in non-ferrous alloys, and in gray, ductile and malleable iron compositions. The principal difficulty with the simple dumping technique is that a uniform dense core surface cannot be obtained whenever a sharp radius or an intricate core design is encountered. Whenever an open surface structure on a core, or for that matter a mold, is obtained, it will result in a metal penetration or burn-in defect on the casting, and may necessitate a core wash for the high melting point alloys such as medium carbon steel unless this condition can be corrected by a procedure change. It has been reported recently in the literature that vibration techniques have been successfully employed in producing a smooth, dense surface on shell cores. Shell cores may be employed satisfactorily with green sand molds, particularly where smooth, accurate internal surfaces are required and the O.D. will be finished by machining. Conversely, shell molds may also be employed with dry sand cores. In the United States foundry industry today simple shell cores are being manufactured for both malleable, gray iron and ductile iron on a competitive basis with conventional dry sand cores produced by blowing, particularly when consideration is given to the indirect cost savings such as driers, expensive baking ovens and space requirements.

Mechanization. When the required volume of casting production is high, favorable opportunities exist for a complete mechanization of the shell molding system. One of the outstanding advantages of the process is the fact that lower grade skills may be utilized and that various degrees of mechanization can be incorporated into a system, depending upon the economics of the situation.

Industrial equipment currently being designed or recently placed on the commercial market is generally of two basic types, one in which the stations or operations are incorporated in a single, automatic molding machine, and the other a typical loop with separate stations for moldmaking, curing, stripping, coating the pattern and preheating. In the single-stage equipment one pattern plate is utilized and the rate of production is determined by the complete molding cycle. Hence, every effort is being exerted in the utilization of this equipment to employ resins with extremely fast cure rates and anti-distortion properties, and to maintain a sufficiently high temperature head on the pattern plate to permit continuous operation. One basic early difficulty in connection with the development of single-stage equipment was the inability to maintain the proper operating temperature on the pattern plate, necessitating reheating. To manufacture several different castings simultaneously with single-stage equipment, it would obviously be necessary to utilize more than one machine.

In the multi-stage machine several pattern plates are required for continuous production. These pattern plates may represent several different castings. In the multi-stage operation the dwell time will determine the rate of production. One serious disadvantage of the loop system is that in the event of a serious mechanical breakdown the whole line must be shut down.

The single stage and loop systems as a general rule will employ dutch-type ovens either electrically or gas heated, with automatic temperature control. In the multi-stage loop system variable belt speeds through the curing and preheating ovens can be maintained as desired. Some loop systems use an overhead investment dispenser, while others use the conventional mold inversion technique.

At the present time there are approximately 15 companies offering industrial shell molding equipment to the foundry industry. Figures 18 and 19 illustrate two types of automatic single stage machines

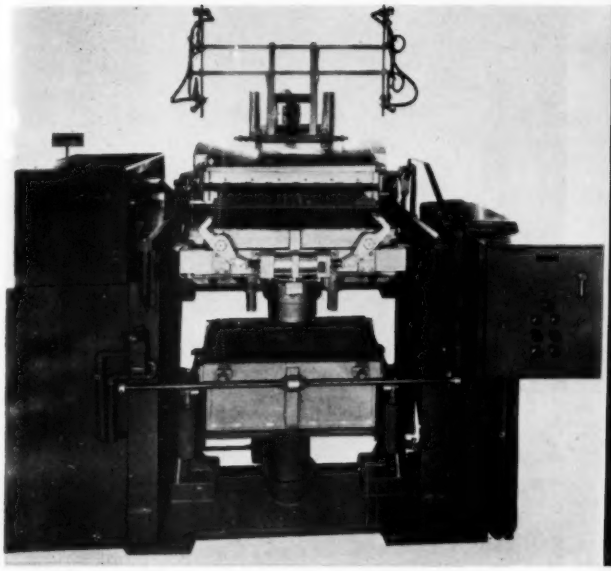


Fig. 18—Single stage automatic shell molding machine.

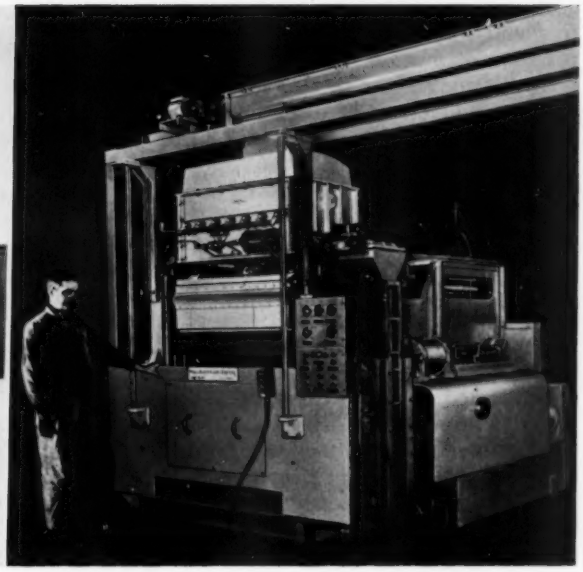


Fig. 19—Single stage automatic shell molding machine.

which are currently available in production on a variety of automotive and miscellaneous castings in gray iron, malleable and stainless steel. The machine shown in Fig. 18 accommodates two sizes of pattern plates, 26 x 41 in. and 20 x 30 in. According to the manufacturer shells are produced on a basic cycle of 45 to 60 shells per hour on patterns with a maximum height of 8 and 6 in., respectively. The curing ovens for the machine can be either gas-fired or electrically heated, and the floor space requirement for the larger unit is 10 x 10 ft. The machine illustrated in Fig. 19 will produce approximately a 26 x 41-in. shell with a 7-in. maximum draw at an approximate rate of 40 shells per hour. The curing oven is gas-fired. An example of a multi-stage rotary unit is illustrated in Figs. 20 and 21. This machine has 12 pattern stations and, according to the manufacturer, is capable of producing 500 shells per hour with one man. The pattern size utilized by this particular machine is 22 x 28 in. to a maximum draw of 8 to 9 in. The unit has a self-contained sand and resin blending unit that feeds both resin and sand in the correct mixture continuously to the 12 sand boxes attached to the 12 pattern stations. This unit has not as yet been installed nor operated in a production foundry and is still undergoing tests by the manufacturer.

By suitable masking of the master pattern, it is possible to produce one or more complete molds from each shell made by all the machines described. Obviously this would necessitate very accurate machining and mounting of the multiple patterns so that each of the shells may be utilized interchangeably.

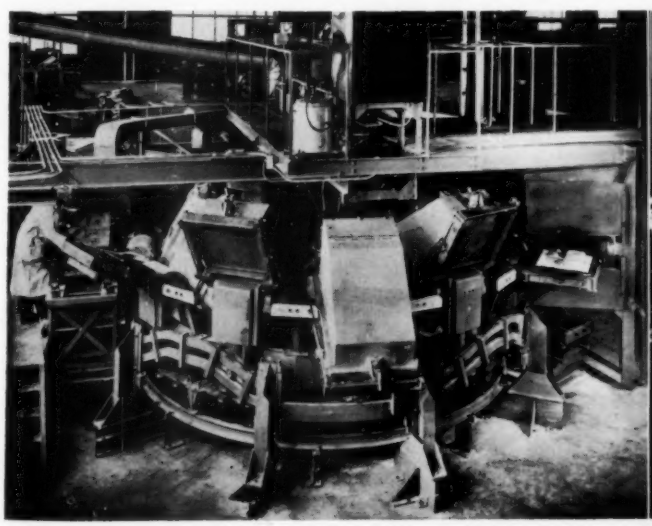


Fig. 20 (above)—Rotary shell mold machine.

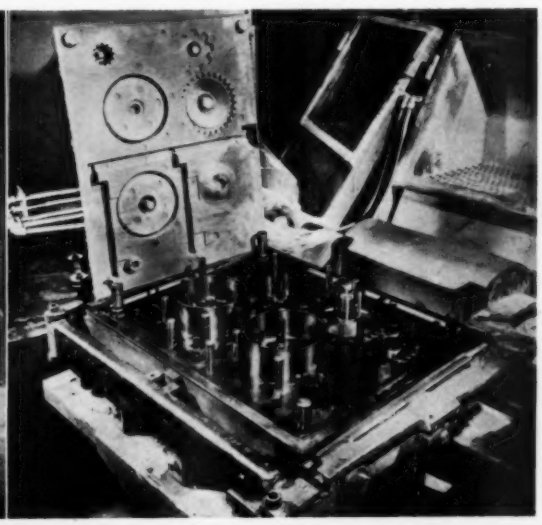


Fig. 21—Pattern plate and mold for shell mold machine.

Plant Safety. The pulverized and fine silica sands frequently used in the shell molding process may constitute a respiratory hazard. In addition the use of relatively high resin percentages increases the possibility of contact dermatitis, particularly with phenolic resins containing hexamethylenetetramine. As part of an industrial health survey of the shell molding installation at the New York Naval Shipyard conducted by the Shipyard's Industrial Health Department, it was determined that in a conventional resin-sand mixture under agitation there was an inversion in the concentration of sand and resin from a 91-9 mixture, so that at normal working distances, employees are exposed to concentration of approximately 90 per cent resin and 10 per cent sand. Part of the basic recommendations of this survey included the ventilation of the molding and curing areas and the mulling or mixing of sand-resin mixtures with a dust-proof cover. In highly mechanized installations where large tonnages of sand and resin are processed on a daily basis, consideration should be given to the possibility of dust explosions.

Metallurgy. There had been some claims in connection with the shell molding process that castings produced in shell molds backed with steel shot are subject to a greater skin chill than those poured in conventional green sand or in "unbacked" shell molds.

The work conducted on a bronze alloy, at the laboratory with which the author is associated did not confirm this concept. To investigate this claim qualitatively a simple wedge casting was poured in shell molds, which were backed up with various kinds of materials such as rammed green sand, steel blasting grit, medium steel shot, and copper shot along with a green sand control casting. All shell molds were 1/8-in. thick and all molds were poured at 2050 F. Figure 22 illustrates photomacrographs of longitudinally etched sections through these wedge castings. It will be noted that the grain size pattern in the green sand control casting is smaller than in any of the castings poured in plastic bonded shell molds regardless of the type of back-up employed. This is indicative of a greater rate of cooling on solidification in

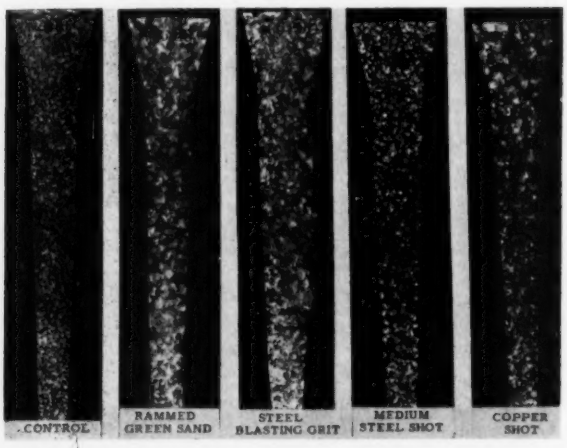


Fig. 22—Effect of varying back-up material on the macrostructure of shell-cast "G" bronze test plates.

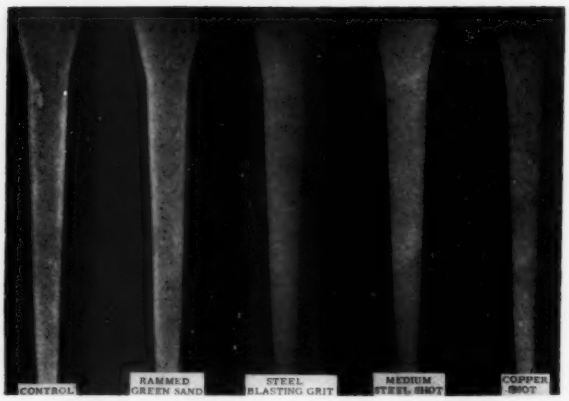


Fig. 23—Exographs of thin slices—effect of varying back-up material on the soundness of shell-cast "G" bronze test plates.

green sand than in plastic bonded shell molds, or a greater insulating effect of the plastic bonded sands. The same effect has been reported by other investigators in steel. It will also be noted in Fig. 22 that there does not appear to be any appreciable differences in cooling rate, reflected by grain size, between the copper-shot-backed shell mold and steel shot despite great differences in their thermal conductivity, which is another indication of the insulating effect of shell molds. Other investigators have reported that back-up materials for some of the higher temperature alloys seem to have a significant effect on cooling rate in ductile and gray iron. Some producers have backed-up shell molds in a dry silica sand in an effort to produce maximum as-cast ductility in spheroidal iron.

The effect of shell mold back-up on the internal soundness of bronze castings as compared with green sand castings in the same alloy is shown in Fig. 23, which represents positive prints of exographs of 1/8-in. thick slices of the specimens shown in Fig. 22. The greater degree of internal soundness of the shell molded castings as opposed to the green sand control will be noted. The green sand control casting, however, showed a greater amount of skin effect. In the shell molds, the specimens backed with steel and copper shot appeared to have a greater degree of internal soundness than the green sand and grit backed specimens, probably due to the higher permeability of the shot in venting mold gases.

Further investigations of the effect of back-up material on macrostructure and soundness utilizing other alloys and other backing materials were pursued. Standardized 1/8-in. shell wedge molds were prepared in the usual manner and two each were bedded in fine zircon sand, steel grit, medium steel shot, rammed green sand, and copper shot representing materials of widely varying thermal conductivity, together with two green sand molded control wedges. Four test series were run, one each in an aluminum alloy (5 per cent silicon) poured at 1250 F, 88-8-4 bronze at 2050 F, gray iron at 2750 F, and Navy class "B" steel at 2960 F. Thin slices (1/8-in.) were taken from all wedges 2 in. from the side opposite the gate for x-ray radiography, while the adjacent slices were polished and

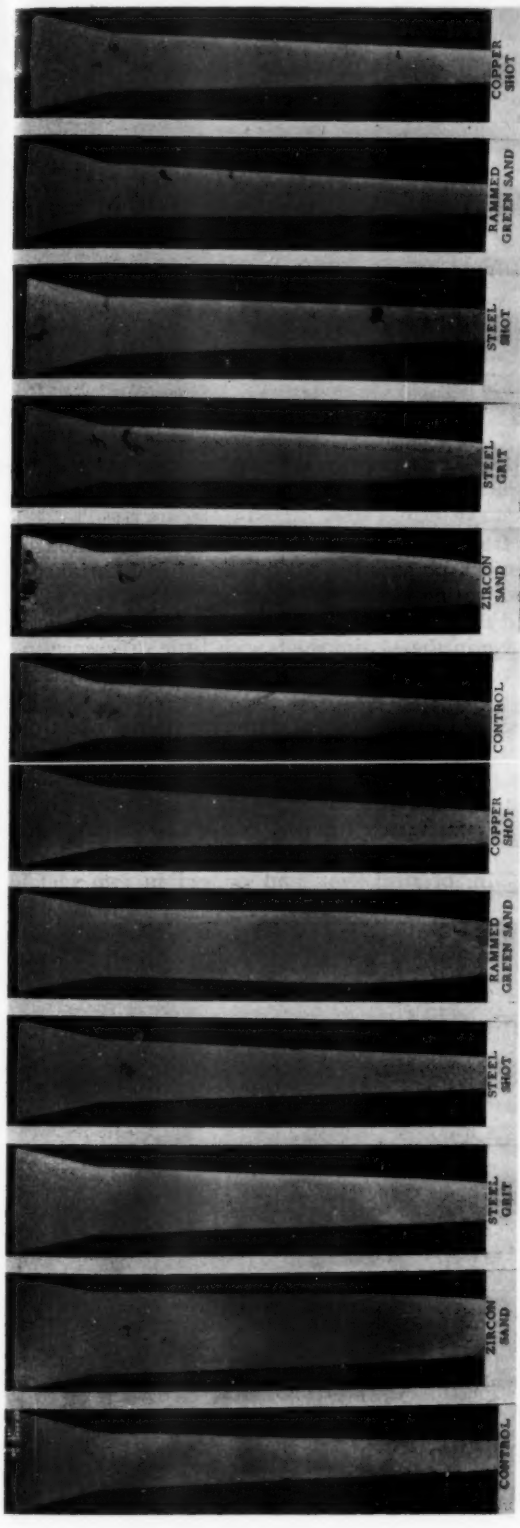


Fig. 24—Exographs of thin slices—effect of varying back-up material on the soundness of shell cast gray iron test plates.

Fig. 25—Exographs of thin slices—effect of varying back-up materials on the soundness of shell-cast class "B" steel test plates.

etched for macro-examination on the bronze and aluminum slices.

An inspection of the macro-etched aluminum slices showed no marked differences in degree of chill between the sand cast and shell cast wedge specimens. Exographs of thin slices indicated some slight porosity on all specimens, with little difference in internal unsoundness.

As noted previously, the bronze sand-cast macrostructure displayed a small, equiaxed grain pattern with evidences of skin chill, while all shell-cast specimens showed large equiaxed grains, without skin chill. The lighter backing materials allowed some swelling of the mold, yielding oversized specimens. This would indicate that on heavier castings requiring close dimensional tolerances the use of a common foundry sand back-up should be explored carefully before being utilized in production. Thin-slice exographs further illustrated the skin chill condition in the green sand control and confirmed the greater degree of soundness in the shot-backed castings.

The gray iron shell-cast slices shown in Fig. 24 displayed no significant differences on x-ray examination insofar as gas porosity was concerned. Slight shrinkage was present under the sinkhead on the zircon and steel shot backed specimens. As in the gun metal bronze series, the zircon and green sand backed specimens showed swelling indicative of mold deflection. The green sand control exhibited some surface roughness due to the gating technique employed.

A reversal in the general soundness trend of the three alloys was apparent from the examination of the class "B" steel thin-slice exographs in this series shown in Fig. 25. The control showed less unsoundness due to gas than any of the shell-cast specimens, although some expected shrinkage due to gating design was present in all. Little difference was noted in the shell-cast series. Modification of the basic investment which was utilized in production with this alloy eliminated this tendency and produced radiographically sound castings.

Effect of Shell Thickness. To investigate the effect of shell thickness, wedge molds were prepared in thicknesses of $\frac{1}{16}$, $\frac{1}{8}$, $\frac{1}{4}$, and $\frac{5}{16}$-in. Two molds of each thickness were clamped and bedded in medium steel shot, with $2\frac{1}{2}$ in. of backing. These were cast off in a 5 per cent silicon-aluminum alloy at 1250 F, in gun metal bronze at 2050 F, gray iron at 2750 F, and Navy class "B" steel at 2950 F. Two green sand control plates were cast with each series. As in the back-up test series, $\frac{1}{8}$-in. thin slices were taken from the wedges, longitudinally, at 2 in. from the side opposite the gate for x-ray inspection, while the adjacent slices were macroetched in the case of gun metal bronze and aluminum alloy specimens.

No appreciable difference in soundness between castings of varying shell mold thickness were found on examination if the aluminum macros and exographs, although the mean grain size of the sand-cast control was slightly smaller. Exographs of the bronze specimens, Fig. 26, did not indicate any appreciable sensitivity to shell thickness from the standpoint of internal unsoundness. The macros as shown in Fig. 27 showed the skin chill condition on the

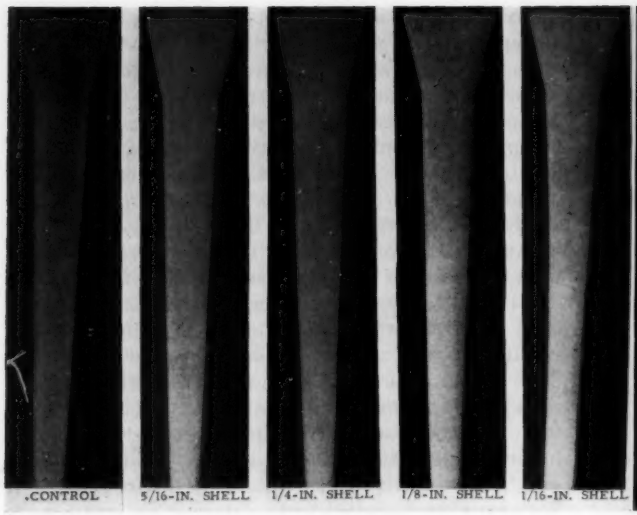


Fig. 26—Exographs of thin slices—effect of shell thickness on soundness of shell-cast "G" bronze test plates.

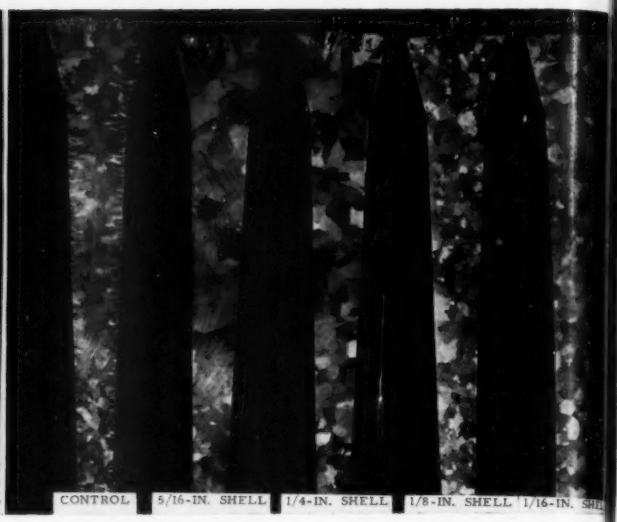


Fig. 27—Effect of varying mold thickness on the macrostructure of shell-cast "G" bronze test plates.

green sand control, and the usual lack of skin chill in the shell mold castings. A definite trend toward larger grain size as the thickness of the shell is increased can be noted. This is indicative again of the insulating effect of shell molds.

The shell-cast gray iron slices exhibited light shrinkage at the sinkhead wedge junction, with a slightly heavier shrinkage condition on the $\frac{1}{8}$ in. and $\frac{1}{16}$ in. shell slices than on the two heavier shell specimens. The controls showed least shrinkage, possibly due to a slight gas porosity condition, but had rougher surface.

Over-all x-ray radiography on the steel wedge plates demonstrated that the green sand controls were inferior from the standpoint of surface finish, although all castings possessed a certain amount of shrinkage due to gating design. Thin slice x-rays, Fig. 28, showed the shrinkage in the casting to be slightly less extensive in the $\frac{1}{16}$-in. shell, with little difference in gas porosity between any of the shells and green sand control.

Effect of Curing Time. To investigate the effect of this variable on macrostructure and internal unsoundness, a number of wedge plate molds were made following standard procedures, except that curing time at 600 F was varied from $\frac{1}{2}$ min to $2\frac{1}{2}$ min. Two molds in each cure time group were clamped, bedded in $2\frac{1}{2}$ in. of steel shot, and poured in gun metal

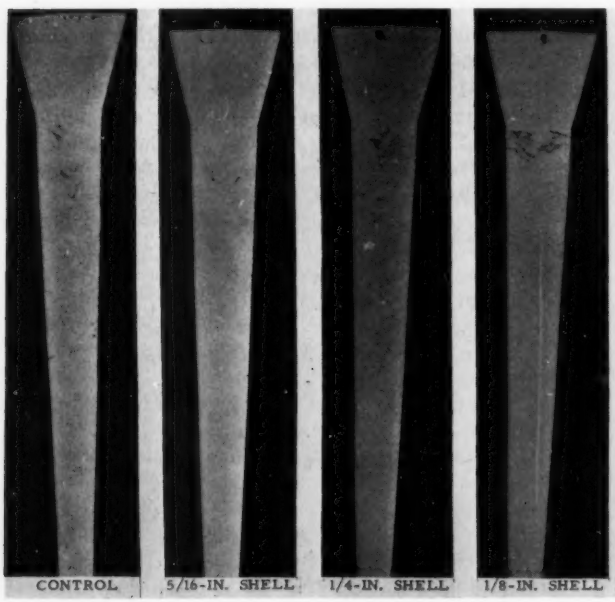


Fig. 28—Exographs of thin slices—effect of shell thickness on soundness of shell-cast class "B" steel test plates.

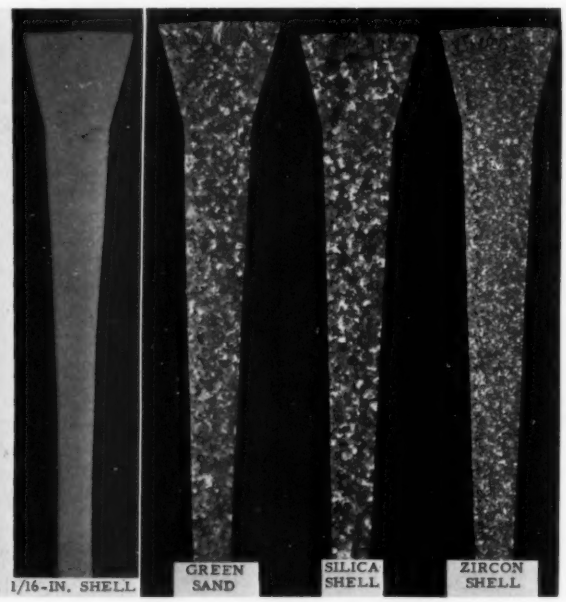
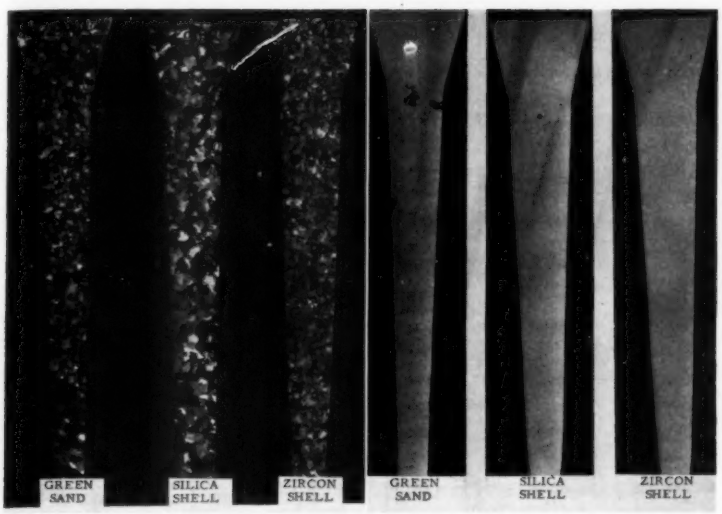


Fig. 29 (right)—Effect of varying base sand on the macrostructure of shell-cast No. 43 aluminum alloy test plates.



(Left)

Fig. 30—Effect of varying base sand on the macrostructure of shell-cast "G" bronze test plates.

(Right)

Fig. 31—Exographs of thin slices—effect of varying base sand on the soundness of gray iron test plates.

bronze at 2050 F, together with two green sand control plates. The castings were sectioned in the usual manner, a 1/8-in. slice being taken 2 in. from the end opposite the gate, with the adjoining slice etched for macroexamination. No significant differences in the degree of internal unsoundness were noted in either the macrostructure or exographs.

Effect of Base Sand. To determine the effect of a substitute sand grain such as zircon sand (a considerably denser material of higher heat conductivity), a number of 1/8-in. shell wedge molds were prepared. One set contained a normal silica sand mix; the other a fine (about 140 AFS) zircon sand, in the proper proportion (4.5 per cent by weight) to maintain the normal sand to resin volume relationship. Eight molds of each type were clamped and bedded in steel shot, 2½ in. all around, together with eight green sand control plate molds. Two of each were cast in a 5 per cent silicon-aluminum alloy at 1250 F, gun metal bronze at 2050 F, gray iron at 2740 F, and class "B" steel at 2950 F. Macro and thin slice x-ray specimens were taken in the usual way.

The photomacrographs of the aluminum slices, Fig. 29, showed a finer grain size in the zircon-cast specimen, indicative of a more rapid rate of solidification. Positive prints of exographs of thin slices showed equivalent microporosity in green-sand and silica-shell cast slices, with somewhat less microporosity in the zircon specimen. Over-all x-ray radiography on the aluminum wedge plates confirmed this condition.

In the bronze plates, a finer grain size was evidenced by the specimens cast in a zircon shell as compared with those cast in green sand or silica shells (Fig. 30).

Exographs of the gray iron specimens showed an area of shrinkage at the sinkhead-plate junction of the sand cast plate, with less shrinkage in the silica shell specimen and practically none in the zircon shell slices (Fig. 31). The exographs of the class "B" steel slices cast in silica and zircon shell molds did not exhibit any appreciable differences.

Comparison of Mechanical Properties. Duplicate heats were run to determine the optimum pouring temperature for shell-cast 5 per cent silicon-aluminum alloys and to secure comparisons of elongations and ultimate strengths between shell-cast and green-sand bars at various pouring temperatures. For these heats, the as-cast 0.505-in. double tensile bar design was used, as shown in Fig. 32, the plastic bonded shells being molded with a 10 x 12 in. plate and pattern, and the green sand counterparts being made in No. O naturally bonded Albany sand with a wood pattern of similar design and dimensions. In each heat two shell molds and two green sand molded tensile coupons were poured off at each of the following pouring temperatures—1350, 1300, 1250 and 1200 F. In addition, two shell-mold wedge plates and two green sand control plates were poured off at each temperature. All shell molds were bedded with a 2 in. thickness of steel shot.

The graphed results are shown in Fig. 33. It was observed that at the higher and optimum pouring temperatures, the over-all properties were slightly su-

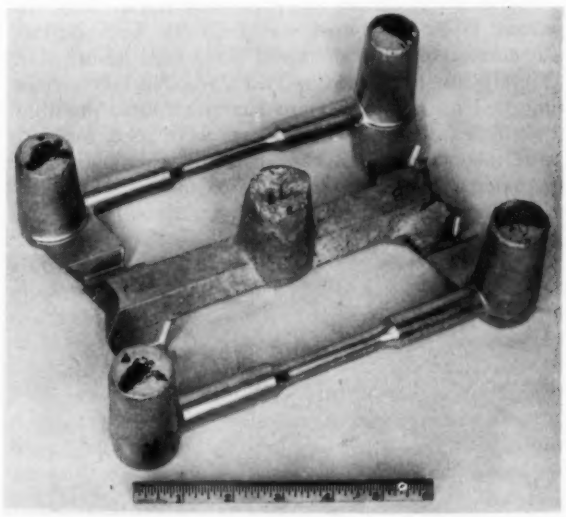


Fig. 32—As-cast aluminum alloy test bar (43 alloy), shell molded.

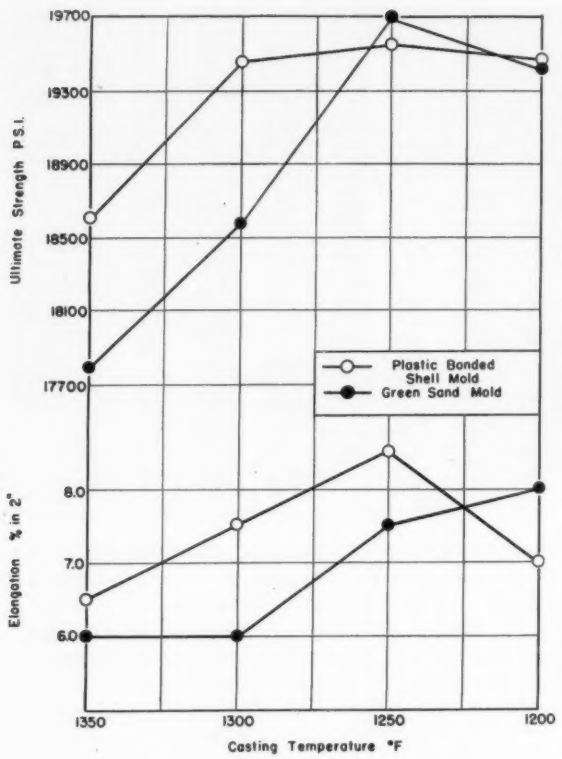


Fig. 33 (above)—Mechanical properties vs. pour temperature No. 43 aluminum alloy shell cast and green sand cast tensile bars.

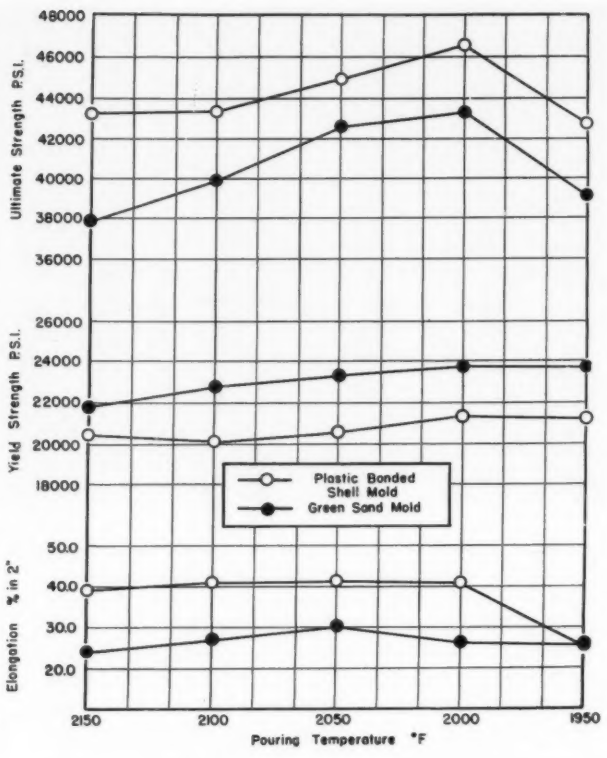


Fig. 34 (right)—Mechanical properties vs. pour temperature; Navy "M" bronze, as-cast gage shell and green sand cast tensile bars.

perior for the shell-cast specimens. Exographs of thin slices taken from the wedge specimens indicated a tendency for a greater degree of internal soundness in the shell-cast specimens. It is believed that the superior properties of shell cast bars at the temperatures noted are due to the improved cleanliness of the tensile bars.

For similarly testing the tin-bronze tensile bars, the pattern equipment used was a double bottom-gated test coupon pattern mounted on a 14 x 18-in. plate (Fig. 6) for shell molding and a wooden counterpart mounted on a follow-board for green sand molding. Two types of tensile bar inserts were used for each pattern, one a pair of bars of standard oversize gage diameter requiring machining to finish to 0.505-in. gage diameter, and the second a pair calculated to cast to approximately 0.505-in. gage diameter. The purpose of this latter set was to secure additional data relative to the skin chill effect, or lack of skin chill, on comparative mechanical properties of shell-cast bars.

Four Navy "M" or valve bronze heats were run in this series, consisting of two duplicate heats for each type of tensile bar insert used. In each heat shell molds bedded in steel shot and five green sand molded counterparts were cast, one of each for the following temperatures: 2150, 2100 2050, 2000 and 1950 F.

The graphed results (Fig. 34) for the as-cast 0.505-in. tension bar inserts showed higher ultimates and

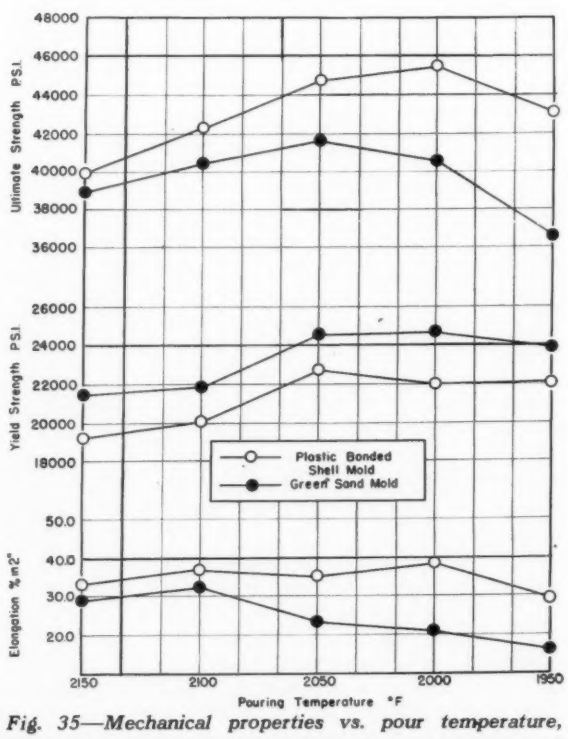


Fig. 35—Mechanical properties vs. pour temperature, Navy "M" bronze, machined gage shell and green sand cast tensile bars.

elongation at virtually all pouring temperatures for the shell-cast specimens, but lower yield strengths. On the machined bar series (Fig. 35) the averaged values followed the same pattern, with ultimates and elongations and lower yield strengths throughout the range.

The green sand test specimens generally exhibited a greater amount of dross and gas porosity than the shell-cast specimens. The lower yield strengths of the shell cast test bars as compared with green sand bars may be due to the slower cooling rate and lack of skin chill exhibited by the shell molded test bars.

In summary, development data and production experience indicates that shell molded castings in the ferrous and non-ferrous alloys can develop mechanical properties and internal soundness, at least equal to, if not superior to green sand castings. The trend toward greater internal soundness and high mechanical properties in the shell molded castings can be attributed to cleaner metal with less gas inclusions and dross. This is probably a function of a consistently clean mold with good permeability and venting capacity.

Applications and Economics

The shell molding process is currently being utilized on a variety of parts predominantly in the ferrous alloys (gray, malleable and spheroidal iron). The applications which may be suitable are closely related to the economics of the process as compared with conventional practice. From an over-all point of view every casting design is not suited for the shell-molding process. Tooling costs are still relatively high, and hence the necessary volume is a basic requirement. Second, the design of the casting must lend itself to a suitable parting and gating arrangement. Third, and possibly the most important factor, will be the degree of savings that can be effected in the cleaning room and machine shop, since the greatest economies will generally be effected in these departments. No general rule for a cost comparison with sand castings can be derived. Each casting must be examined on its own merits. Other considerations, however, which may be factors in considering the utilization of the shell process as a production technique involves the conservation of material normally removed during machining operations, or in parts produced in an alloy that is difficult to machine, or in a significant decrease in foundry rejections. In the first case, if a large amount of material is removed in machining, or in the second case if machining costs are high, it may be advisable to consider shell molding as on alternative and possibly more economical method of manufacture.

At the present stage of development shell molding is particularly suited to the production of castings in aluminum alloys, certain bronzes and brasses, high conductivity copper, malleable, gray and ductile iron and stainless steels. In the production of medium carbon steels careful control of investment formulations and processing and proper conditioning of the molten metal are necessary, if pinholing difficulties are to be avoided. In the case of small tin bronze or aluminum castings, as indicated previously, which have been "treed" as in the investment casting process and poured vertically, some difficulty has been experienced in penetration and "burning in" on the castings in the lower part of the mold.

The following applications for shell molded castings being produced on a production basis have been reported:[26]

a) Automotive castings in gray iron such as governor bodies and bushings for automatic drives, exhaust valves in alloy steel, camshafts, rocker arms and crankshafts.

b) Agricultural castings such as rocker arm brackets, pinion bearing cages, crankshaft pulleys, exhaust pipe flanges and brake drums.

c) Valve bodies, bonnets and pipe fittings such as tees and ells in stainless steel and bronze.

d) Ordnance parts in magnesium, aluminum, ductile and malleable iron.

e) High conductivity copper castings.

In addition to the above many miscellaneous castings are currently being evaluated for aircraft or general machinery utilization.

At the Shipyard Foundry with which the author is associated many different applications have been explored in the laboratory, and a production unit on a jobbing basis has been in operation for several years. The operation of this shell-molding unit, previously illustrated, which has been operated on a very simple, manual basis has established the fact that considerable economies can be derived by the utilization of this process on a jobbing basis provided the proper casting application is selected. The determination of the feasibility of the process on a particular casting design can be accomplished quite economically and quickly. A wooden dump box or a hand-charging device, an oven which is capable of attaining a minimum temperature of 500 F and an aluminum matchplate or a steel plate with a rough mounted pattern is all that is required. On occasion, to explore certain applications, rough sand castings in bronze have been hand-tooled and mounted. This is illustrated in Fig. 36. On this impeller casting, which was subsequently cast in several alloys to determine the practicability of manufacturing similar parts, a slight backdraft condition existed on several vanes which were $\frac{1}{10}$-in. thick at the tip. However, suitable shells and castings in aluminum alloys and gray iron were obtained by this technique.

The first production shell-molding job which was assumed by the New York Naval Shipyard centered around several fire control instrument components in

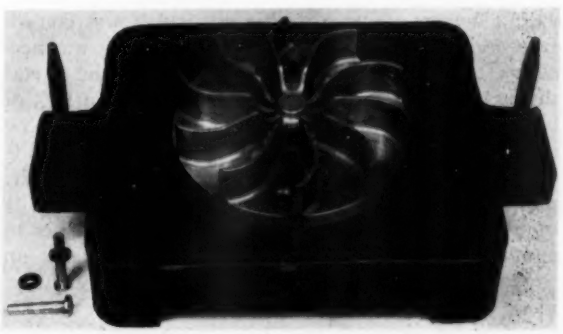


Fig. 36—10 x 12 in. impeller pattern plate.

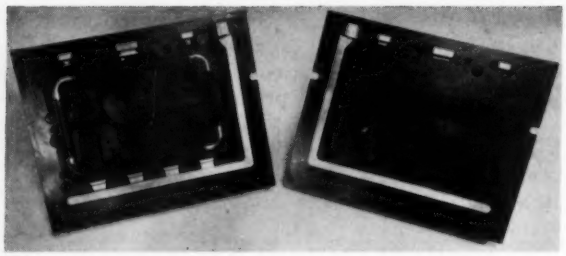


Fig. 37—14 x 18 in. pattern plates for mounting panel.

a 5 per cent silicon-aluminum alloy. The original design specified sheet metal fabrication, but long-term delivery quotations necessitated a study of shell molding possibilities. Shell molding as a manufacturing technique was finally decided upon due to the expected savings in the machining of gasket grooves of certain castings, superior surface finishes which were expected to reduce surface preparation for final painting, and the limited machine tool facilities available at that time. Quantities for each item ranged from 1300 to 4000 pieces.

The larger and more critical castings involved were: a) a cover casting; b) a mounting panel; and c) a motor mounting bracket. The plate equipment for the cover and mounting panel castings, was of alloy cast iron, while the patterns were of machined medium carbon steel, with aluminum runners in some instances. The plate and pattern for the motor bracket casting were of aluminum alloy throughout. All plates incorporated button-head, spring-return stripping pins for mold ejection, and small tapered buttons for bolting mold halves together.

The pattern equipment for the mounting panel casting is illustrated in Fig. 37. The pattern is bottom gated with choked ingates, while three small "risers" hold the top rim section. A dry sand pouring cup was set in a print at the top of the downgate. Metal shrinkage was provided on the pattern as in normal sand practice, i.e., $\frac{3}{16}$ in. per ft. The maximum casting tolerance specified was $\pm$.010 in. on a 13.75-in. length and 8.875-in. width of the casting, and $\pm$.005-in. on the depth and width of the gasket groove. The average tolerance on all other dimensions, including bore locations, was $\pm$.010-in. The pouring weight was 6.5 lb.

The cover casting, shown previously in Fig. 13, presented an additional problem in that the only available pattern plate size 14 x 18 in. could not accommodate a bottom-gating arrangement which was considered desirable at that time. Consequently, it was necessary to gate on the flat, introducing the metal through three (3) choked ingates. Here again lack of head necessitated the use of a pouring cup. Casting tolerances on this 12.500 in. long and 9.876 in. wide cover were of approximately the same order as the mounting panel. The poured weight was 6.6 lb. The motor bracket pattern, Fig. 7, was gang-gated, six to a mold. Difficulties in securing smooth surfaces on the lower castings on the tree with this gating arrangement necessitated a gating change. A print was located in the center of the runner to receive a dry sand pouring cup, and the mold was poured flat, without shot backing. The poured weight was 6 lb. On this casting, tolerances varied from $\pm$0.005 to $\pm$0.010 in., with no draft permitted on the motor mounting surface.

A portable muller mixer, shown in Fig. 38, was utilized for mixing the resin-sand mixture—150 AFS silica sand of five-screen distribution with 7 per cent resin was generally used. The dwell and cure cycles were as follows:

a) Covers

Dwell temperature range: 300 - 450 F (a resin with setting latitude used)
Dwell time range: 10-15 sec, depending on plate temperature
Cure temperature: 600 F
Cure time: 50 to 60 sec, depending on plate temperature at dwell
Average Mold Thickness: $\frac{3}{16}$ - $\frac{1}{4}$ in.

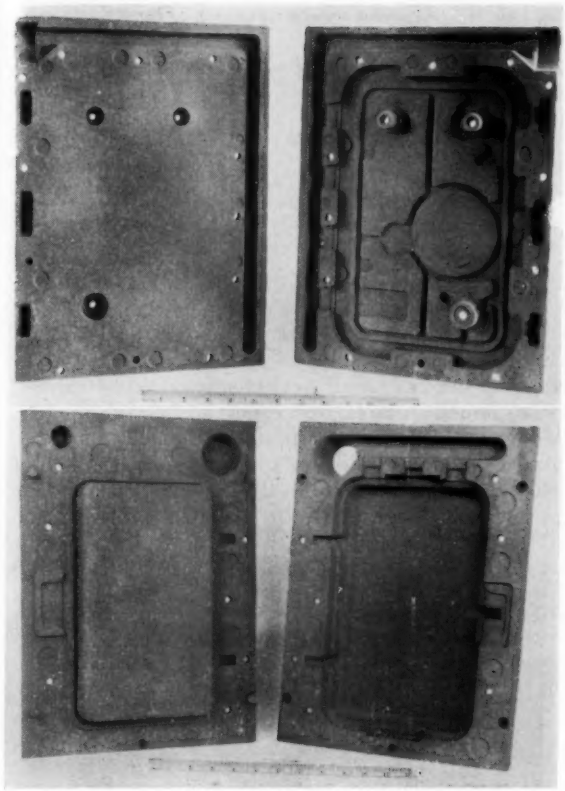
b) Motor Bracket Mounting Panel

Dwell temperature range: 370-450 F
Dwell time range: 12-15 sec, depending on temperature
Cure temperature: 600 F
Cure time: 60 sec
Average Mold Thickness: $\frac{3}{16}$ - $\frac{1}{4}$ in.

After the shell mold was stripped free, it was placed down on leveling boards, and weighted with bags of shot to minimize distortion or handling deformation while hot. An elapsed time of 30 to 60 sec was generally sufficient to eliminate any thermoplastic tendencies in the shell mold. Following stripping, the pattern plate was run through as many cycles as the heat head permitted, which generally averaged three to five molds, depending on the resins employed. When one pattern half had cooled past the usable range, it was placed in the oven for reheating, and the other pattern half used until that too required reheating. Stripped and cooled shells were stored vertically in portable racks until ready for assembly. Typical mounting panel and cover molds are shown in Figs. 39 and 40, respectively. Due to the close tol-



Fig. 38—Portable muller for blending resin-sand mixtures.



Figs. 39, 40 (top to bottom). Fig. 39—Mounting panel mold, cope and drag. Fig. 40—Ordnance cover mold—cope and drag.

erances required, molds were assembled with machine screws and speed units, Fig. 14, driven with a torque controlled electric nut runner. This manner of assembly, while holding dimensional tolerances and reproducibility to the most favorable degree, is time consuming and expensive. Before assembly, molds were given a routine inspection for quality. Few defects were encountered. However, the defective molds encountered usually were rejected due to thin spots, chipping or porous areas on vertical surfaces. The use of a quick setting core paste, applied by bulb in a thin bead around the mold cavities reduced assembly time appreciably, and promoted a strong joint. However, the application required extreme care; the paste, if placed any closer to the mold cavities than ½-in. resulted in casting blows. In addition, the bond line thickness generally influenced the casting dimensions.

For bedding the mounting panels were placed vertically in the double compartment bedding boxes, three to each compartment, and a No. 660 steel shot valved into, around, and over the molds, with a hose arrangement leading from the loading tank. Dry sand pouring cups were dropped into the downgate print to provide a suitable head. The cover molds were bedded in on the flat in a similar manner with a dry sand pouring cup attached, save that only one mold per box could be bedded. The motor brackets were poured off without shot backing since the light sections made additional mold support unnecessary.

Mounting panels, covers and motor brackets were poured from 1350 F down, 3 to a ladle. The pouring was done as close to the cup as possible, to minimize drop, and it was found necessary to control pouring temperatures very closely in order to minimize shrinkage. The molds collapsed readily on breakout, displaying carbonization in the mold section area immediately at and bordering the casting cavity, with the remaining portions apparently unaffected. In all instances, castings broke out cleanly, with no adhering sand. No sandblasting or other surface cleaning was required at any time. Finning was practically non-existent.

Dimensional Tolerances Satisfactory

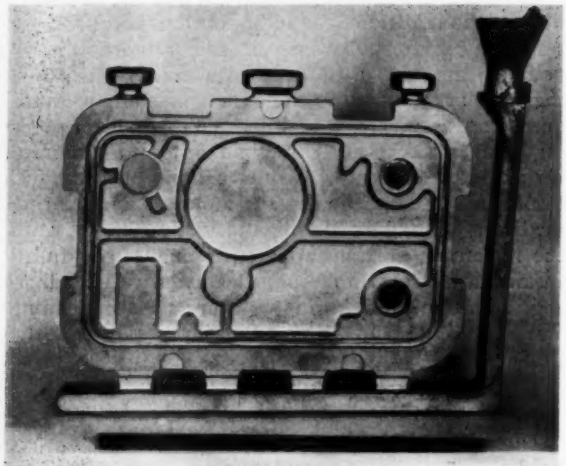
Dimensional tolerances in all critical areas were held satisfactorily. Control castings from each heat were radiographed and found to be free from objectionable internal shrinkage and gas porosity. Rejects in the mounting panel casting were fairly low—24 plate castings were rejected of a total of 1005 manufactured due to casting defects or dimensional inaccuracies. The cover castings were susceptible to shrinkage at the hinges and lifting lug. This defect was reduced in severity by close control of pouring temperature. This particular casting was sensitive to the type of resin employed; stiff, high tensile molds causing mold failure on pour-off. Typical panel, cover and motor bracket castings are illustrated in Figs. 41, 13, and 42.

Another class of castings currently in production are ductile iron clamps used in securing staging around ship hulls. All patterns were made in an aluminum alloy, as a weight saving feature. The master patterns and castings for one of these clamp designs are shown in Figs. 43 and 44. It will be noted that the pattern is of the reversible mold type, and that four castings of two designs are cast in each mold. The gating and heading was typical for ductile cast iron, incorporating heavily choked runners and ingates, together with skim bobs, and substantial risers, notched for easy removal by flogging. Molded mixtures and cycles were similar to those employed on the aluminum castings described previously. Molds were generally pasted together and bedded vertically, several to each box, using vibrators to secure maximum compression of the shot during the bedding operation. A typical mold half is shown in Fig. 45. The castings produced were exceptionally smooth and required no further machining operations.

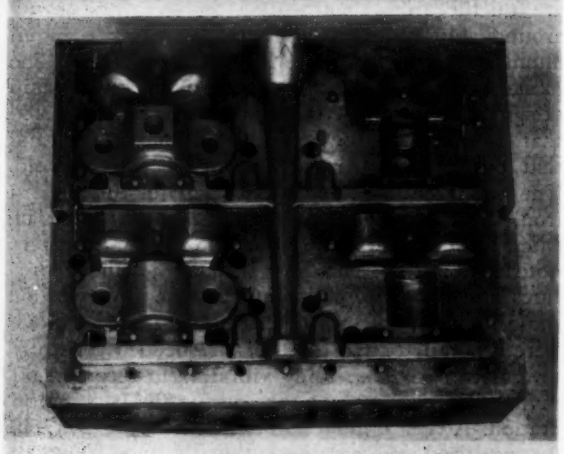
An 0.45 C Steel Shell Molded

An example of an application of shell molding in a 0.45 per cent carbon steel where improved yield and the elimination of certain machining operations justified the use of the method even though very close tolerances and high surface finish were not particularly important. The master pattern, mold and core were shown previously in Figs. 10, 11, and 12.

The investment formula utilized on this job was varied somewhat from that previously described, in order to combat the increased penetration and washing characteristics of medium carbon steel. The form-







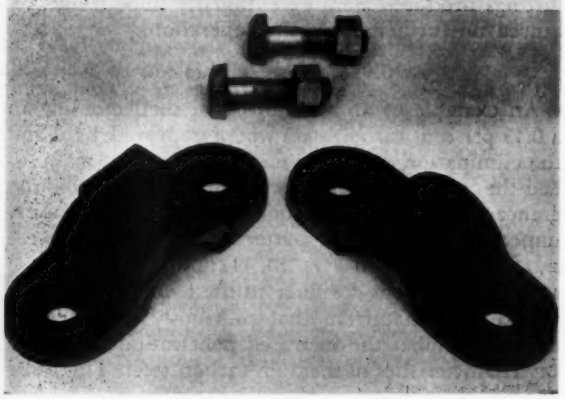


Fig. 41, 42, 43, 44 (top to bottom). Fig. 41—Mounting panel, as-cast. Fig. 42—Ordnance motor bracket castings—as-cast. Fig. 43—Staging clamp pattern plate. Fig. 44—Staging clamp castings—as-cast.

ulation generally employed for medium carbon steels, consisted of:

AFS 230 sand (relatively high clay)	82.0 per cent
Silica or zircon flour, 375 mesh	7.5 per cent
Phenol-formaldehyde resin	10.5 per cent

It was found necessary to lengthen the dwell period for investments of this type to 25-30 sec at 350-450 F plate temperature to secure ¼-in. thick molds. The curing time was not affected and averaged 60 sec in the strip heater. Molds were characterized by velvety, close-grained surfaces, firm edges, and good strengths. Molds were bedded horizontally in steel shot, vibrated, and poured off in the temperature range of 2900 - 3000 F. Very little sand adherance to these castings was experienced in comparison with sand

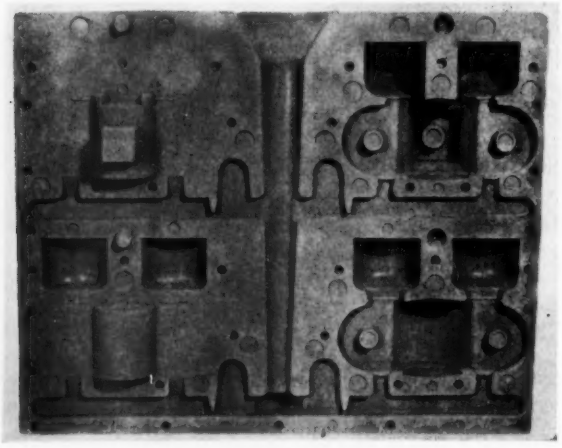


Fig. 45—Staging clamp mold half.

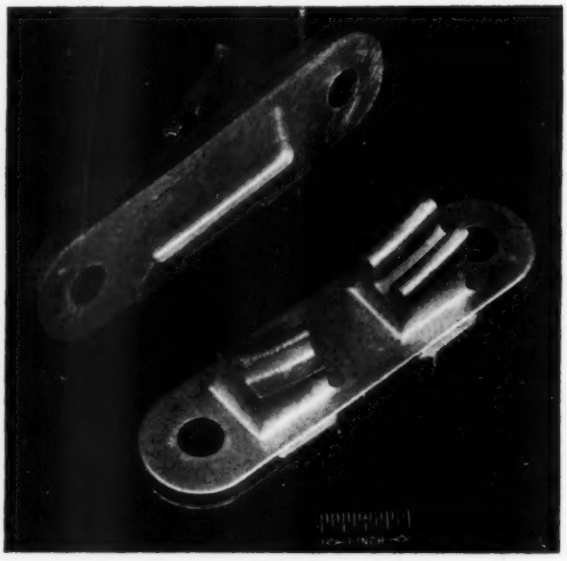


Fig. 46—Aluminum bomb chock castings—as-cast.

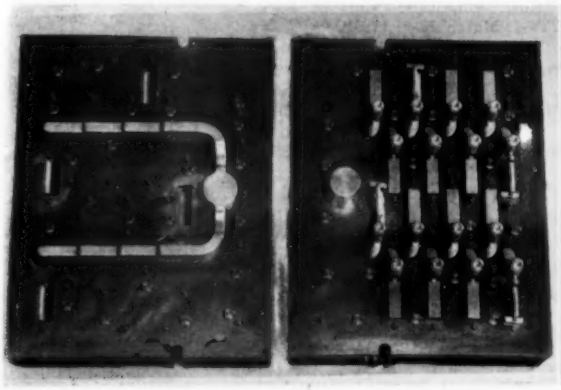


Fig. 47 (above)—Turbine blade pattern plates.

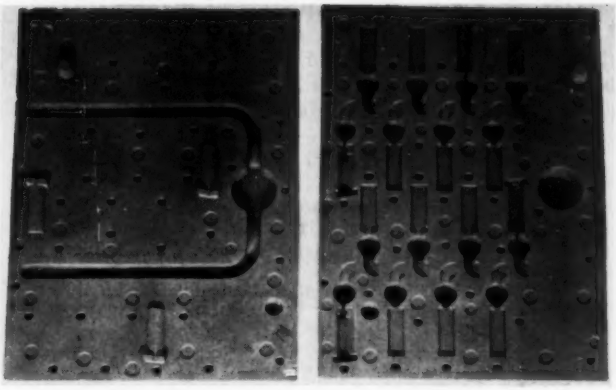


Fig. 48—Turbine blade mold—cope and drag halves.

castings, and the resultant surface finishes were superior to sand castings, although not approaching the surfaces obtained in the non-ferrous or gray iron alloys.

Another shell molding casting application in an aluminum alloy is illustrated in Fig. 46. In this bomb chock casting considerable machining time was eliminated.

Still another application, steam turbine blades, currently under development for manufacture by the shell molding process, is illustrated in Figs. 47, 48, and 49. These blades are cast in a 410 stainless steel alloy and poured horizontally. The gating arrangement shown yielded x-ray sound blades. On the basis of the development work accomplished to date it is believed practical and economical to cast 410 stainless turbine blades of the size indicated and to finish to plan dimensions by an automatic grinding and machining operation. A light belt sanding as practiced in the investment casting field is sufficient to reduce the surface finish of these blades to a RMS (root mean square) value of approximately 25.

In evaluating the applicability of any particular casting design to the shell molding process, it has been found profitable to discuss the casting tolerances and design with the design agency concerned. Frequently, tolerances are applied to a dimension of a part simply because in ordinary machining operations it is easy to attain. However, in many cases it is possible to open up tolerances on a piece if machining is to be completely eliminated. This has been accomplished, in many cases, without affecting the serviceability of the casting. In addition, design changes may sometimes make the difference between an economical productive casting method in shell and one which will give considerable difficulty in casting sound.

Shell molds are also being utilized to some extent as a core drier. Shell molds coated with a high-temperature varnish will withstand numerous runs through a tower oven and provide a relatively inexpensive core drier.

In any cost analysis of the shell molding operation, the cost of the resin employed will frequently represent 50 to 75 per cent of the direct labor and raw material costs of manufacturing a mold or core, depending upon the degree of mechanization employed. Hence, any method or process which will reduce the conventional amounts of resin required to produce a shell mold or core of sufficient strength will certainly be reflected in marked decreased costs, and may enlarge the area in which the process can be utilized.

There are many hidden advantages to the shell molding process which many operators have considered in deciding upon the applicability of any casting design to the shell molding process. Among them are a saving in foundry scrap due to dirt and trapped sand. Usually the defective molds made by this process are eliminated prior to pouring. In addition, overhead, based on space required for a certain degree of productivity is less than in conventional molding. The smooth surfaces obtained in shell molded castings permit easy identification of defects. It has been the experience of the author that evidence of internal unsoundness of a shell molded casting can be detected by surface examination. However, in conclusion, it must be re-emphasized that this process is not a panacea for all foundry ills. In the final analysis, it probably will only replace a small percentage of conventional sand casting, but if properly applied to the right job it can be a useful and economic tool.

Acknowledgment

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Fig. 49—Type 410 stainless steel turbine blade castings —as-cast.

tain H. T. Koonce, USN, Director of the Material Laboratory, for his interest and encouragement in this project; to Noah A. Kahn, Head Metallurgist, for his review and criticism, and to S. B. Donner for assistance rendered in obtaining the data.

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EFFICIENT METHODS OF PRODUCTION OF MACHINE TOOL CASTINGS

By

G. W. Nicholls*

Introduction

Quantity production of castings has necessitated the use of all types of mechanical aids in foundries, and while this modernization and mechanization has been carried out successfully in many sections of the foundry industry with improvement of production rates and elimination of unnecessary material handling, it is obvious from results observed in many foundries over the past few years that insufficient time, thought and capital outlay have been expended on the reduction of scrap and defective castings.

As a general rule, no great concern is shown so long as the percentage of scrap castings falls within what any particular foundry considers to be a reasonable limit. It must be admitted, however, that by careful control of all foundry operations it should be possible to reduce appreciably this accepted limit or percentage of scrap and, furthermore, reduce the amount of defective castings which have either to be salvaged or scrapped in the machine shop after an appreciable number of man hours have been expended in machining operations.

Many persons believe, through lack of knowledge, that the quality of castings begins and ends in the foundry department. This, of course, is incorrect. Actually, the process of manufacture of sound castings begins at design, passing through the pattern department, control laboratory, and ending at the casting operation.

All foundrymen should, therefore, take greater interest in production design, which comprises basically the study and re-design of castings, in order to simplify manufacture and ascertain the most rapid method of quality production. It must be borne in mind, however, that the production of satisfactory castings of high quality, with maximum economy in both materials and labor, requires continuous research, development and control to secure the improvement of methods and materials. For this reason alone foundrymen will find it increasingly necessary to follow and study all foundry developments more closely.

All matters relating to the question of production and quality must fall under the responsibilities of management. They must be considered as the main key to the foundry's success in the production of sound castings, and any progress achieved toward eliminating scrap and defective work can usually be measured by the management's attitude and the amount of thought and energy expended by the foundry technical staff when carrying out their control, research and development work.

Research and development work carried out in a foundry implies progress, and its widest objective should be to prepare the way for the production of castings of high quality at a price which will benefit the greatest number of sections of all branches of industry. This research and development work varies considerably. The type which merely devises the cheaper methods of production with the object of increasing profits is not of such high order as that which aims at improving quality without increasing production costs.

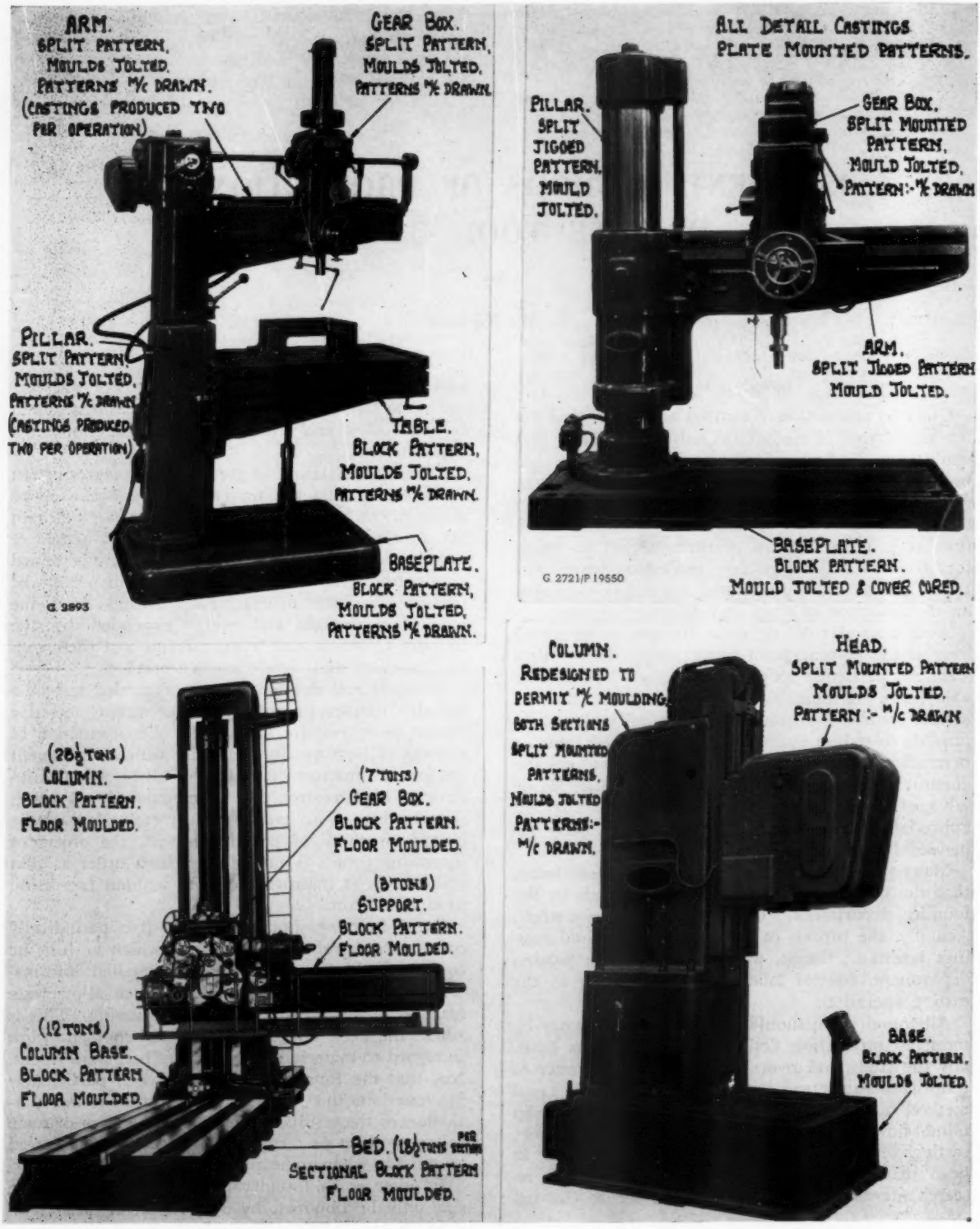
Every foundry shop floor executive periodically comes across problems, concerning which he feels he could do with more definite and detailed information instead of vague generalities which allow wide margins for error, assumption and guessing. This is where the need for research and development work in regard to materials and processes becomes evident. Not that the foundry floor executive is particularly interested in this work as such, but he wants particulars of the results, preferably in the form of some reference sheet or card index, which can be referred to quickly when some problem arises.

In other words, foundrymen require facts and these can only be obtained by observation of what happens under definite conditions; always bearing in mind, however, that in view of the many variables involved in the founding of castings, more than one should not be altered at a time. Otherwise it would be impossible to assess any single influence.

Whatever investigational or development work is

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Figs. 1, 2, 3, 4—Examples of machine tools with descriptions of patterns and molding methods for the cast parts.

proposed, a detailed schedule should be planned in steps and carefully followed to a conclusion, tackling one factor at a time and devoting to it whatever time is necessary for completion before taking up the next problem. Patience is required and short cuts do not pay. All instructions should be definite and specific and put into writing for the benefit of those who are to carry them out, as well as for future reference.

The final report of any investigation should cover:

a) The objective of the investigation.
b) The nature of work and tests carried out.
c) Summary of results and conclusions.
d) Recommendations.

The report eventually should be filed for reference under one of the sub-sections listed in Table 1.

If this procedure is to be successfully carried out the employment of first-class foundry technologists is essential. In addition to being highly trained and possessing imagination, as well as a good scientific

TABLE 1

Design	Molding	Coremaking	Metal
Type of Pattern	Sand Mixtures	Sand Mixtures	Metal Composition
	Production Methods	Production Methods	Physical Properties
Special Features	Venting	Venting	Melting Practice and Temperature Control
	Binding Materials		
	Refractory Dressings	Binding Materials	
	Drying Practice	Refractory Dressings	
		Drying Practice	
	Gating	Storage	
	Risers—Feeders		
	Molding Tackle		

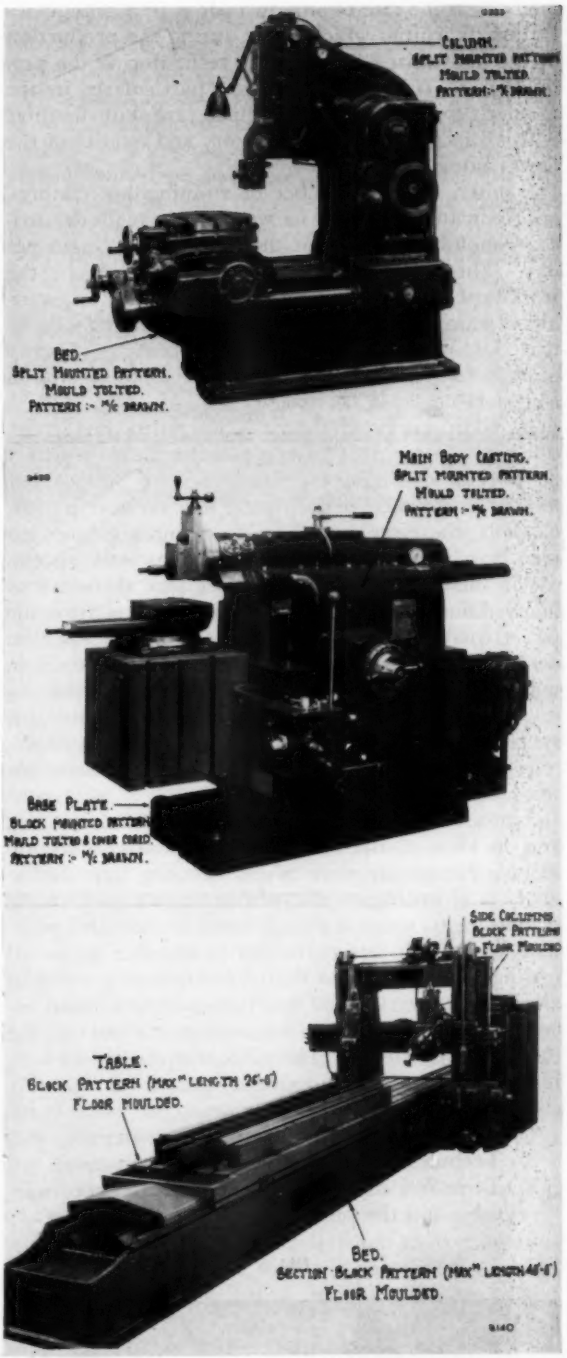
knowledge of all foundry techniques and materials, these technologists should be gifted with the following essential qualifications:

a) A critical attitude toward anything that cannot be proved by facts and sound reasoning.
b) Patience to take meticulous care over small details.
c) Systematic and orderly minds enabling them to distinguish and separate any variables observed and to record the same.

These qualifications are necessary, as the fundamental of good quality products is the application of knowledge gained by a process of constructive reasoning which must be of the highest order and fully exercised for the successful solution of founding problems involved in design, materials and method.

The object of this paper (much of which has previously been published in the I.B.F. Proceedings[1,2,3,4]) is to outline to the American Foundrymen's Society the manufacturing methods and control system, based upon the principles laid down in the introduction, which are operating in the production of many types of machine tool castings, such as radial drill, planing, boring machines, etc. (Figs. 1 to 7), the manufacture being carried out in accordance with the production flow diagram illustrated in Diagram No. 1.

The manufacture of such castings, which vary in weight from ounces to 30 tons per piece, and which are produced by hand-molding, sand slinging and machine molding methods, in order to be both economical in price and of good quality, must be based upon a thorough knowledge of all the various materials and processes involved and the many variables



Figs. 5, 6, 7—Types of machine tools and molding methods used for the cast parts.

attached to each. An individual knowledge of each of the multitude of processing factors which are involved in the manufacture of the varying types of machine tool castings goes far toward achieving maximum economy and speed of production of quality castings, which can only be successfully accomplished by first class control.

In some cases, particularly in respect to heavy casttings, quite a number are made by floor-molding

methods at the rate of only two per year. Consequently, any difficulties which occur during the production of the first casting are likely to recur during the production of subsequent castings, particularly if the molder, coremaker and foremen are not familiar with all facts regarding the history and quality of the first casting made.

Further, a large number of running-line castings, approximately one ton in weight, are made by machine-molding methods at the rate of six to eight per day. These castings are usually weathered in the stock-yard for periods up to 6 months, and any variables which creep into the production methods or materials may result in a large percentage of scrap or defective castings being brought to light after a considerable lapse of time.

In addition, the foundry is operating on an individual piece-work system, producing approximately 30 tons of castings per employee, or 110 tons per molder/coremaker, per annum. The variable human element, therefore, comes in to a much greater extent than in a foundry operating a day-work system.

For these reasons, plus the fact that there are so many different factors which collectively control the price, quality and soundness of a casting, such as design, patternmaking, mold and core construction, materials and methods of preparation, gating and rising, metallurgical control and sand control, that this system of studying the quality of castings in relationship to foundry production methods and materials was commenced some 10 years ago.

At the commencement of the system of control, and in view of the fact that many foundry laboratories were simply show pieces carrying out routine analysis according to the whims and fancies of the management without direct contact with the problems constantly arising in the production of sound castings, it was decided that the thorough control of all defective work could not be achieved without incorporating the laboratory as an integral part of the full production cycle. The laboratory was, therefore, reorganized and re-equipped to carry out scientific control over the following:

1) Maintenance of quality of raw materials.
2) Technical supervision of melting processes.
3) Technical supervision over sand and core mixtures and the preparation of mixtures.

Fig. 8



4) Technical control of all foundry drying processes of molds and cores.
5) Full liason between foundry foremen on all matters relating to gating and feeding of castings.
6) Control of development work in connection with improvements in products and raw materials.

At the same period, the foundry was reorganized into sections and a foreman placed in charge of each, as illustrated in Diagram No. 2. The following notes give a brief description of the production methods applied in each section.

A) Section 1 is devoted to the production of castings weighing from 3 to 30 tons each, which are manufactured by floor-molding methods. Typical castings include boring machine columns, beds, workplates and gear boxes; also planing machine beds and tables, and miscellaneous large special components.

Fig. 9



The maximum size of molding pit in this department is 44 ft long and all pits are shaped out of solid rock, lined with cast iron plates and traversed underneath by heavy bearer bars complete with wrought iron loops.

In view of the tremendous amount of sand to be handled in the production of these heavy castings, use is made wherever possible of sand grabs for preparing the molding pits, and pneumatic rammers, sand-throwing machines and portable sandslingers for speeding up the ramming of the molding sand.

The majority of the patterns utilized in this section are in the form of solid blocks or sectional blocks. Figure 8 shows the bedding-in of part of the pattern for a large boring machine column weighing 30 tons; Fig 9 the mold for this component, finally cored, and Fig. 10 a 40 ft long planing machine bed casting, molded from a sectional block pattern.

B) Section 2 is devoted to the production of miscellaneous machine tool castings up to 3 tons in weight, utilizing similar aids to production as described in Section 1. The department has also installed a complete molding unit for the production of medium-weight castings up to one ton, such as

light type radial drill, shaping and slotting machine, and lathe castings.

The molding boxes utilized on this machine have been standardized to two-box sizes which combine to any depth. The location of boxes to this machine, which is a 78 × 44-in. pneumatic jolt rollover design, is by means of lined pin holes at one end and slot holes at the other, thus avoiding pin jamming due to temperature differences.

The patterns for this production unit are all accurately located on metal plates with the runner system attached. Figure 11 shows a typical pattern plate arranged for the production of two castings per operation, while Fig. 12 shows the machine with the pattern withdrawn from the mold.

C) This section of the foundry is equipped for the rapid production of radial drilling machine castings such as pillars, arms, baseplates and box worktables. The molds are produced by jolting jig-set patterns in the molding box, on a 10-ton straight jolting machine. The sand is fed into the molding box by overhead crane and sand grab as illustrated in Fig. 13. This method of sand feed is, however, being superseded by a quicker and more up-to-date method as illustrated in Fig. 14.

All pillar molds are cast vertically, the metal being allowed to flow from the runner bush through pencil runners. The metal thus drops down the inside of the mold cavity; the distance sometimes is as great as 18 ft. Cores for these pillar molds are made in loam, and an interesting feature is that they are fashioned on a strickle machine driven by a lathe headstock having quick-operating adaptable clutches (Fig. 15).

As mentioned above, the majority of patterns in this section are all split and jigged to the molding box, as illustrated in Fig. 16, and the method of location is indicated by Fig. 17. Figure 18 shows a radial drilling machine arm cored up ready to close.

Small Castings Unit

D) Figure 19 illustrates the section in which small detail castings, usually below 100 lb in weight, are produced. This department is equipped with a mechanized sand plant, a roller conveyor in the form of a horseshoe, and along one side are situated three power molding machines which deal with the bulk of repetition detail casting production. The department has its own melting furnaces in order that special attention can be paid to obtaining the correct metal specification for these smaller components.

All patterns used in this part of the foundry are suitably constructed on plates for machine molding, although provision is also made for dealing with a number of odd loose patterns without interfering with the main system.

E) The coremaking department is split into two sections, the heavier cores being made on the shop floor and the smaller ones in a mechanized department, which is illustrated in Fig. 20. The production of cores for machine tool castings is strictly controlled so that all intricate gear box castings with multiple cores can be produced to close limits of accuracy, permitting jig-machining at the engineering works.

In the heavy core section, cores weighing up to 3 tons are made, while those handled in the mechanized section weigh up to 500 lb.

The dressing and treatment with refractory coating, as well as inspection of cores, are very carefully carried out to a timetable, so that the cores are ready for almost immediate use. Cores are not stored for any great length of time, for reasons which are described later in this paper.

F) The melting department includes three cupolas of 8, 6 and 3 tons per hour melting capacity. All

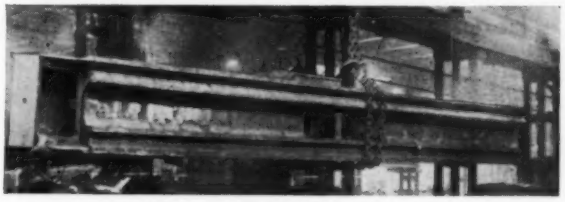


Fig. 10

furnaces are charged from the same platform, including the furnace situated in the small mechanized section. The platform is reached by electric lift and all furnaces are fitted with volume and pressure gages for maintaining strict control over the tapping temperature, usually of the order of 1400 C.

The value of thorough control of all production processes and materials with a view to reducing the percentage of defective castings cannot be too strongly emphasized; each job has to be considered in detail from design to the casting operation.

The main function of any control system should be to serve the following three processes:

a) To study production methods and processes and eliminate defective castings.
b) To prevent defective castings arriving at the machine shop.
c) To supply the foundry with complete details of all defective work in the form of record cards and instruction sheets.

All of these are essential if any measure of success is to be gained in reducing financial losses through scrap and defective castings reaching the machine shop.

For the production of an entirely new casting, where a record card (Fig. 21) is not available, a detailed study of the drawing is made in conjunction with the drawing office staff, and later with the pattern shop personnel, before the construction of the pattern is started, so that the completed pattern will be in the most suitable form from the molding point of view. Then, before beginning molding, the following factors are considered: composition of the metal; methods of gating and feeding; camber; sand; mold drying and molding technique.

Having decided upon the above factors, the molder's attention must be drawn to any part of the mold requiring particular attention to detail such as venting, sprigging, ramming, etc., in order to minimize the risk of producing defective castings. On completion, the mold is stamped with the date cast, so that any defects which appear during the machining of the casting may be traced back by referring to the

PRODUCTION FLOW DIAGRAM FOR MACHINE TOOL CASTINGS.

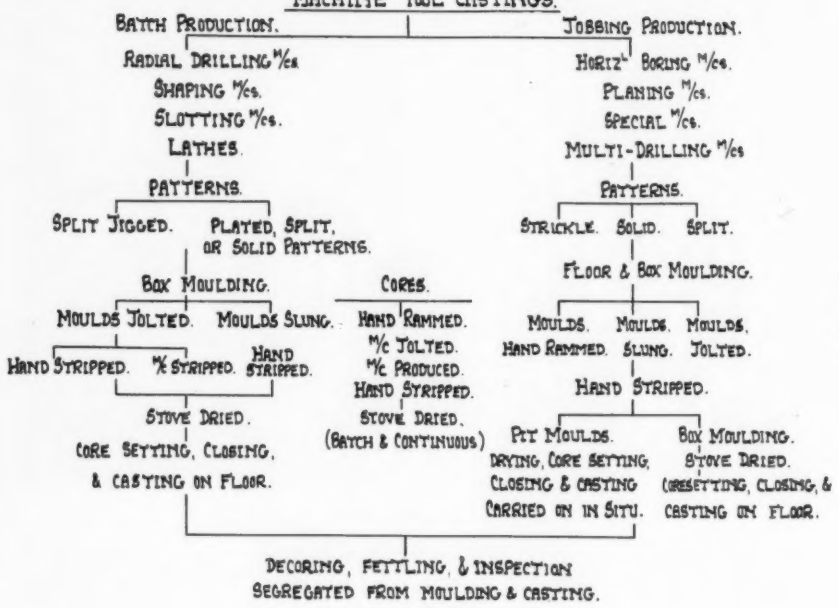


Diagram No. 1

FOUNDRY ORGANIZATION.

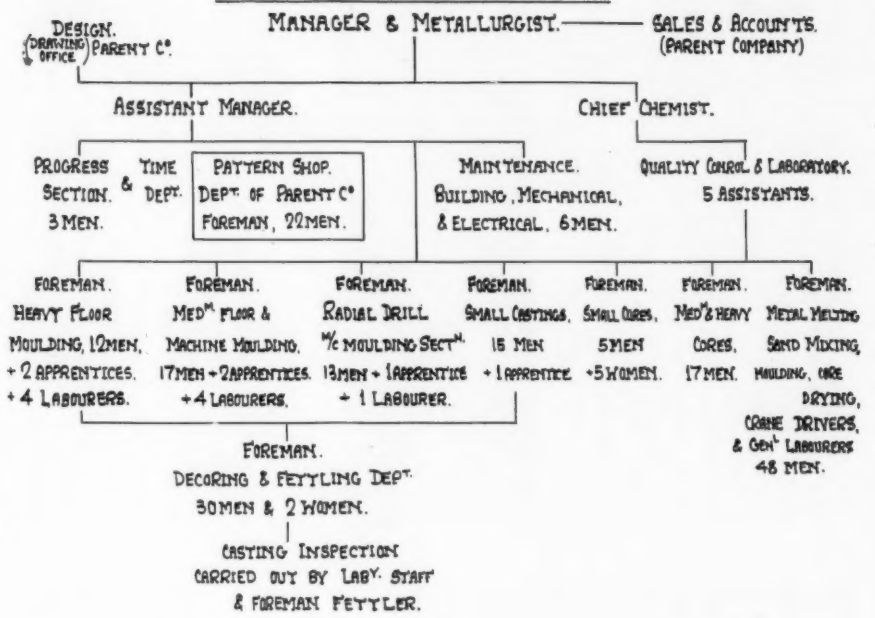


Diagram No. 2

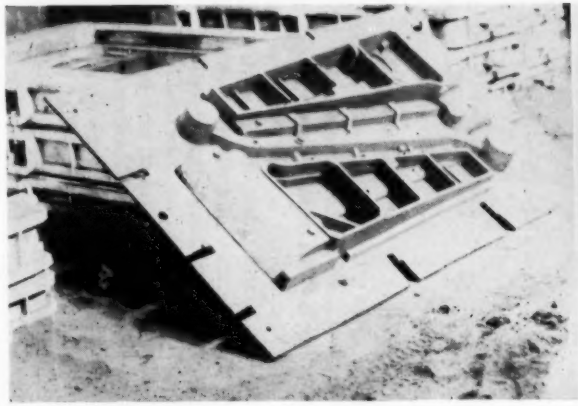


Fig. 11

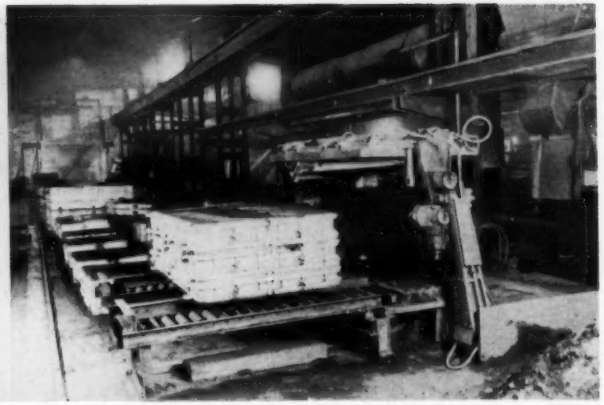


Fig. 12



Fig. 13

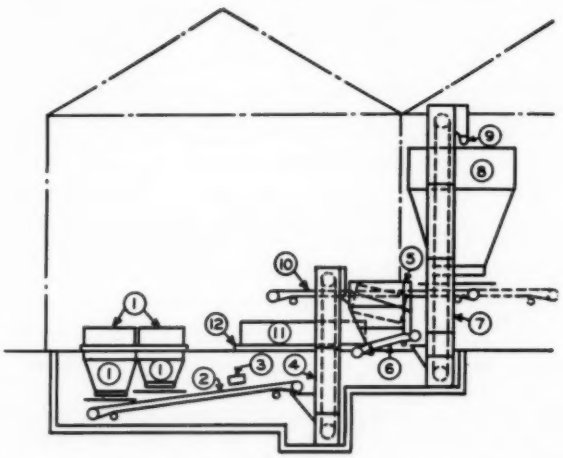


Fig. 14—Sand-handling layout. 1) 12 x 4 ft vibrating knock-out grid; two rotary feed hoppers and pit covers. 2) No. 1 belt conveyor, 18 in. wide belt, 23 ft centers. 3) Overband magnetic separator. 4) No. 1 elevator, 9-in. buckets, 16-in. centers. 5) Rotary screen, 3-in. diameter. 6) No. 2 belt conveyor, 18 in. wide belt, 7-ft. centers. 7) No. 2 elevator, 9-in. buckets, 27-ft 6-in. centers. 8) Two rotary storage hoppers—total capacity, 75 tons. 9) Paddle type mixer-conveyor. 10) No. 3 (shuttle) belt conveyor; 18-in. belt, 23-ft. centers, 12-ft. travel. 11) Molding box. 12) Jolter.



Fig. 15.

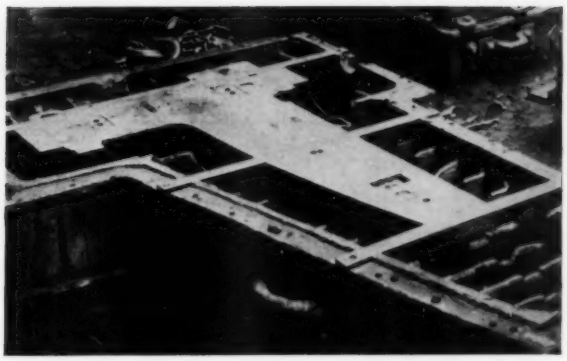


Fig. 16.

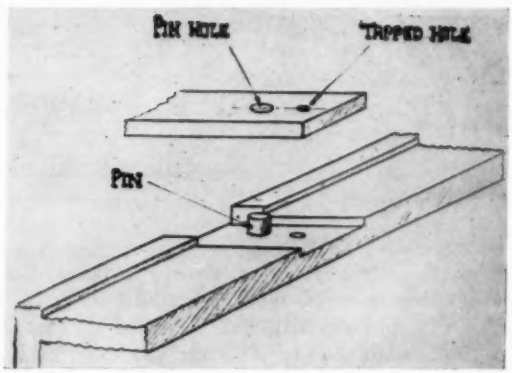


Fig. 17—Jig location of patterns for molding box.

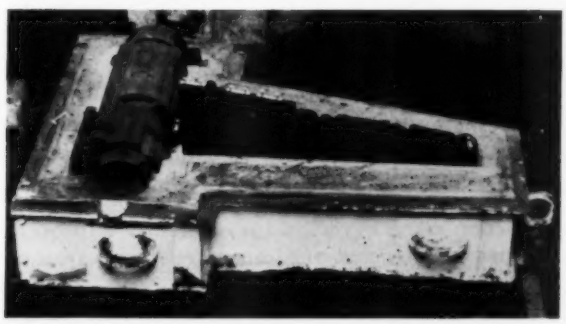


Fig. 18—Mold for drilling machine casting with cores set and mold ready for closing.

conditions under which the mold was made. The time taken to pour the casting and the temperature of the metal are also recorded on the card (Fig. 21).

Apart from the recording system, inspection has also been established to cover mold cleanliness and dryness. Properties of sands are controlled to obtain a satisfactory combination and this condition is checked daily, sometimes twice daily. Molders and coremakers are instructed as to the requirements to be observed in the molding operation, with special reference to venting, gating, and feeding technique. In addition, all molding and coring operations are standardized.

Conveying such information to the operators is of the greatest importance in this control system, and a process sheet giving brief instructions on all import-

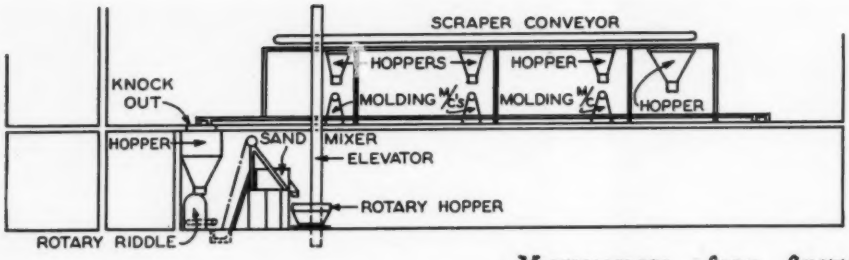


Fig. 19—Molding and sand-handling layout of foundry section producing small detail castings, generally less than 100 lb in weight.

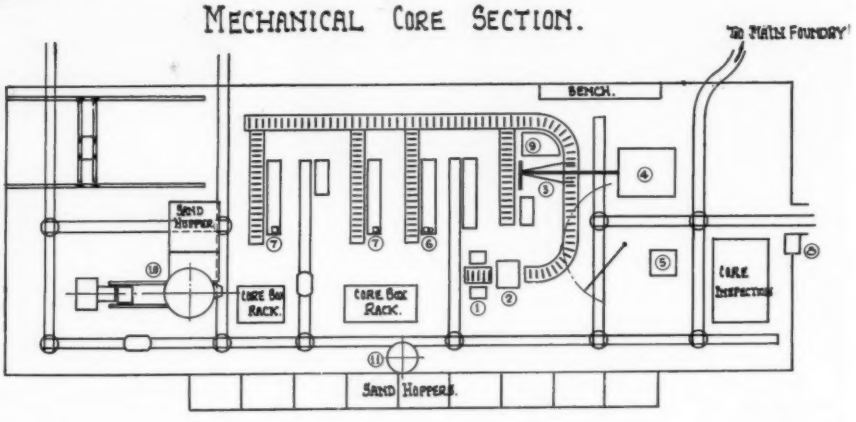


Fig. 20—Layout of mechanized small core department. 1) Core Jolter. 2) Core turnover. 3) Charging machine. 4) Core drying stove. 5) Hoist. 6) Core machine. 7) Bar cutter. 8) Blacking mixer. 9) Blacking spray. 10) General sand plant. 11) Oil and sand mixer.

ant points, such as the type of sand to be used, special molding features, type of gating and the number of drying holes required, is issued to the foreman along with the order for the job (Fig. 22).

After the casting has been completed in accordance with the instructions issued to the foreman molder, the next stage is a thorough examination of the fettled casting by the quality-control staff, and the recording of their findings on the record card. Any action necessary to improve the quality of the casting is then taken.

The machining operations having been completed, a member of the staff makes regular routine tours of the machine shop and reports on any visible defects on the castings examined (Fig. 23). The results of the action taken to rectify any defects on subsequent castings are observed and the necessary information added to the standard record card.

At the commencement of the system it was realized that the laboratory staff would have to perform a large part of the work involved, and they were, therefore, trained for this purpose and gradually made responsible for maintaining the standard and quality of castings. They are also responsible for taking all necessary action to prevent defective work from reaching the machine shops. If defects are brought to light, the laboratory staff record full particulars and then investigate the causes and take any action necessary for their elimination on future runs.

Value of Record Cards

A further responsibility is that of submitting data to the foundry, this data being taken from the record cards. The value of this part of the work (compiling records of all defective and scrap castings), in most cases is insufficiently recognized.

The information on these records serves two use-

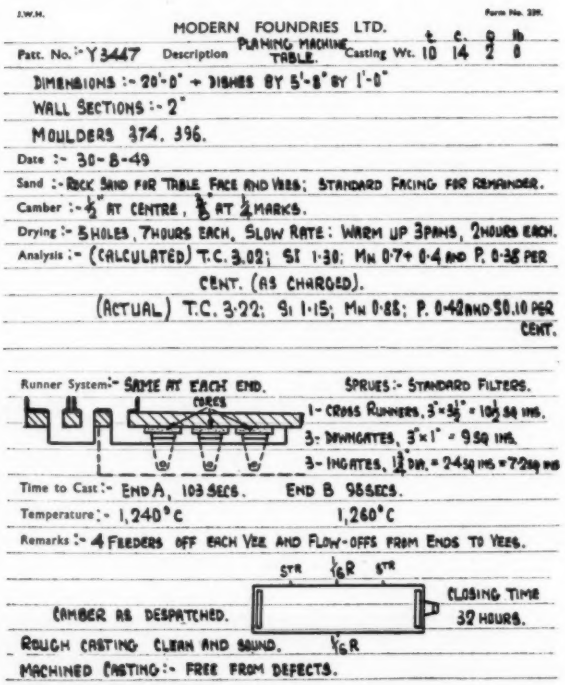


Fig. 21—Record card showing molding and pouring details for a planing machine casting.

ful purposes: a) it indicates to the management the possible causes of scrap; and b) enables the foundry to correct reported defects in the shortest possible time. It has been found that where defects are not properly diagnosed, much time is spent on incorrect remedies. In order to carry out proper diagnosis of defects, the laboratory staff must be properly trained and hold frequent consultations with the foundry

foremen, so that the records combine the judgment of both departments.

In launching this system it was borne in mind that one of its objects would be to eliminate haphazard methods of investigation, and that in every case where an investigation was necessary, it would convey an over-all picture of every phase of the manufacturing process, as any defect can be the result of a number of out-of-balance factors. Therefore, the diagnosis must be tackled by a combination of technical studies, quality-control and laboratory tests.

For the system to operate satisfactorily, it is essential that the staff be made scrap conscious and fully trained to specialize in one or more aspects of foundry technology. It is also necessary to have available an adequate supply of testing equipment and to ensure that the whole staff fits effectively into the manufacturing process.

After a careful study of the record cards collected during the first 12 months it was found that of the castings rejected 47 per cent were due to blowholes; 25 per cent to sand or dirt inclusions; 17 per cent to shrinkage and metallurgical unsoundness; 6 per cent to misruns or cracks, and 5 per cent to miscellaneous causes, e.g., distortion, short-cast, core movement, etc.

From the inauguration of the recording system the percentage of castings rejected showed a downward trend despite a) the more rigid foundry and machine-shop inspection than that obtained during the war years, and b) the increase in the gross output from the same number of operators. All the record cards and process sheets are maintained and filed by the laboratory staff. In addition to their use on the present system, they also serve a useful purpose in the training of foundry apprentices, all of whom pass through the laboratory as part of their training.

Fig. 22—Process Instruction Sheet

Planer Bed—PBE/01
16-ft Stroke—Y.4495

Length .....39 ft 9 in. (Over-all)
Width .....4 ft 3 in. (Order)
Camber .....2 in. at center

In addition to carrying out normal good molding practice please adhere to the following instructions:

1. Set camber according to sheet issued by laboratory.
2. Use two 1¾-in. dia. ingates into each end of flat and vee, in position marked by pattern shop.
3. Use 3 x 1¼-in. down runners.
4. Thoroughly vent down sides of pattern.
5. Use 4 x 3-in. cross runners arranged so that there is a 1-in. step at top and bottom at junction with downgates, i.e., use 1-in. cover cores over downgate cores.
6. Take dirt risers from cross runners between sprues and first ingate.
7. Make 10 drying holes, end holes to be 1 ft 6 in. from ends of bed.
8. Use 4-in. dia. sprues through top part.
9. Take bushed up side feeders from each bolting down lug, as per pattern provided.
10. Black mold before drying and spray-black after drying while there is still sufficient heat in the mold to dry out the moisture in the blacking.
11. Take coked vent away from each pocket core, i.e., the cores forming the recesses beneath the bolting down lugs.
12. Any cores which have to be "rubbed" to fit must be re-blacked with special blacking obtained from the laboratory.
13. Place clay seal around cross runner, to prevent metal entering mold at joint.
14. Use dry sand runner basins, plugged at commencement of casting operation.
15. Inspect all cores on day prior to coring up.

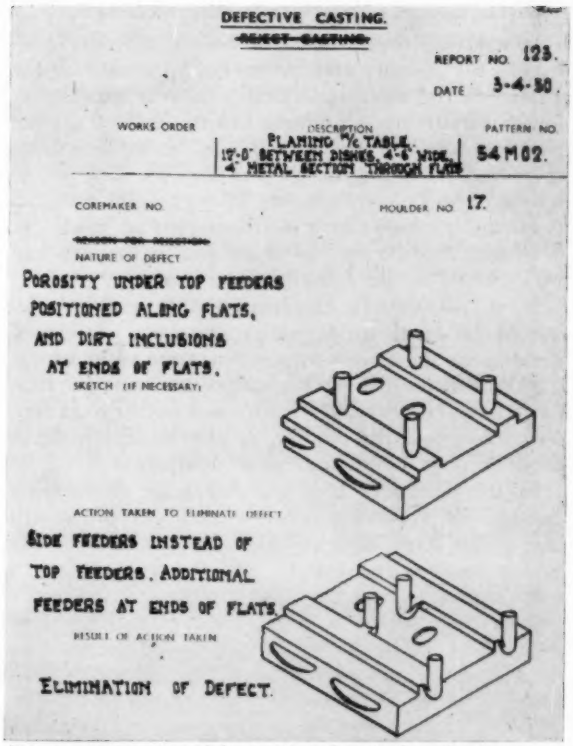


Fig. 23—Report card for casting defects found in machine shop showing nature of defects and action taken to eliminate them.

Staff Training

The staff, as mentioned previously, are trained to specialize in one or more branches of foundry technology such as 1) design of castings, pattern construction and preparation of molding tackle; 2) sand properties and control; 3) mold camber; 4) gating of molds; 5) feeding of castings; 6) drying processes; and 7) melting and casting operations.

This method of breaking down the foundry processes into seven groups was used partly because these were found to be the main categories into which to classify defective castings, and also because it was further considered that it would be necessary to record all defective work, such as defective molds, cores unsuitable for use; or metal unsuitable for casting, if complete quality control through the various processes up to the casting stage was to be achieved.

The training of the staff to fit into the organization was partly achieved by establishing a complete reference library of technical literature relating to foundry technology and casting defects, carrying out fundamental investigations of foundry problems as they arose, and issuing instructional notes on the result of the findings of these investigations.

The way in which the results of investigations and the information contained on the record cards has been used to assist in the elimination of defects, together with the principles involved, is illustrated in some of the following sections.

Casting Design, Pattern Construction, Molding Tackle

In studying the problem of design, defective castings in the foundry and machine shop constitute the quality-control staff's practical school of experience, for by careful study of these castings from the same patterns can be made correctly and many difficulties overcome. It is considered very unwise, however, to wait for scrap to occur before attempting to avoid it. A far more sensible idea is to consider, on receipt of drawings, the best method of making the pattern and what should be the foundry requirements.

It is particularly emphasized that examination should be made to determine the best method of molding at the lowest cost, at the same time achieving the best results. Arrangements are made for such patterns to be periodically inspected both by the pattern stores and the molder, as poorly kept patterns produce poor molds and slow production.

In the design of medium and large engineering castings, the following points are considered; a) will the design lend itself to quality production at the lowest possible cost, and b) will the type of pattern equipment proposed maintain quality castings of the required tolerances. Consideration is also given to breaking down large structures into components suitable for machine molding, or if this is not practical, provision is made for the easy withdrawal of the pattern equipment from the molding sand without having to resort to the use of irregular parting lines coupled with the use of loose pieces.

It is generally understood that the more complicated the design the more individual core boxes, etc., are required, which always has an adverse effect on the resulting accuracy and price of the finished component. In other words, it is better to make the design of a large construction as an assembly of several parts, which may be machined separately and fitted together later, using shoulders or pins for the alignment, and fastening the parts together by bolts or other means.

Design Pattern for Machine Molding

At the same time isolated facings are designed, wherever possible, as one common face to permit machine molding (Fig. 24). Protruding brackets or arms are made from separate patterns, which after casting and machining are secured and aligned to the principal body. All this provides for an easier and more economical method of molding than if molded in one piece from loose patterns on the floor.

The patterns are constructed to prevent scaling and warping, during both the molding operation and the extraction of the pattern from the mold, and sufficiently braced to prevent breakage on the initial high pressure to which a pattern is subjected when first drawn from the sand.

For large circular designs of castings the molds are made with strickles, as in loam or strickle molding, without the use of expensive equipment, ensuring that designs show a thicker section of metal at the outside, and not in the center. Designers should consider this possibility when designing such parts. In those foundries where heavy jolt machines are utilized, and where usually one half of the pattern is jolted and then handed to the molder to hand ram the top portion of the mold, the molding tackle available should be examined to see if it is suitable for the jigging of the pattern to the molding box (Fig. 16). By such methods, production times can be greatly reduced.

Before starting to work on any castings, a certain amount of engineering and planning is necessary to determine the proper method of procedure. The drawings are checked carefully and special consideration is given to the following details:

Ribs or other protruding parts are so designed that they may be drawn from the mold as fixed parts of the main patterns. The individual cores are kept to

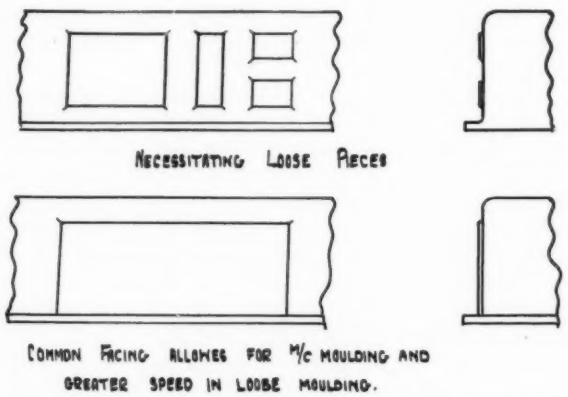


Fig. 24—Redesign of casting facing to permit machine molding.

a minimum in order to reduce the number of chaplets required for supports, and the design is also checked to see that cores permit reinforcement by cast irons or loose rods when needed, and that it is possible to remove the core from the cored holes with ease when cleaning the latter.

Many of the foundryman's problems arise because the engineer fails to take into account the contraction or shrinkage characteristics of the metal. Isolated heavy sections must be properly cared for by gating and risering, or they must be eliminated. All sections of the casting should be as nearly uniform in thickness as possible.

When a change in section is necessary it must be done gradually, otherwise defects such as shrinks, hot tears, cold laps and cracks are bound to occur, as well as the possibility of excessive stresses causing later failures. Corners at adjoining sections must have ample fillets to prevent tears and cracks, but care must be taken to avoid making them too generous. The generous fillets tend to create hot spots which are liable to occur at "T" and "X" sections. The thickness of casting sections must be so proportioned as to permit steady flow of metal, avoiding sections which would interrupt this and cause cold shuts.

Patterns are, if possible, constructed to permit cores to be vented from the top to facilitate escape of gas. They are so designed that the castings can be filled from the bottom, except in special cases, even though it is necessary to use specially prepared runners to get the metal into the casting at the bottom.

In most cases the part is designed so that it may be molded with any machined surfaces in the bottom side of the molds, as this side of the casting is the easiest to produce with the least blemishes. The designer can aid in this by designing the part for ease in drawing the pattern with machined side down. Abrupt changes of section and sharp corners are avoided. The design should be such that, on cooling, no part of the casting is unduly stressed either by tension or compression, and with no interference to free contraction.

Examination is made of the design for parts containing sections likely to cause shrinkage, so that when molding provision can be made for the shrinkage to occur in feeder heads or feeder runners. It must be remembered that the cavities and shrinkages are liable to appear in the heaviest sections, particularly where ribs or bosses join the main section. Progressive solidification from the bottom to the top should be planned. If, for reasons of design, this is not practical by normal methods, denseners may be used.

One of the most uneconomical features of floor molding of heavy castings of intricate design is the necessity of making deep joints (Fig. 25) which absorbs in some cases as much as 50 pct of the labor charge in preparing the mold. Modification of design, would reduce this charge by such a large amount as to have an appreciable effect on the cost of the castings, and further assist in improving output by earlier release of the skilled molder from each job.

Pattern Design and Mold Joints

Mold joints are determined by the design of the pattern, the shape of which controls speed of production. The greater the deviation of any joint from its common horizontal plane, the greater the foundry handicap, coupled with increased production time. Much more can be done by the foundry to exert pressure on all concerned to eliminate faulty casting design, whereby foundrymen may reduce their losses, increase production, improve their products and save vital raw materials.

The responsibility for all reject castings must not, however, be placed on the shoulders of the designer. Many are the result of poor workmanship in the production of molds and/or cores, and errors made in assembling them, which could be avoided by taking more care and giving greater consideration to essential points. Common among molder's mistakes are misplaced cores, sealed and forgotten vents, badly placed studs and chaplets, crushes, and misplaced runners and risers.

Consideration should be given to all molding tackle, particularly molding boxes, before the operation of molding is commenced with a view to ascertaining that the design is sufficiently rigid for the amount of sand to be carried.

Particular attention should also be paid to the box bar construction, as many defects have been attributed to the design of box bars; e.g., if they are too close to the pattern it may result in loose and uneven sand, which may produce scabs or rough surfaces.

Bars which are too close to the runners or risers may result in hot tears, cracked or distorted castings, due to interference with the normal contraction of the casting. Loose or springy bars can result in subsequent sand drops. If these take place after closing the mold, generally the casting is a total loss.

Pin holes in the boxes should be checked periodically to ascertain that the centers are maintained, thus avoiding the production of cross-jointed castings.

Wherever possible the foundry staff now consult with the drawing office and the pattern shop before a design is finalized and before the pattern construction is commenced.

Sand Control

In order to maintain a steady flow of sound castings, it is essential that variations from proved properties should be corrected immediately, and not left until a pile of scrap or defective castings bears mute evidence of the necessity.

Although perfect sand control, particularly in a jobbing foundry, can seldom if ever be achieved, it is possible to obtain effective control by regular testing of raw materials and mixed sand, and by taking suitable action immediately when any discrepancies appear. Variations in sand properties are best illustrated by a graph chart containing the daily test values. General tendencies of the different properties can then be observed easily, without having to study long lists of figures. The charts are also valuable for record purposes, since they indicate the properties of the sand used in the molding of any defective casting, provided the date has been stamped on the mold.



Fig. 25—Floor mold for heavy casting showing deep joint made necessary by design of pattern.

Controlling the physical properties of a sand between certain fixed limits, however, does not necessarily mean the elimination of all the defects attributed to sand. Many defects occur through the improper use or treatment of good sand. Furthermore, sand which is suitable for one class of work may cause endless trouble with a different type of casting.

Thus, in the foundry to which the author is attached, four different grades of facing sand are in constant use. Each grade is suitable for a particular class of casting, ranging from a fine sand for small work to a coarse bonded sand for large castings.

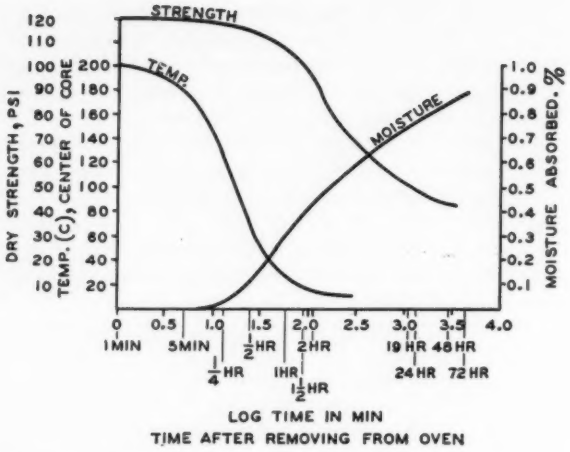


Fig. 26—Effect of moisture on strength of baked cores. All cores baked for 1¾ hr at 200 C; room temperature, 10 C (50 F); barometric pressure, 75 cm Hg.

The latter sand was recently incorporated in the standard range as a means of eliminating the blind or dummy scabs which sometimes appeared on heavy castings with large, flat surfaces. Blind scabs may originate from a number of causes, such as improper drying, insufficient clay content for the grain type, too high moisture content or uneven ramming of the mold surface.

From a study of the record cards and sand charts, it was found that a sand with a high baked compression strength plus a small percentage of cellulose matter successfully eliminated blind scabs from smaller work, provided it was not dried so rapidly as to cause cracks to appear on the mold surface. All that appeared necessary to eliminate this defect from large castings, therefore, was a sand with still higher dry strength. Scottish rotten-rock sand with a coarse grain size and high clay content was found to be ideally suitable in this respect.

In addition to solving the origin of the defects appearing on castings, the recording system indicates the type of investigation to be carried out with a view to reducing the number of defectives which are likely to appear. Thus, the record cards indicated that a mold which took more than one day to core and close was more likely to show minor sand defects than one which could be closed and cast in one day. This was thought to be due to the increased time which elapsed between drying and casting, permitting the moisture in the sand behind the dried mold surface to strike back. While this creeping back of moisture no doubt plays an important part in the production of defects, investigation showed that considerable quantities of moisture were also absorbed from the atmosphere.

A series of A.F.S. Standard Test Specimens was prepared from a sample of mixed facing sand, and baked until the uncombined water had been driven off. After removal from the baking oven, the cores were tested for dry strength at intervals as they cooled to room temperature. Portions of the broken cores were then tested for absorbed moisture by determining the loss in weight after removal from the oven. The results obtained from this experiment are shown in Fig. 26. It will be noticed that moisture began to be absorbed by the cooling molding sand as soon as the temperature had fallen below 100 C. Also, as the absorbed moisture increased, the strength of the sand was considerably reduced, until after 3 days the absorbed water approached 1 pct, while the retained strength had dropped to one third of its former value.

It would appear from these results, therefore, that defects produced on molds which have stood for an appreciable length of time after drying are due to the combined effects of the loss in strength, which is

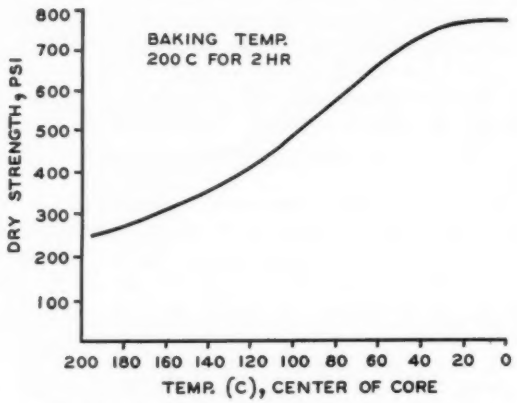


Fig. 27—Determination of baked strength of oil-bonded sand on cooling from baking to room temperature.

known to cause blind scabbing, and to the absorbtion of moisture on the mold surface. Two modifications have therefore been made to the molding practice for castings taking more than one day to close.

1) In cases where the cores are all suspended from the cope, e.g., planing machine tables and horizontal boring machine workplates, the top parts are removed after securing all cores and the mold warmed before final closing.
2) For castings where the above modification is impossible, coring up is carried out by a day and night shift to reduce the standing time.

Figures 27 and 28 depict the results obtained by extending this investigation to the case of oil- and resin-bonded sands. The strength of an oil sand was shown to increase progressively as the temperature fell to normal (Fig. 27), due to the setting and hardening of the oil film, but on prolonged standing a reduction of strength occurred similar to that noted with molding sand (Fig. 28). In obtaining the latter results, the baked cores were stored at a constant temperature (65 F) with 100 pct relative humidity to accentuate any moisture absorption which occurred. The moisture absorption figures obtained after 16 days standard were: proprietary drying oil, 0.92 pct; urea-formaldehyde resin, 0.92 pct; phenol-formaldehyde resin, 0.53 pct.

Oil-sand cores for large castings may have to be stored for relatively long periods before use, due to the fact that many cores must frequently be made

from the same core box. In order to obtain the best results, therefore, the absorbed surface moisture is removed, either by returning the cores to the baking oven at a reduced temperature, or by making use of the heat contained in a recently cast mold. In addition to increasing the strength of the core, this warm-up ensures that both mold and cores are at approximately the same temperature and so reduces the risk of condensation taking place.

Camber

Internal stresses, which are present in all but the simplest of castings, arise from a number of causes, the chief of which is the unequal cooling rate of the different sections. Any distortion which occurs because of these stresses usually is not very noticeable with small castings, but as the casting length increases, so the distortion is magnified until it exceeds the machining additions to the pattern.

In the manufacture of sizeable castings, therefore, allowance must be made for the distortion which will take place on cooling. Until recently, the knowledge of the amount of camber required for the production of straight castings could only be gained from past experience in the production of similar castings.

E. Longden[5] in an excellent paper on contraction and distortion of iron castings, included a camber graph which describes the camber that he found necessary for the castings produced at the foundry with which he was associated. On comparing this graph with the results obtained on similar castings (machine tool bedplates, tables, etc.) at the author's foundry, some slight variations were noticed, which can be explained by the differences in the respective castings. Nevertheless, the graph can be used with confidence once the differences between the types of casting are known.

The method adopted at the author's foundry is to compare the amount of camber called for by the camber graph with the amount given to a casting of a design similar to the one being prepared, and to adopt a compromise between the two values after considering the various factors involved. By this method, castings are rarely more than $\frac{1}{8}$ to $\frac{1}{4}$-in. distorted, even on a length of 40 ft providing the strickles used for making the bed of the mold have been set correctly. Incorrect setting of the strickles, it was found, was the most usual cause of distorted castings.

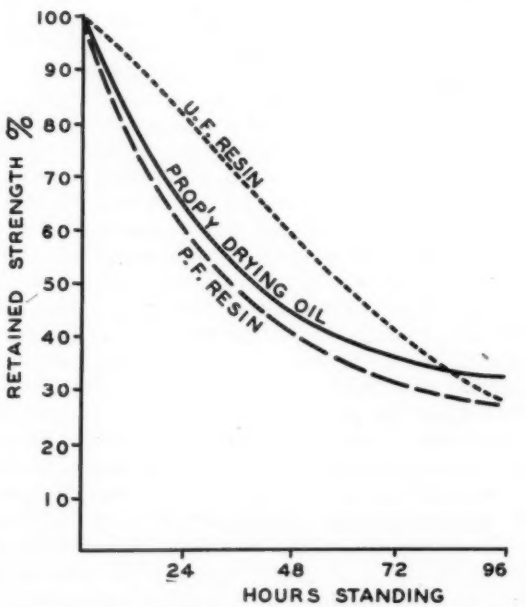


Fig. 28—Determination of strength of oil- and resin-bonded sand mixtures upon prolonged standing. Oil mixture; 1.75 pct oil, 0.80 pct cereal, 4 pct moisture, baked 2 hr at 210 C. Resin mixtures; 0.75 pct (net) resin, 1.5 pct cereal, 4 pct moisture, baked 1 hr at 190 C.

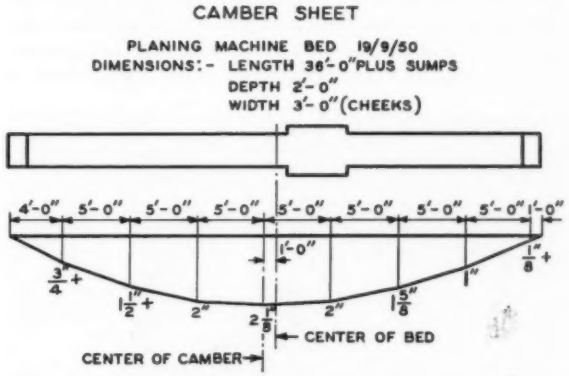


Fig. 29—Diagram showing amount and distribution of required camber.

Before the study of distortion was commenced, the camber was set by bedding long wooden strickles into the floor of the casting pit and bending them to the desired camber. One, two, or three strickles were used for this purpose, depending on the length of the casting and the strickle sizes most readily available. Also, while the molder was instructed as to the amount of camber to be used, the distribution of the camber along the bed was left to his discretion. It was found that the most usual practice was to set the full camber at the correct point, and to use half of the full camber at points half-way between the center of camber and the ends of the bed. This caused the camber to be set in a misshapen manner.

A formula was then derived from geometrical principles so that the distribution of camber at any point along the arc of a circle could be calculated, provided that the maximum camber was known. Also, the length of strickles was standardized at 5 ft and the camber required at points at the juncture of these strickles was calculated.

A diagram showing the amount and distribution of the required camber is now issued to the molder before starting the job (Fig. 29).

In order to set the camber accurately it is necessary to put the required radius of curvature into the strickle. The amount of curvature required on 5-ft lengths, however, is very small, and it is within the experimental error to use straight strickles.

Assuming that the casting to be produced has a uniform cross section along its entire length, the center of camber, i.e., the points at which the maximum camber occurs, will be at the center of the cast-

ing. Bedplates for planing machines, however, possess heavy flanges of metal projecting from the sides, to which the cheeks for the crossrail are bolted. From Fig. 29 it will be seen that these projections are not in the center of the bed; in fact, they may be anywhere along the bed, depending upon the design of the machine.

Isolated heavy metal sections affect the resulting distortion of a casting since, in addition to altering the cooling rate, they possess additional strength and so prevent the casting from bending. Accordingly, the end of a bed casting, containing the heavy side flanges, cambers less than the uniform end.

This difference in cambering tendencies is allowed for by moving the center of camber from the center of the casting to a point away from the projections. The actual location of the center of the camber is determined from past experience in the production of similar castings. The net effect of altering the center of camber is to increase the radius of curvature of the end to which the camber is moved, and simultaneously decrease curvature of the other end.

Castings which possess no longitudinal plane of symmetry require side camber to be used when setting the pattern, in addition to the usual bottom camber, in order to nullify the distortion produced by the differing cooling rates of the two sides of the castings.

In setting the strickles, a string is first stretched along one side of the floor of the casting pit from two points at equal depths below the foundry floor level. The strickles are then placed underneath the string to give the desired camber by using setting blocks of the required size. Great care is taken when securing the strickles in the floor of the pit to ensure that no movement occurs. Before the string is removed, the camber points are checked by the laboratory staff. The whole process is then repeated at the other side of the pit so that two lines of strickles, accurately set, are secured in the floor. Facing sand is then firmly rammed in between the two lines and the surplus sand strickled off. During the strickling-off process, the accuracy of the strickle setting is checked by placing a spirit level across the junction of the strickles at each end of the mold (Fig. 30).

The camber is again checked after the mold has been dried, and the chills forming the slideways have been placed in position (Fig. 31). In this way the number of defective castings produced through incorrect camber has been reduced to a minimum, and a casting rejected because of excessive distortion has become a rarity. However, allowances must be made for the sag in the line band, which varies from ¼ to ⅜-in. over a 40-ft length.



Fig. 30 (left)—Accuracy of strickle setting in pit mold is checked by means of a spirit level.



Fig. 31 (right)—Camber is again checked after drying mold and placing chills.

Gating

In determining the number and size of gates as well as their location, it is important to consider their effects on the resultant casting, and this cannot be too strongly emphasized. In many foundries, even today, the runner is regarded simply as a channel to permit the molten metal to enter the mold cavity. They are sometimes made as quickly and cheaply as possible, since it is not realized that the gating system is subjected to a more severe cutting action than any other part of the mold, and should receive the most attention from the molder.

The principal function of an efficient gating system is to properly run and feed the casting, and to allow clean metal to enter the mold with the minimum of turbulence and agitation. To be efficient, therefore, some form of dirt trap must be included to hold back any dross or slag which may be present in the metal. It is a mistaken belief that the inclusion of a dirt trap at some one point is sufficient to ensure a clean casting. The whole of the gating system, from the pouring basin to the ingates, must be fashioned to minimize the possibility of dross entering the casting. This entails the correct proportioning of the various components of the gating system in order to obtain the required degree of choke on the metal flowing through the system, in addition to incorporating such dirt traps as may be deemed necessary.

The separation of slag from metal in a gating system is usually accomplished by making use of their differing physical properties, density and viscosity. By causing a molten mixture of slag and metal to rotate, the centrifugal force produced acts in addition to the normal gravitational effect, and so ensures a more rapid separation of the two constituents. This principle is employed successfully in the whirlgate types of runner used in the gating of small

castings. With larger castings, however, the use of the whirlgate is not always practicable, and so other methods may have to be used. One such method employs a slag dam, usually in the cross runner, one form being illustrated in Fig. 32, whereby any slag floating on the surface of the flowing metal is held back, permitting the clean metal to pass underneath the dam and into the ingates.

"Dirt risers" are sometimes incorporated into the gating system. This takes the form of a riser located at a position along the cross runner, whereby any dross on the surface of the metal stream enters this riser and is removed from the gating system.

One very effective method of gating large castings, which have to be machined on both top and bottom faces, is to employ a double runner basin with a plug in the outlet from the second basin, as per the diagram on the record card (Fig. 21). The quantity of dross remaining in the second basin after the casting has been poured gives some indication of the efficiency of this method. If, in addition, syphon runners are included in front of the ingates, there is little likelihood of any dross from the metal entering the mold cavity.

The location, size and number of gates to be used for a casting depends on a number of factors, each of which must receive due consideration before the form of the gating system is decided upon.

Location of Gates

Gates should be located with a view to producing simultaneous cooling of light and heavy sections, thereby reducing or eliminating the internal stress present in the casting. This may be achieved by gating so that the hottest metal goes to the lighter sections and the duller metal into the heavier sections. Also, since any dirt which enters the mold cavity through the ingates tends to lodge in front of the ingates, it is better to gate away from machined surfaces.

Heavy castings, which usually are bottom poured to ensure quiet delivery of metal, are molded with the heavy, machined, sliding surfaces downward in the mold to obtain clean slideways. It usually is impossible, therefore, to observe strictly the above principles in the molding of this class of work.

In running heavy castings, it is preferable to use a number of small runners rather than one large runner, and to provide for the metal to enter the mold with as little hindrance as possible, and in such a manner that the hottest metal enters the thinnest sections first. Furthermore, it is important to remember that the runner system withstands more arduous conditions than most parts of the mold, and therefore requires more attention. The ingates should be made as short as possible, in order to reduce the tendency to pick up dirt and to unduly cool the first metal.

Figure 33 is a section of a planing machine bed with the ingates in position "A". Occasional defects in the form of porous patches appeared during the machining operation in the position indicated. By altering the position of the runner from "A" to "B", and modifying the shape of the densener used, the defect was rectified.

Gates should also be situated so that the metal on entering the mold cavity does not impinge against a core or fragile part of the mold, nor should it flow onto a surface which is drained immediately the metal strikes it.

Pouring Rate

The rate of pouring a mold has a pronounced effect upon the resultant casting. Slow pouring aggravates the troubles associated with the expansion of the sand, such as buckles, rat-tails, and pulldowns, due to the increased time during which these defects may occur. Slow pouring may also result in cold laps or seams if the loss of temperature taking place during the filling of the mold causes the metal to set before the cavity has been completely filled.

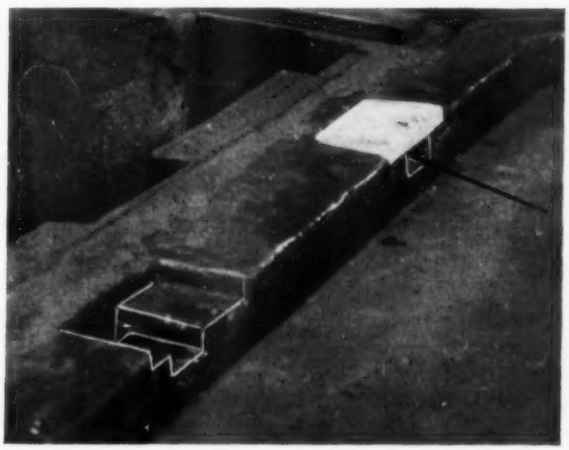


Fig. 32—Method of placing slag dam in runner of mold for large casting.

Gating Technique

Molten metal on entering the mold gives up heat to the surrounding sand, and the greater the distance it travels the greater the cooling effect. In very large castings, fast-flowing of metal occurs in parts, while on other parts the metal may remain stationary. It is evident, therefore, that it will be impossible for some parts to be freshened with clean hot metal (unless an auxiliary system of runners is used which can be brought into operation by means of a short-circuited dry battery lamp and two wires rammed-up in the mold, at the level at which it is desired to freshen the metal).

These variations result in trapped gas and unequal time of setting, quite apart from the question of thickness. The choice of runners must be determined according to location, and the capacity for distributing and filling the mold with metal at, as nearly as possible, uniform temperatures. Whether or not top or bottom running is utilized depends on the type of casting.

Fast pouring, while reducing or eliminating the defects outlined above and helping denseners to function more evenly, has its own attendant disadvantages. Intricate jobs containing many cores require time before the free functioning of the vents from

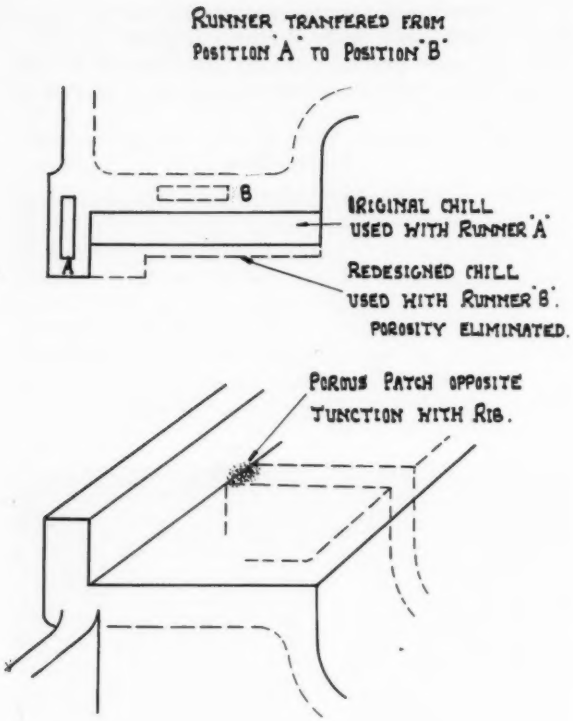


Fig. 33—Altering position of runner and changing shape of chill eliminated porosity defect in large planing machine bed casting.

these cores can commence. If the mold cavity has been filled with metal before this occurs, core gases may be forced into the metal and trapped in the casting. Higher pouring speed also involves greater turbulence in the flowing metal with a consequent increase in the erosive action.

Shape of Ingate

Where possible, round ingates are preferred to square or rectangular ones. A circular ingate, possessing no corners, is much easier to dry thoroughly than other shapes, and has less surface area for a given cross section, thereby reducing the possibility of erosion taking place. There is also a more streamlined flow of liquid, causing less turbulence as the metal is delivered to the mold cavity. Ingates should be as short as possible to reduce the amount of heat lost by the metal, and should be choked at the junction with the downgate to prevent a nozzle effect squirting the metal into the mold.

Efficiently designed gating systems are not adequate in themselves to ensure a clean casting, since they will not compensate for any slipshod methods used in the making of the system. The majority of large castings are bottom poured to ensure quiet delivery of metal with the minimum of agitation, so that impurities picked up by the metal after passing from the cross runner and the downgate must be carried into the casting. If this part of the gating system is made by the normal method of ramming sand around gate sticks, the use of a finishing tool or the application of a coating of refractory material to the sand is impossible.

The most vital part of the gating system is, therefore, the most difficult part to produce satisfactorily. In order to overcome this difficulty, all down and ingates are now made in the form of cores, so designed that the coremaker can finish and coat with refractory dressing the surfaces against which the metal will flow. The finished dried runner cores are rammed-up with the pattern, as illustrated in Fig. 34.

Feeding

Basically, one of the chief problems of the foundryman arises from the fact that he must cast a liquid material into a mold and then take from this mold a solid of high quality, which will, within close tolerances, meet the demands of the designer. Of the total shrinkage, that which occurs on the change from the liquid to the solid state is the first concern of the foundryman. Because of this shrinkage, a uniform sectional thickness, as already referred to, is a cardinal principle in good casting design. Failing this, at least a progressive increase in section to a location where a feeder can be placed is essential.

There are three main stages in the cooling of the molten iron-liquid stage, solidifying stage and solid stage. Foundrymen should direct their energies to trying to obtain the combination of the lowest possible shrinkage and free escape of gas, which will result in a sound, close-grained casting. It should be remembered that when the metal in a mold cavity freezes according to the principle of controlled progressive solidification, the coldest and thinnest-section metal furthest from the gates freezes progressively toward the hottest metal and heaviest section.

This means that the freezing portion of the casting is being fed progressively by the slightly hotter metal ahead. The metal in the feeder must then supply the metal to fill the shrinkage requirements of the entire casting. Foundrymen often use feeders adequate for a heavy section of the casting, disregarding the overall shrinkage which may draw metal from a heavy section before it freezes, which loss must be made good by metal withdrawn from the feeders. Nevertheless, with a great body of cores or with hard cores, the expansion of the cores, and to some extent that of the mold, will compensate for some of the shrinkage tendencies.

Successful feeding of a casting consists of supplying liquid metal to the casting to compensate for the reduction in volume which occurs as the metal changes from the liquid to the solid state. The amount of shrinkage contraction which takes place, however, is related to the composition of the iron, so that before designing a feeder for a particular casting, consideration must be given to the type of iron to be used.

In order to satisfy the basic fundamentals of feeding, the casting should freeze progressively from the bottom of the mold to the top, thus ensuring that the last part of the casting to set is immediately below the feeder heads supplying feed metal to the casting. This entails gating so that the hottest metal enters

the feeder heads. With small work, it is often possible to gate through the feeder in order to establish this condition, but with bottom poured heavy castings, heat is lost to the mold and cores during the filling of the cold, causing the dullest metal to enter the feeder heads.

Topping up of feeders as soon as the rising metal enters the neck assists in rectifying the position. Whatever form of feeder is used, the length of the neck joining the feeder proper with the casting should be correctly proportioned, thus allowing maximum feed to take place before final solidification occurs.

In recent years the use of exothermic sleeves for feeders has been developed, whereby the temperature of the metal in the head is increased by the heat generated from the chemical reaction taking place in the sleeve. By taking advantage of these materials, a slower cooling factor in the feeder compared with that in the mold can be guaranteed, leading to greater efficiency of the feeder.

Liquid metal is supplied to the casting, by the combined effect of gravitation and atmospheric pressure, only for the length of time that the surface of the metal in the feeder remains fluid. Once a skin of solid metal has formed over the surface the atmospheric pressure, amounting to approximately 14 psi and equivalent to 5 ft of metal head, ceases to act.

One means of retarding the freezing of the surface layer of an open feeder is to cover the surface of the metal with a heat-insulating material, the so-called feeding flux.

Another method of utilizing atmospheric pressure for feeding purposes is to allow a small piece of oil-sand core to penetrate into the center of the feeder head. The oil-sand core is quickly heated to the temperature of the fluid metal in the riser without absorbing sufficient heat to cause a skin of metal to solidify round it. Atmospheric pressure is then transmitted through the sand to the fluid metal in the center of the feeder even though the upper surface has solidified.

Head Pressure

From the foregoing, it is seen that the effect of metal head is small compared with atmospheric pressure in forcing fluid metal into the shrinking casting. When the atmospheric pressure ceases to act through the formation of a skin on the surface of the iron in both feeder and the casting, the metal head provides the only pressure promoting feeding. Contraction taking place within a solid skin of metal capable of withstanding atmospheric pressure does so with the formation of a vacuum.

Freezing over of the surface of the feeder, provided the body and neck are still fluid, does not prevent metal head pressure playing its part in feeding the casting, since all movement of metal takes place in vacuo, and no additional energy is needed to increase the space between the fluid surface of the metal in the feeder and the solid top. The gravitational effect of a high head can thus play an important role in securing sound castings.

In actual practice, of course, the above conditions are very rarely established, since the atmospheric pressure is usually sufficient to puncture the soft surface

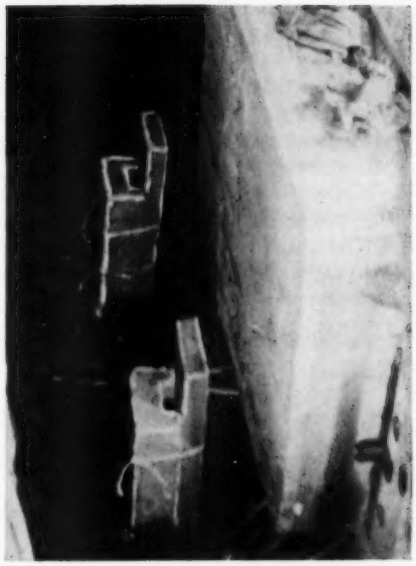


Fig. 34 — Refractory coated runner cores are rammed up with pattern.

skin of a feeder once a vacuum is formed underneath, causing the well known tearing effect to the surface. The general principle is present, however, and the validity can be judged from the increased feeder efficiency gained by increasing the metal head pressure.

This is very noticeable in small castings where the metal solidifies in a very short period of time. In many cases, the metal in the gates remains fluid long enough to allow metal pressure caused by a high pouring head to be sufficient to feed the casting without the use of additional feeding.

High head pressure, however, causes an increased strain on the sand walls of the mold, and additional care must be taken while molding to ensure that no deformation occurs when the mold is poured. Yielding of the sand walls has been known to produce defects which at first were wrongly attributed to shrinkage troubles caused by incorrect metal composition or inefficient feeding technique.

If the mold cavity has been filled before the deformation occurs, a depression (having the appearance of a typical shrinkage defect) is formed at the upper surface of the casting from where the metal has drained to fill the increased mold dimensions.

In the application of the principles of feeding to the production of sound castings it has been found necessary to study the design of each casting produced and to indicate on the pattern the location of the necessary feeders. Standard feeder patterns, suitable for the metal section they are required to feed, are then issued to the foundry along with the casting pattern. Wherever possible, side feeders are used in preference to top or flange feeders, since they appear to function more efficiently and possess the additional advantage that any dirt which may fall through does not enter the mold.

Side feeder patterns are made in two sections, the neck (Fig. 35a), which is molded into the sand at the parting line, and the feeder which is rammed up with the cope. The two sections of the pattern are dowelled to ensure correct positioning, thus avoiding the defect commonly associated with the side feeders,

namely, too long a neck causing premature freezing, and thereby preventing the flow of metal from feeder to casting. The other end of the feeder portion is shaped so that it may be used as a top or flange feeder (Fig. 35b), while Fig. 35c incorporates a step to allow an exothermic sleeve to be fitted inside the feeder.

Heavy feeders, however, are not always essential in the production of large castings. Many such castings can be produced without feeders if due regard has previously been given to the location of gates, and to the incorporation of chills or denseners to promote a uniform cooling rate. In such cases, small sectioned

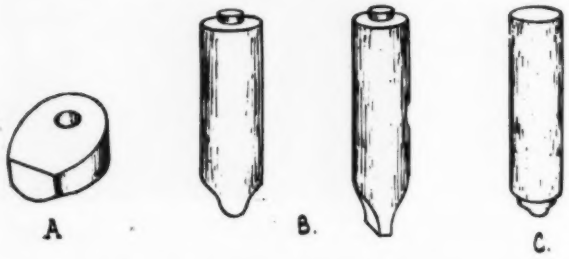


Fig. 35—Example of feeder patterns.

risers, as distinct from feeders, prove invaluable in ensuring clean sound castings. By permitting flow-off metal, more equal cooling conditions are established in the casting, in addition to flushing any gas through the riser openings.

Denseners

In view of the fact that most of the available supplies of pig iron are of the phosphoric type, difficulties are often met in maintaining the phosphorus content of the various specifications to the desired limits. This results in the fairly wide use of denseners. The application of such denseners, however, must be carefully made as they may cause shrinkage in some other position unless they are placed with due regard to controlling progressive solidification. All the denseners should be made large enough for their effect to penetrate across the section to be densified, but not so large that they prematurely freeze the metal in the light or adjacent sections.

Figure 36 illustrates an example where it is necessary to increase the size of the densener in front of the ingate of a large planing machine bed casting. This is necessary owing to the fact that these particular end denseners tend to become overheated when the mold is poured. Increasing the mass effect of the densener to overcome this over-heating tendency permits the metal to be satisfactorily densened. If this method is not adopted sponge porosity can occur in this vicinity, appearing after machining.

Figure 37 illustrates a further application of a densener. In this case it is in the form of tubular steel built up into the cores so that the main sliding bore of this particular gear box may be produced with an exceptionally close-grained metal, free from any sponge porosity. This defect can occur if this densener is not used, owing to the varying metal sections and to the fact, as mentioned earlier, that the phosphorus content of the iron cannot always be controlled to a point sufficiently low to prevent occurrence of internal shrinkage.

Drying

The drying practice of the foundry had previously been in charge of a foreman molder. However, the high proportion of castings which were defective due to blowholes and scabs, etc., made it imperative that the whole drying practice should be reviewed.

Accordingly, a careful study of the matter was undertaken. Every mold was inspected immediately prior to coring, and the depth to which the dried sand extended was noted. Each casting was inspected immediately after shot blasting, and any surface blemishes were noted and correlation made between the condition of the mold and the condition of the casting. The conclusions were:

a) On a mold which was cast within 12 hr of drying, a dried skin of 1½ in. average depth was necessary to prevent scabbing.
b) A convex mold surface required drying to a depth of 2½ in.
c) On a concave mold, surface drying to a depth of ½ in. was sufficient to produce a clean surface.
d) On a mold which was required to stand from 24 to 40 hr, one inch extra skin depth was required to allow for "striking back."
e) A slightly burnt mold would produce a good casting provided the surface was not damaged in coring and the dried skin was 1½ in. or more in depth.

As the standard of drying on a great number of molds falls far short of that required to produce a good casting, the following investigation was carried out:

The mold-drying stoves were first investigated. Two forced-draft coke-fired stoves, each with a recirculating system, were used for drying box molds. Each stove was equipped with a recording pyrometer. The average drying cycle was 4½ hr at 500 F.

The drying temperature was raised gradually to 600 F, but above this temperature the molds near to the air inlet were burnt, despite the fact that the large boxes were placed in the hottest part of the stove. The increase in temperature seemed to have

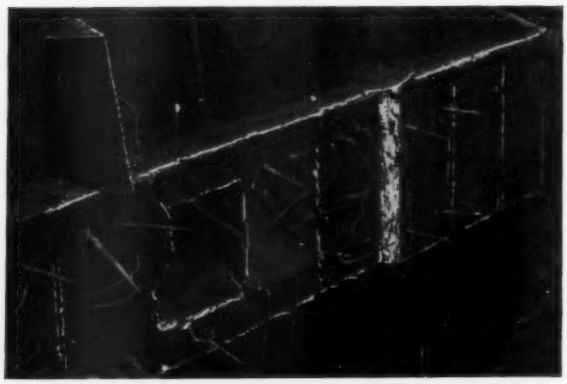


Fig. 36—Deeper denseners are placed in front of ingates to eliminate porosity in large planing machine bed casting.

little effect on the molds furthest away from the air inlet.

The drying time at 600 F then gradually increased until at 9 hr every mold was dry, while none was burnt.

Variations in the size of the batch were usually allowed for by lengthening or shortening the cycle. However, it was soon found that however small the batch, any reduction in the drying time tended to produce under-dried molds; the drying cycle was therefore standardized at 9 hr at 600 F.

As mentioned previously, convex surfaces required drying to a greater depth than flat surfaces, and concave surfaces to a lesser depth. Fortunately, in stove drying any areas where the skin is thin usually are on the concave surfaces, while the convex surfaces are dried to a greater depth than average.

Every mold is checked for dryness before coring commences. When an occasional mold is insufficiently dry, the matter is rectified by re-drying with a portable hot-air dryer.

The reason for the mold being under-dried is sought and invariably found before the next drying cycle commences. Among the common causes of under-dried or burnt molds are:

1) Instructions not fully carried out by operator.
2) Faulty temperature recording.
3) Stoppages in re-circulation flues.
4) Exceptionally poor quality coke.
5) On a number of occasions, the reversal of the fans after electrical breakdown and faulty re-wiring.

The result of rectifying these conditions was to bring about a big improvement in the finish of the castings.

Unfortunately, the molds for nearly half of the tonnage of castings were, of necessity, dried by portable dryers. For many years the drying of molds by this method had been a matter of guesswork. The drying, even in different parts of the same mold, was found to be very uneven, and it was sometimes found that one half of a mold was crumbling due to its burnt condition, while the other half was hardly dried.

The lack of control over the behavior of the drying pans was a serious stumbling block to any re-timing of the drying periods. Before any attempt could be made to improve the standard of drying, some attempt had to be made to control the temperature of the gases entering the mold.

A portable drying stove was therefore set up and the temperature recorded for various positions of the dampers. It was found that the temperature of the gases produced could be varied between 100 and 1000 C by various settings of the dampers.

It was known from experience on the drying stoves that the optimum temperature of the mold face was in the region of 300 C. Experiments were carried out to determine under what conditions the pans had to be run to produce the desired temperature of 300 C.

The drying pans were of simple design, consisting of a firebox with one damper controlling the air supply to the fire and another controlling the supply of secondary air. The following results were recorded:

Temperature with bottom air only, 1060 C.

Temperature with all dampers open, 800 C.

A series of temperatures was taken at 5-min intervals during the combustion of a full firebox of coke with the bottom air shut off and using top air only. The results indicated that the temperature dropped from 480 C at the commencement of the test to 390 C after approx. 10 min when half of the coke had been burnt, and to 300 C after 45 min when approximately 7/8ths of the coke had burnt.

Any opening of the bottom damper was found to produce temperatures in excess of 450 C. It was therefore decided that in all subsequent experiments the bottom damper should be closed.

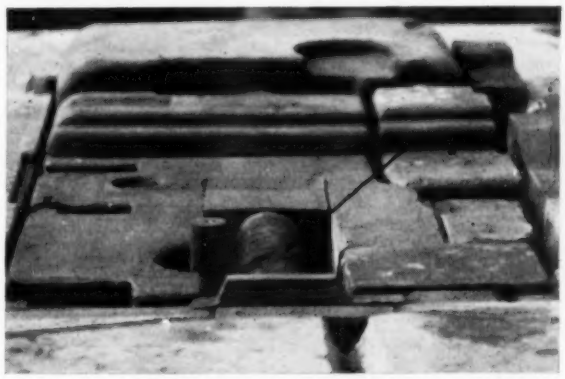


Fig. 37—Another type of densener in the form of tubular steel built up into the cores to obtain close-grained, porosity-free metal in the bore of a gear box casting.

An experimental sand bed was then made on the floor and dried in a manner similar to the usual practice, with the following results:

Directly under the nozzle the surface was burnt to a depth of 3/4-in. over an area of approx. 2 sq. ft.

The bed was dried to a depth of 7 in. immediately under the burnt area, tapering to a depth of 1½ in. at 2½ ft from the center of the nozzle (Fig. 38).

A rather significant feature was a definite crack approximately one inch below the surface.

The experiment was again repeated, the hot gases being kept at a temperature of 300 C. It was noted that to maintain this temperature the bottom damper must be closed and the firebox slightly less than half full. The fuel consumption was 20 lb of coke per 40-min charge. The bed was again examined after 3 hr.

This time there was no burnt area, no cracking and the depth of sand dried varied from 3½ in. at the center to one inch at 2½ ft from the center of the nozzle (Fig. 39).

From this test it was concluded that a drying pan would not efficiently dry any part of the mold which was more than 2½ ft away from the center of the nozzle.

The next step was to standardize the practice throughout the foundry.

First, a standard drying peg was made to ram up in the top part, leaving a hole of 20 sq. in. All drying holes were made 5 ft or less apart.

Several molds were dried successfully and careful

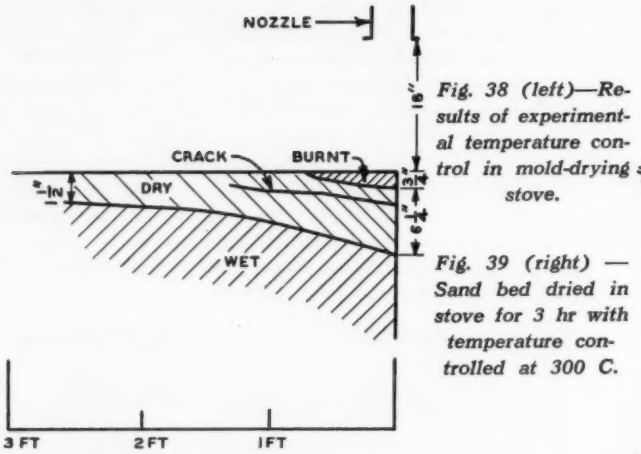


Fig. 38 (left)—Results of experimental temperature control in mold-drying stove.

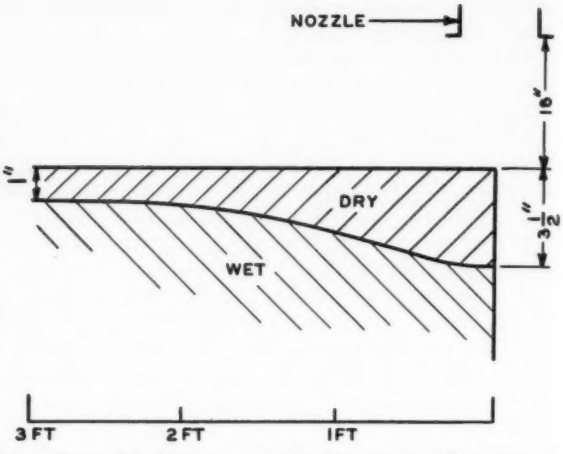


Fig. 39 (right) — Sand bed dried in stove for 3 hr with temperature controlled at 300 C.

records kept. Each casting was examined, and followed through the machine shops.

When details of sufficient molds had been collected, the exact relationship between the surface area to be dried and the time required to dry it was ascertained. It was found that one pan-hour was required for every 75 sq. ft. of mold surface. The drying times for many molds were worked out, using this formula, with excellent results.

It was soon found that deep molds could be dried satisfactorily at a quicker drying rate than shallow ones.

The rate of charging was therefore increased from 20 lb every 40 min to 20 lb every 30 min, and the drying time shortened proportionately, i.e., one pan-hour was required for every 100 sq. ft. of mold.

In addition to the surface area, special attention must be paid to convex mold surfaces. These can often be cared for by careful positioning of drying holes in the cope. Failing this, considerable periods of extra drying are necessary to prevent scabbing from such areas. It is also desirable to increase the skin depth in front of the ingates.

Great difficulty was experienced in drying the runner systems, as during the drying of the mold cavity there was considerable condensation on the cold runner. The only method of overcoming this difficulty was found to be the drying of the runner separately after completion of the mold drying.

A record is now kept of every mold dried. Any particular difficulties are noted and all drying times are filed under pattern numbers, as well as in chronological order. A vast improvement in casting surface has resulted since the drying practice was reviewed.

Pouring Basins and Riser Bushes

The use of a properly designed and prepared pouring basin is of great importance in ensuring clean castings. The main objects of basin design are 1) to permit quiet entry of iron through the sprue opening, and 2) to allow time for the separation of slag before the metal enters the sprue.

The major portion of slag which passes from the pouring basin does so in the time which elapses before the basin is completely filled with metal, and this depth should not be less than approximately four times the diameter of the downgate. The use of a skim plate or plug, to ensure that the basin is full before any metal passes through the sprue, is a wise precaution. Plugs are the best preventative in this respect, but great care must be taken in the preparation and use of them.

Poorly fitted plugs permit small quantities of metal to pass into the sprue and freeze before the plug is removed. Removal of the plugs must be accomplished in one smooth movement to avoid disturbing the sand around the sprue orifice. Any sand which is loosened in this way is carried into the gating system proper by the velocity of the metal leaving the pouring basin.

Dry sand molds should never be prepared with green sand pouring basins. Heat from the warm dry mold rises through the gates, producing a friable skin-dried crust on the green sand, which is easily washed away. Furthermore, the steam produced in drying this crust condenses on the cooler parts of the pouring basin and causes a boiling action when the mold is poured. Accordingly, pouring basins for large dry sand work are now prepared on the day the mold is finished, and dried in a mold drying stove.

All risers are built up on the top part of the mold before it is lowered into place for drying. This ensures that the mold is free from the possibility of dirt falling in during the ramming of the risers. In making the risers it should be remembered that all liquids find their own level. Therefore, all open risers should at least be as tall as the runner bush unless the casting is to be flushed. Mold and core gas pressure can be very high when molten metal touches the sand surfaces.

High sand permeability and free venting can assist in preventing pressure build-up. However, to provide a further safety factor against these gas pressures, it is recommended that liquid pressure of the molten metal should be as high as possible so that it exceeds the maximum gas pressure at the sand face. High liquid metal pressure is achieved simply by using tall risers and runner basins.

Metal Melting and Composition

Closely associated with the problem of design is the question of metal composition. Designers, while

knowing the service required from the casting, rarely specify the physical requirements of the metal on the drawing, but simply state "cast iron"—"close-grained iron"—or "high-duty iron."

The foundry metallurgist must seek much information, ascertain the most important working surfaces and the service demanded and, with his knowledge of the characteristics of his available specifications, produce the most economical metal.

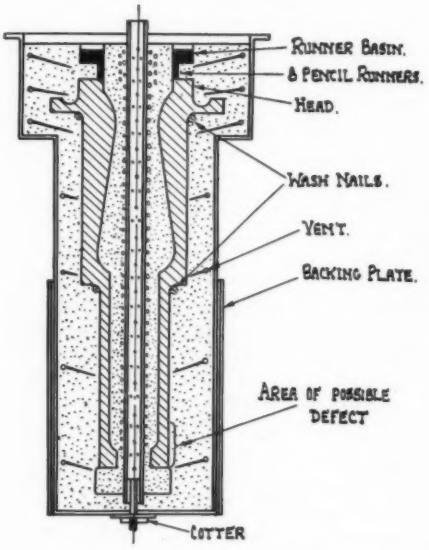
The metal specification also has a bearing on the running and risering of a mold, as each grade has different running properties, liquid shrinkages and solidification characteristics. For example, as one proceeds through the six grades quoted in the *British Standard Specification,* the running characteristics of metal decrease and the shrinkage characteristics increase.

Constant attention is paid to the melting of the correct grade of iron and pouring temperature, which is usually between 1280 and 1350 C, depending upon the design of the casting. The relationship between the composition of the iron and the temperature plays an important part in producing castings free from defects, particularly those of shrinkage and gas holes. All shrinkage cavities and gas cavities are formed during the freezing or setting period of the molten iron, and the composition and temperature determine the length of this period.

The present-day tendency for total carbon contents below 3.0 pct is not always essential to general foundry practice, and can lead to difficulties. The important part which the total carbon-silicon-phosphorus ratio plays in the solidity of casting is illustrated in Fig. 40. This is a cross-sectional diagram of a radial drill pillar mold, the casting to be machined over the complete outer surface and then ground (Fig. 40a).

It was found that, periodically, small air or gas holes appeared after machining in the bottom section of the casting, as indicated. After careful study of production methods, materials and drying processes, the defect was found to be caused by the lack of fluidity in the iron, due to out-of-balance carbon, silicon and phosphorus contents, and too low a casting temperature.

All future castings were then numbered and full information regarding composition and casting temperature plotted in relation to the soundness. The result was that a chart of the type illustrated in Fig. 41 was produced, showing the area "X" for composition and casting temperature to which all castings of this type are now manufactured with highly successful results; scrap and defective castings over the past 6 months were less than 1.0 pct.



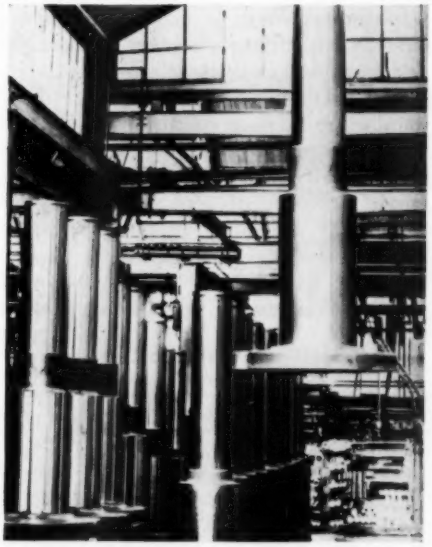


Fig. 40 (left) and Fig. 40A (right)—Cross-sectional view of mold for radial drill pillar mold. The outer surfaces of the castings are completely machined and then ground.

Metal Melting

The production of iron possessing the desired properties of temperature and composition is essential in producing sound castings, for no matter how well prepared a mold might be, faulty metal can nullify all the good work put into its preparation. In order to obtain this type of iron consistently, strict cupola control is practiced, so that many variables may be reduced to a minimum. In addition to the weighing of all charge ingredients, including air, this involves an efficient method for the segregation of the different classes of scrap.

Under the prevailing conditions of supply, no foundry can afford to reject any scrap allocated to them, with the consequent result that many grades of scrap must now be used for the compilation of charges. This scrap, which is frequently high phosphorus, high silicon, can create havoc if used for making castings where closeness of grain is essential, e.g., for high pressure work, unless it is blended with the correct grades of pig iron. It is essential, therefore, to check the analysis of each consignment of scrap immediately it is received by the foundry.

The efficiency of melting technique can be judged from the appearance and temperature of the metal as tapped, and from simple control tests[6] carried out at the cupola spout. Any deviation from the produc-

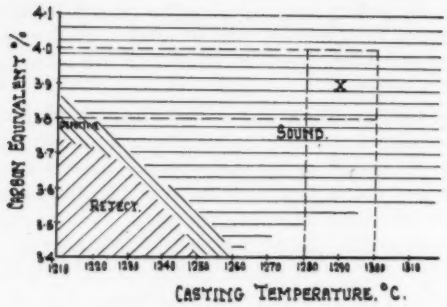


Fig. 41—Chart showing area (X) for composition and pouring temperatures to produce sound castings.

tion of good hot metal can usually be traced to some variation of the standardized procedure and rectified immediately.

After melting the iron to the correct composition, it is not sufficient to guess at the total weight tapped. A portable weighing machine is used; it is much safer to avoid short casts and prevent the melting of unnecessary quantities of metal.

The method adopted by the author's foundry to standardize melting conditions is the use of a cupola control form (Fig. 42), on which all relevant data of the daily operation is recorded, together with graph recording of wind volume and pressure, Fig. 43. This graph also records the time that each charge is placed in the furnace.

Ladles

Melting control does not end with the production of hot iron of suitable composition at the cupola spout. The metal melting section is responsible for the metal unit it is ready for pouring into the waiting molds. Many defects have been found to be produced through the use of inefficiently prepared ladles to collect molten iron. Ladles should always be clean, dry and pre-heated before use. Any damage which occurs to the ladle lips is at once repaired and the ladle thoroughly dried.

1. Dirty Ladles.

Clean metal cannot be poured from dirty, slag encrusted ladles. Slag and dross is melted by the hot metal in the ladle and is continuously rising through the liquid metal. During the pouring operation, therefore, no matter how well skimmed the ladle may be, entrained slag passes from the ladle into the pouring basin and thence into the mold. If, in addition to slag, solidified iron has been left in the ladle from the previous melt, new metal tapped into the ladle quickly loses heat to the cold solid iron. This results in dull metal being poured into the mold, with consequent defects.

Defects which have been found to occur through

Nº 2130

THE MODERN FOUNDRIES LABORATORY

No D16 149 CUPOLA CONTROL. DATE 16th MARCH 1953 SIGNATURE EB

BLAST. TIME	PRESS' H_2O	VOL. (pitot) H_2O	MELTING. TAP No.	TIME START	TIME FINISH	TEMP. °C	REMARKS. APPROX WEIGHT CWTS	Castings Made	Time Tuyeres Cleaned
			BLAST ON	2·30					
2·40	27	0·40	FIRST METAL	2·38					
3·05	29	0·45	1	3·00	3·02	1325 1400	30	GRATES	
			2	3·10	3·11	1360 1405	20	"	3·15
3·20	31	0·52	3	3·16	3·18	1375 1420	30	OD3	
3·30	30	0·50	4	3·26½	3·28	1360 1410	30	PILLAR	
3·40	30	0·51	5	3·35	3·37	1360 1415	30	OD1 PILLAR	
SLAG HOLE OPENED 3·47			6	3·45	3·46	1370 1400	25	NHB BED	3·50
4·00	31	0·55	7	3·52	3·54	1350 1390	35	4 TON LADLE	
4·10	31·5	0·55	8	4·01	4·03	1345 1395	30	NHB BED	
			9	4·10	4·12	1350 1405	30	3 TON LADLE NHB BED	4·15
4·25	31·5	0·60	10	4·20	4·21	1360 1410	20	4 TON LADLE	
			11	4·28	4·30	1360 1405	30	OD1	4·35
4·45	32	0·58	12	4·42	4·44	1360 1410	30	BASE	
5·00	31	0·59	13	4·52½	4·54	1360 1400	30		
			14	5·03	5·05	1360 1410	30		5·10
5·20	31·5	0·62	15	5·17	5·19	1360 1410	35	COLUMN	
			16	5·28	5·30	1390 1425	30	BASE	5·35
5·45	30·5	0·60	17	5·38½	5·40	1375 1420	30		
			18	5·47	5·48	1370 1405	25		
6·00	25	0·42	19	5·56	5·58	1360 1395	30	OD1 ARMS & BASES	6·00
6·20	15	0·37	20	6·10	6·12	1365 1405	30		6·20
			21	6·26	6·28	1360 1395	80		

Nº 2130 Form No. 195.

THE MODERN FOUNDRIES LABORATORY.

No D16 149 CUPOLA CONTROL. DATE 3RD MARCH 1953 SIGNATURE EB

CUPOLA No. D MELT No. 3475

TIME OF LIGHTING	10·30 A.M.
BED TIME OF MAKING	1·35 P.M.
DISTANCE from CHARGING DOOR	11' 1"
HEIGHT ABOVE TUYERES	47"
TIME FURNACE COMPLETELY CHARGED	2·25 P.M.
TIME BLAST ON	2·30 P.M.
TIME OF LAST CHARGE	5·25 P.M.

1 MIXTURE 4 CHARGES	2 MIXTURE 18 CHARGES	3 MIXTURE 14 CHARGES	4 MIXTURE 7 CHARGES	MIXTURE CHARGES
GRATES	PILLARS:- OD3:- 1300°C 45 SECS	OD2 BASE 1285°C 87 SECS	OD1 BASES 6'-0" 1290°C 68 SECS.	
	4'-6" OD1:- 1295°C (658) 35 SECS	COLUMN BASE	4'-6" 1300°C 55 SECS.	
	4'-6" O.D.1:- 1290°C (656) 38 SECS	1285°C 90 SECS		
			ARMS 6'-0" 1300°C 63 SECS.	
	N.H.B. BED:- A 4 TON LADLE 1270°C 75 SECS.		4'-6" 1300°C 54 SECS.	
	B 3 TON LADLE 1265°C 70 SECS.			

Fig. 42—Cupola control form.

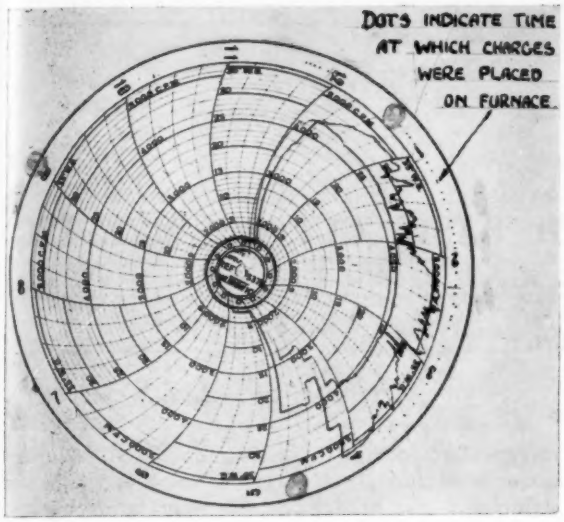


Fig. 43—Graph recording of wind volume and pressure; also, time each charge is placed.

the use of wet ladles are caused principally by the boiling action which takes place when the steam generated from the moisture finds an escape through the liquid metal. Moisture in ladle lips is more detrimental than in the lower lining since there is less ferrostatic pressure at the top of the ladle to force the steam back through the ladle casing. Also, boiling which takes place at the ladle lip is immediately transferred to the pouring basin during casting.

Steam reacts chemically with molten iron, forming an oxide of iron together with hydrogen. The result of the reaction, therefore, is to produce dross within the metal itself, and hydrogen, which is partly dissolved in the iron, to be liberated on cooling with the formation of gas holes or porous metal.

Efficiently maintained equipment, however, is not in itself a sufficient safeguard against producing defective castings. Effective control over the pouring operation is also required, as careless pouring may result in defects such as dirt inclusions, shot iron and cold laps through improperly skimmed metal, faulty positioning of ladle, interrupted pouring or insufficient basin depth.

Conclusion

Many years ago, Plato recorded that education imparts appreciation of quality, and it follows naturally that lack of education encourages the acceptance of poor quality castings. It is suggested that the industry as a whole cannot fail to benefit from a more comprehensive study of the origin of the defects occurring in castings. The adoption of a manufacturing control system similar to the one outlined in this paper, modified to suit the conditions peculiar to the individual foundry, is recommended as one method of achieving this object.

Our own experiences of the past few years have indicated conclusively that a continuous study of foundry processes results in lower cost castings of better quality, coupled with a reduction of waste of valuable materials and loss of machine shop hours.

If this paper helps in furthering the application of quality control in the foundry, by better understanding and further realization of the economic importance of the production of sound castings, the efforts of preparing the paper will have been worth while. It is further suggested that perhaps a good motto for each foundry to follow is an S.O.S. which the writer once noticed displayed in a factory:

Simplify your thinking and actions by cutting out waste at all stages of the production of castings, remembering that waste may take the form of material, labor or energy.

Organize for continuous cost reduction without sacrifice of quality by steadily improving production methods and team efficiency.

Serve the industry with quality products. Cooperate as a team with renewed vigor and determination, making your united efforts objective to improving the service rendered to your customers.

Acknowledgments

The author wishes to express his thanks to the directors of Modern Foundries Limited for permission to present this paper, and to his quality control staff for their valuable assistance.

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AMERICAN EXPERIENCES WITH BASIC CUPOLA MELTING

By

Sam F. Carter*

Through the years the cupola with a conventional lining of clay and silica refractories has been operated purely as a melting furnace with no chemical refinement expected. The resulting slag has been predominantly acid with no capacity to absorb sulfur or phosphorus from the metal. Cumulative sulfur absorption and declining raw material quality have made it increasingly difficult to meet the more rigid and more varied metallurgical expectations with the materials available and the chemical limitations of the acid cupola.

Theoretically any appreciable removal of either phosphorus or sulfur within the cupola would require a slag with the basic constituents predominant. Such a basic slag can hardly be maintained in contact with an acid lining especially since a large part of the slag comes from the fluxing away of the lining in the melting zone.

In Europe some theoretical and experimental consideration has been given a cupola lined with basic refractories, but such an operation remained chiefly an academic dream because of questionable control and refractory service. Within the last decade the British made such a basic cupola a practical reality and Renshaw reported successful production experiences. Most of these first basic cupolas were operated on special applications and/or raw material situations and the versatility was not immediately evident.

American experiences with the basic cupola have not been as long as some European experiences. Within the last two or three years basic melting has grown rapidly in the United States and operating experiences have contributed to the knowledge of the versatility as well as the practicability of the basic cupola for a number of melting applications.

Basic cupola research was started in the author's plant five years ago. The first three years of experimentation and short production trials were reported in a previous paper.[1] Production trials gave satisfactory performance on a difficult operation. A magnesite brick lining was far more resistant than clay refractories but refractory costs were much higher. Patching with rammed magnesite and dolomite mixtures then available showed promise for a more reasonable basic refractory maintenance cost. Over 100 experimental heats proved that basic slags could be made to remove either sulfur or phosphorus, but not both by the same practice with good efficiency.

Removal of Phosphorus by Oxidation

Phosphorus was removed by basic slags when made sufficiently oxidizing with low coke ratios and/or the addition of oxidizing agents. Table 1 shows some of the heats that removed 0.19 to 0.32 per cent phosphorus from low silicon charges in the range of 0.30 to 0.80 per cent phosphorus. This metal low in silicon and in an oxidized condition was readily dephosphorized another 0.10 per cent to 0.20 per cent in the forehearth ladle by means of sodium carbonate additions.

Slag analyses in Table 2 show the high iron oxide contents necessary for dephosphorization. The 1.7 per cent to 3.5 per cent P_2O_5 content in the slags account for the phosphorus removed from the metal. These heats show the chemical possibilities of a basic slag. But such an oxidizing treatment has some practical limitations. The deficient silicon and manganese contents must be restored and the resulting iron is too cold for most castings. No commercial application of this process has been made. This treatment might prove practical in a duplex melting arrangement or for pigging and remelting where high phosphorus pig iron is the most available material and a low phosphorus product is required.

Desulfurization in the Cupola

Desulfurization was accomplished most efficiently with slags of high basicity with low iron oxide contents. Such reducing conditions are more in line with the good cupola performance and high temperature metal ordinarily desired in the foundry.

Some of the experimental desulfurizing heats are shown in Table 3. With conventional fluxes sulfur contents of .040 to .080 per cent could be obtained, compared to sulfur contents of .080 to .140 per cent

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Note: This is the Official Exchange Paper from the American Foundrymen's Society to The Institute of Australian Foundrymen, Victorian Division, Melbourne, Australia.

TABLE 1—BASIC DEPHOSPHORIZING HEATS

Flux, %	Metal Charge	Amount Coke, %	Phosphorus Charge	Phosphorus Final	Silicon Charge	Silicon Final	Manganese Charge	Manganese Final	Carbon Charge	Carbon Final	Sulfur Final	Temp. Max.
10 Lst, 3 Fe Ore, 1 Sp.	Remelt	14	0.65	0.33	0.48	0.17	0.22	0.13	3.64	3.55	0.091	2530
10 Lst, 3 Fe Ore, 1 Sp.	Remelt	14	0.33	0.14	0.17	0.12	0.13	0.05	3.55	3.24	0.097	2540
10 Lst, 3 Mn Ore, 1 Sp.	100% Pig	14	0.79	0.58	0.75	0.23	0.24	0.39	4.13	3.79	0.036	2640
8 Lst, 4 Purite	70 Pig, 30 Steel	12	0.57	0.30	0.56	0.10	0.31	0.12	2.95	2.80	0.081	2550
6 Lst, 3 Mn Ore, 4 Pur. 2 Sp.	100% Pig	12	0.79	0.49	0.75	0.37	0.24	0.24	4.13	3.65	0.052	2570

Flux expressed as per cent of metal charge. Lst. = limestone, Purite = fused sodium carbonate, Sp = Fluorspar.
All analyses represent average of 3 to 7 samples.
Maximum temperature is °F as determined by optical pyrometer standardized with thermocouple.
Maximum level is average of three highest adjacent readings in middle of heat.

TABLE 2—SLAG COMPOSITION—BASIC DEPHOSPHORIZING HEATS

Flux, %	Metal Phos. Charge	Metal Phos. Final	Slag Composition, % SiO_2	Al_2O_3	Fe_2O_3	FeO	MnO	CaO	MgO	CaF_2	S	P_2O_5
10 Lst, 1 Sp, 3 Fe Ore	0.74	0.65	28.4	9.3	7.6	19.5	2.0	21.6	——	1.8	0.02	2.5
10 Lst, 1 Sp, 3 Fe Ore	0.74	0.65	16.0	3.5	9.1	32.2	1.5	20.4	14.4	2.5	0.14	2.9
10 Lst, 1 Sp, 3 Fe Ore	0.65	0.33	20.3	6.2	9.4	23.0	1.3	28.3	9.2	2.0	0.08	2.9
10 Lst, 1 Sp	0.74	0.55	31.3	7.1	4.2	4.6	2.7	39.7	5.2	2.4	0.11	3.5
10 Lst, 1 Sp	0.41	0.20	14.3	4.4	18.6	19.8	0.9	35.5	11.0	1.0	0.11	1.7

TABLE 3—BASIC DESULFURIZING HEATS
(Experimental 23-in. Cupola)

Metal Charge	Flux Per cent	Coke %	Coke Type	Final % Si	Final % Mn	Carbon % Charge	Carbon % Final	Sulfur % Charge	Sulfur % Final	Temp. F, Max.
Remelt, 0.08% S	2% Limestone	12½	A	2.29	0.79	3.43	3.45	0.077	0.073	2660
Remelt, 0.08% S	10 Lst, 2 Spar	12½	A	2.25	0.76	3.39	3.72	0.077	0.066	2600
Remelt, 0.12% S	3 CaC_2, 5 Lst, 3 Sp.	14	A	2.31	0.89	2.48	3.62	0.119	0.031	2780
Remelt, 0.13% S	6 CaC_2, 5 Lst, 3 Sp.	14	A	1.85	0.72	2.30	3.83	0.134	0.016	2815
Remelt, 0.29% S	5 CaC_2, 5 Lst, 2 Sp.	14	A	1.79	0.81	2.20	2.89	0.287	0.100	2740
70% Steel	6 CaC_2, 5 Lst, 2 Sp.	17	D	2.13	0.74	1.20	3.30	0.060	0.032	2900
100% Steel	10 Lst, 1 Spar	16	A	2.34	0.67	0.20	2.48	0.050	0.081	2805
100% Steel	3 CaC_2, 5 Lst, 3 Sp.	16	A	2.41	0.89	0.20	3.50	0.050	0.026	2950
100% Pig Iron	7 Lst, 1 Spar	15	A	2.34	0.15	4.20	3.80	0.030	0.022	2700
100% Pig Iron	6 CaC_2, 5 Lst, 3 Sp.	14	A	1.98	0.14	4.20	4.11	0.030	0.009	2780

Coke A is a regular foundry coke with 0.75% S. Coke D is 2.0% S, 14% ash, 5% volatile.

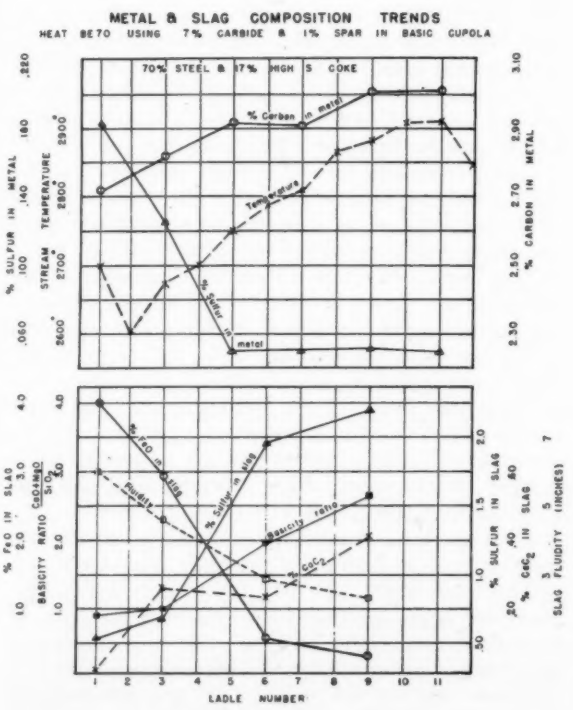


Fig. 1—Slag-metal relationships in experimental basic heat.[1]

expected in acid melting from similar materials. In the course of our experimentation calcium carbide was discovered to be a powerful aid to desulfurization when charged directly into the cupola as a flux. With the help of carbide the basic slag removed as much as 0.187 per cent sulfur from one charge of 0.287 per cent sulfur scrap. A 0.134 per cent sulfur charge was reduced to 0.016 per cent sulfur. Charges of 100 per cent steel were melted with low sulfur and the carbon pick-up increased enough to give a gray iron carbon content. A coke with 2 per cent sulfur and 14 per cent ash, ordinarily considered unfit for foundry use, was made to produce a 0.032 per cent sulfur iron. Charges of 100 per cent pig iron for nodular or ductile iron have been made to produce sulfurs as low as 0.005 per cent directly from the cupola.

Figure 1 shows graphically the effect of slag composition on metal chemistry. On one heat, due to the delayed action of the carbide, the slag chemistry changed drastically near the middle of the heat. Basicity increased and slag FeO decreased sharply. These changes produced a sulfur increase in the slag and a sharp decrease in the sulfur of the metal. Tempera-

ture and carbon content were increased by the same reaction. Whereas the acid slag had always been considered an incidental nuisance, the basic slag was shown capable of accomplishing desirable chemical refinement. On the basis of the desulfurization accomplished and the increased carbon pickup several varied applications were suggested in which basic melting might find a place either in improved iron quality or in relieving raw material problems through successful use of more scrap, or steel, and lower quality coke. The basic refinement made possible an iron chemistry suitable for the newly developed nodular or ductile iron.

Production Experience—Refractory Maintenance

For the last two years the writer's plant has been operating one of the smaller production cupolas with a basic lining.[2] This cupola is a 36-in. I.D. furnace melting 4 tons per hr for 5- to 8-hr shifts. The output is predominantly a gray iron of 0.20 per cent phosphorus content. A number of occasional special irons have been melted in this cupola. After a few early troubles the operation has been satisfactory. Practically all the lower phosphorus irons have been melted with an economic advantage and some of the special irons have been produced with considerable economic and/or metallurgical advantages.

Figure 2 shows details of the basic lining used in the writer's cupola. The left side shows the original magnesite brick lining and the right side the lining as it is presently maintained with a monolithic dolomite patch blown with a pneumatic gun. Obviously basic refractories are necessary only in the portions of the lining that directly contact the slag or contribute heavily to the slag composition.

Before arriving at the writer's current procedure for daily patching, we investigated five fundamentally different combinations and eight different basic materials. The author's earlier basic cupolas were maintained primarily with rather expensive magnesite brick which were available for higher temperature steel furnaces. Some patching with rammed magnesite and rammed dolomite showed considerable economic promise. With the introduction of the pneumatic guns for cupola patching, several of the granular basic materials produced for steel furnace hearths were successfully blown. Slight adjustments were sometimes made on the commercial mixtures, either in the bond or size distribution, to facilitate the application with a particular gun. A comparative study was made of daily attack and refractory maintenance costs. Results are summarized in Table 4.

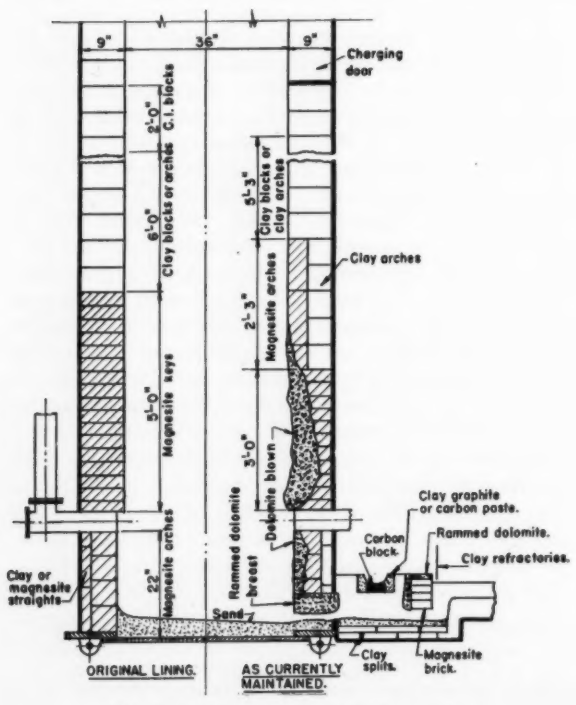


Fig. 2—Lining details of basic cupola.

TABLE 4—COMPARATIVE REFRACTORY PERFORMANCE AND PATCHING COSTS
(25-ton heats—36-in. cupola)

Patch Material	No. Days Averaged	Max. attack[1] per side	Patch cost per ton[2]
Acid:			
Clay brick	(18)	6.0 in.	$1.51
Ganister-clay gunned	(4)	4.8 in.	1.10
Basic:			
Magnesite brick	(18)	2.4 in.	3.80
Magnesite rammed	(3)	3.5 in.	——
Magnesite gunned	(4)	3.8 in.	2.68
Dolomite rammed	(4)	4.0 in.	——
Dolomite gunned	(30)	4.2 in.	1.85

[1] Difference in lining before and after heat at point of maximum attack.
[2] Both material and labor costs of patching back to original dimensions.

All the basic materials were equal to or better in performance than clay refractories, but considerably higher in cost. Patching with magnesite brick gave the least attack but the highest cost. Although refractoriness was slightly less, the blown dolomite preparation has given us the best overall cost. On this particular cupola the dolomite patching cost was $0.75 per ton of melt higher than acid patching with a blown ganister mixture. This was a 68 per cent increase in refractory cost. Economic justification for a basic lining can be expected only when this increased refractory cost can be overcome by greater savings in the metal charge or improved casting quality.

This cupola is equiped with water cooling by external sprays. As expected the water cooling does not appear to retard refractory consumption until the lining has been burned very thin. However, operations for a few days were successful with essentially no refractory lining in the melting zone. One of the author's acid cupolas is operated regularly with external water cooling and practically no lining. With further development water cooling seems to offer more relative advantages on a basic furnace.

Some special consideration must be given the slag hole refractories because of the more corrosive nature of the basic slags. The basic cupola in the writer's plant has been operated both as a back slagging, intermittent tap cupola, and currently as a continuous tap, front slagging cupola. In back slagging, the rear slag hole received the abuse. Clay-graphite refractor-

ies were sufficient for only short heats of 2 or 3 hr. Slag holes rammed from dolomite and magnesite, when properly bonded and densely rammed, have lasted satisfactorily for a one shift operation. Silicon carbide blocks have lasted generally one and two days. Graphite and carbon slag hole blocks proved completely resistant to slag attack and slag adherence but were gradually burned larger by escaping air when left open too much.

In front slagging the special refractories are needed in the tap hole, the slag skimmer knife and the runner box wherever contacted by the slag. Tap holes rammed from both dolomite and magnesite have performed with consistent satisfaction for a one shift operation. Frequently these have been used two or three days with essentially no patching. High alumina brick (98% + Al_2O_3) have shown good resistance, but at a higher cost than other materials. Our runner box and slag knife are made from magnesite brick with a 1-in. inner lining of rammed dolomite. The daily attack is confined to this dolomite layer which is patched after each day's operation. The slag overflow and run-off trough are lined with thin graphite or carbon blocks with the sides formed from carbon paste or clay-graphite refractories. Carbon blocks and pastes proved completely resistant to slag attack and adherence, but are attacked slowly by hypo-eutectic iron if extended below the slag level.

Flexibility by Varying Slag Basicity

Experience at the author's plant has indicated that a cupola with a basic lining may be operated at several distinct levels of slag basicity dependent on the quantity of basic flux used. For that reason experiences from various cupolas cannot be properly compared without consideration of slag basicity level. Larger slag volumes and slags of higher basicity remove more sulfur from the metal. The more basic slags also wash away the acid ash from the coke more effectively, producing a higher carbon pickup and more reactive combustion. The additional use of small percentages of calcium carbide by virtue of its exothermic and strongly reducing effect has enabled us to make the slag still more basic and lower in FeO, the sulfur extremely low, and the carbon pickup still greater.

On the low basicity extreme our basic furnace has been operated with no flux addition. The basic refractory consumed from the lining more than neutralized the acid constituents from the coke ash and silicon oxidation so that the resulting slag was still slightly more basic than an acid slag. The charge was little different from an acid mix and the sulfur was only slightly lower. Progressive increases in flux quantity produced increased slag basicity and final sulfurs were decreased progressively from .075 per cent to .030 per cent. To compensate for the increased carbon pickup the proportion of steel on the mix was increased from 35 per cent to 80 per cent to produce the same carbon content averaging 3.40 per cent. Table 5 shows five different basic mixes along with an acid mix that we have used to obtain iron of the same chemistry, except for sulfur.

TABLE 5—BASIC MIXES WITH VARYING SLAG BASICITY

(Producing same final chemistry: Si 2.00% C 3.40% P 0.20%)

Flux Charge	Proportion of Steel, %	Silicon Loss, %	Carbon Pickup, %	Sulfur Final, %
Acid —4% Limestone	30	15	.45	.090
Basic—No Flux	35	20	.65	.075
1% Limestone	40	24	.85	.070
2% Limestone	50	27	1.25	.060
4% Limestone—½% Sp.	60	30	1.60	.050
1% CaC_2, 4% Lst, 1% Sp.	80	30	2.40	.030

Rounded averages of several days operation based on average raw material conditions.
Silicon loss is per cent of overall silicon addition with .25% silicon inoculation added to ladle as FeSi.
Carbon pickup is per cent carbon content above calculated carbon content of charge.

In the writer's raw material situation and on lower phosphorus iron the basic mix has proved a net economic advantage by virtue of the increased proportion of steel scrap. The 60 per cent steel mix affected a net saving of over $2.00 per ton. The 80 per cent steel mix with carbide produced a further saving but required closer supervision to maintain good control.

As indicated in Table 5 silicon loss is much higher in basic melting and must be considered chemically and economically. No manganese has been required on the author's basic operation. Better manganese recovery with a basic slag has produced manganese contents of 0.45 to 0.70 per cent which are sufficient to neutralize the lower sulfur contents of .030 per cent to .070 per cent.

Nodular and Special Irons

The reactions under a highly basic slag have produced chemical accomplishments beyond the limitations of the acid cupola, some of which have desir-

TABLE 6—SPECIAL IRONS MADE IN BASIC CUPOLA

(Typical Short Heats—36-in. Production Cupola)

Type Iron	Metal Charge	Flux Charge	Metal Chemistry S	Si	C	Alloys
1. Nodular Iron	50% Steel 50% Low P Pig	4% CaC_2, 4% Lst, 2% Spar	.010	2.07	3.90	Mg
2. Permanent Molds	30% Steel 70% Pig	6% Limestone 2% Spar	.040	1.79	3.80	Cr
3. Forging "Die Pots" (A)	100% Alloy return	4% Limestone ½% Spar	.061	1.10	3.97	Ni, Mo, Cr
(B)	100% Alloy return	2% Limestone	.073	1.22	2.70	Ni, Mo, Cr
4. Ni-Resist	100% Ni-R return	6% Limestone 1% Spar	.082	2.18	2.84	Ni, Cu, Cr
5. High Strength	90% Steel	1% Limestone	.082	1.67	2.87	

able metallurgical applications. Table 6 shows some of the occasional special irons made advantageously in the writer's basic cupola.

The first special iron described is a typical heat of nodular iron. Irons of 0.010 per cent sulfur and 3.90 per cent carbon were obtained with a flux charge of 4 per cent calcium carbide, 4 per cent limestone, and 2 per cent fluorspar. This basic iron required less than half the magnesium alloy addition usually needed for acid cupola iron. From the reduced alloy consumption and savings on the base metal charge, the basic cupola has shown us potential net savings of $10.00 to $12.00 per ton on nodular iron. From the economic and metallurgical standpoint the basic cupola seems to offer the most definite advantages for nodular iron.

The second iron in Table 6 is a permanent mold application in which a very high carbon iron is desired for heat shock resistance. The basic cupola made it possible to obtain 3.80 per cent and higher carbons which could not be obtained readily in our acid cupola. Furthermore, the use of 30 per cent steel scrap substituting for low phosphorus pig iron produced a net saving of $4.00 per ton where low phosphorus content was desired.

The third iron for forging dies is another high carbon heat shock application, alloyed with nickel, molybdenum, and chromium. In acid melting no more than 60 per cent return could ever be used, and then a sulfur of about .120 per cent required ladle desulfurization and carbon content was not as high as desired. With the basic cupola it was possible to melt 100 per cent alloy return (used dies from the customer plus shop return). Sulfur was sufficiently low, and carbon content as high as desired. The use of this scrap, with no primary alloys required, affected a net saving of $18.00 per ton on this particular iron.

The fourth example in Table 6 is a high nickel iron on which a charge of 100 per cent alloy scrap, home and purchased, was successfully melted. The sulfur was reduced from .120 per cent in the charge to a final of .082 per cent and carbon content was higher than can normally be attained. The basic cupola has shown possibilities in the more efficient utilization and conservation of critical alloys where alloy irons are made.

The last iron in Table 6 is a low carbon, high strength iron with a 48,500 psi tensile from a 2-in. bar. The basic cupola seems to offer no particular advantage on this type of iron and the low carbon is more difficult to obtain because of the increased pickup in the basic cupola. However, by reducing the flux and producing less basic slags, low carbon irons have been produced with fair success in the basic cupola. Very low sulfurs are not obtained, but low sulfur contents seem to be of less advantage on these low carbon irons cast into heavy sections. As far as we know all representative compositions of cast iron have been melted in a basic cupola, some with more ease and some with more difficulty than in an acid cupola.

These examples typify some of the various applications on which the cost of basic refractories have been more than offset by savings in the charge makeup. On the other hand, basic operation cannot be justified in many applications. Where high phosphorus pig iron can be used and a sufficient supply is locally available at low cost, and when coke quality is sufficiently good to produce no problems in acid melting, the author found no advantage in basic melting. On a high silicon, low carbon iron we found the basic cupola at an economic disadvantage. The cost of the higher silicon loss plus the increased refractory cost was greater than savings from the elimination of forehearth desulfurization.

Slag Composition and Appearance

Since the accomplishments of the basic cupola result from slag chemistry, much consideration must be given slag composition and its control.

Slag analyses, which require some experience to run accurately, do not have to be made regularly to take advantage of basic performance. However, slag composition studies have given direction to much of the past research on basic melting and will continue to be useful in developing a better understanding and more efficient utilization of basic performance.

Certain general relationships have been found to apply with reasonable dependability. Occasional apparent exceptions remind us of the need for more research on slag-metal relationships and possibly sampling and analytical methods. As the slag basicity increases, the sulfur absorbed in the slag goes up and consequently sulfur in the metal goes down. The desulfurization capacity of the slag increases markedly when slag FeO is reduced under 1.0 per cent. Sulfur removal may be increased by melting larger volumes of slag in contact with the metal. However, equal and better desulfurization has been accomplished with smaller slag volumes of more effective composition, accomplished by reduction of iron oxide content.

Table 7 shows some typical slag compositions representative of several degrees of basicity. Until more accurate relationships have been determined the author used the ratio $\frac{CaO + MgO}{SiO_2}$ as a simplified expression of basicity. Al_2O_3 has not appeared to have much effect on desulfurization and has been left out of the ratio. While MgO is probably not as effective as CaO when MgO is very high, the difference seems slight when MgO is the minor basic constituent.

For a simplified classification we have arbitrarily grouped basic slags in three ranges; mild basicity with basicity ratios 1.0 to 2.0, moderate or medium basicity with ratios 2.0 to 3.0, and high basicity with ratios above 3.0.

Reasonably good slag control can be accomplished by observing the color and luster of slag pancake samples taken at intervals during operation. Slag samples are collected at the author's plant in a shallow metal dish on a handle projected under the continuous slag stream.

The mildly basic slags are generally dull in luster and black in color, tending toward brown as basicity increases and FeO decreases. Should the FeO go high and the basicity ratio drop below 1.0, the black slag assumes a glassy luster characteristic of acid slags.

TABLE 7—TYPICAL BASIC CUPOLA SLAGS WITH VARYING DEGREES OF BASICITY

Flux Charge	Metal Sulfur, %	Slag Composition, % SiO_2	Al_2O_3	FeO (2)	MnO	CaO (3)	MgO	`S	Basicity Ratio (1)	Slag Appearance
Mild Basicity										
1. 5% Lst, 1% Sp, 1st Ladle	.079	36.5	11.5	13.4	2.6	26.1	5.7	0.04	0.87	Glassy, Black
2. No Flux	.074	37.5	12.8	3.1	1.8	23.2	20.1	0.28	1.15	Resinous, Dark black
3. 1% Limestone	.061	34.3	9.4	1.7	1.7	30.5	22.0	0.39	1.53	Dull, black
4. 2% Limestone	.064	35.1	7.8	2.2	1.7	35.0	16.8	0.46	1.47	Dull, Brownish black
Medium Basicity										
5. 4% Lst, ½% Spar	.047	28.6	6.5	1.8	1.2	41.2	19.6	0.56	2.12	Dull, brownish gray
6. 4% Lst, Hot Blast 450°F	.030	26.0	9.2	2.9	0.6	32.5	28.6	1.26	2.35	Dull, dark gray
7. 1% CaC_2, 4% Lst, 1% Sp.	.032	25.8	6.5	2.0	0.6	40.3	23.2	3.08	2.46	Dull, brownish gray
High Basicity										
8. 4% CaC_2, 4% Lst, 2% Sp.	.010	18.8	12.5	0.4	0.6	62.4	9.0	2.50	3.80	Powder, grayish white
9. 5% CaC_2, 5% Lst, 3% Sp.	.005	15.8	15.2	0.2	0.1	59.3	8.6	——	4.29	Powder, grayish white
100% Scrap Charges with Increasing Basicity										
10. 3% Limestone	.069	35.4	10.1	1.9	1.9	34.1	16.2	0.46	1.42	Dull, brownish black
11. 4% Lst, ½% Spar	.051	32.4	11.7	1.2	0.6	39.0	15.1	0.62	1.67	Dull, medium brown
12. 6% Lst, 2% Spar	.022	30.6	7.8	0.7	0.8	42.3	15.2	0.70	1.88	Dull, light brown

(1) Basicity ratio $\frac{CaO + MgO}{SiO_2}$

(2) Total Fe_2O_3 and FeO determined as FeO.

(3) Total calcium determined as CaO.

(4) Total sulfur determined by gravimetric precipitation as $BaSO_4$.

With such a slag most of the effects of basic melting are lost and a limestone increase is necessary to restore basic behavior. Such a slag is often obtained on the first ladle from a fresh coke bed but becomes more basic immediately.

In the medium basicity range the slags are dark gray or brownish gray. In the high basicity, low FeO ranges the slags become lighter gray to white. In the highest CaO/FeO ratios the slags disintegrate upon cooling into a white powder. Some calcium carbide content may be determined in the analyses and some odor of acetylene detected. Slags of such composition have been found to contain as much as 1.0 per cent to 3.0 per cent sulfur, that has been absorbed from the metal.

In order to insure reaction of slag and metal with consistency, slag volume and period of contact require some degree of uniformity in basic melting. In front slagging, proper dam levels keep the metal level, slag level, and period of contact essentially consistent. In intermittent tapping it is more important in basic melting that some regularity of tapping and "slagging off" be established.

General Operation and Control

Inside the cupola the basic slag is more fluid and maintains a clean, open cupola, with brighter tuyeres, less bridging, and less frequent troubles resulting from poor stack permeability and oxidizing channels. On comparable types of charges and coke ratios the basic cupola seems to melt slightly faster and hotter and with better thermal consistency. These effects seem to result from cleaner, more reactive coke, resulting from the ash cleaning ability of the basic slag. Because of better combustion, coke reductions can be made with resultant increases in output and no sacrifice of temperature, if chemical objectives are not defeated. The operating advantages of basic melting seem to increase with poorer coke and/or dirtier charges.

With calcium carbide additions to the charge, further increases in basicity, desulfurization, carbon pickup, and temperature have been produced.

The effect of preheated blast is much greater on the basic cupola than on the acid furnace. A few days operations on 450 F blast, from an adjacent cupola, indicated to us that operation was made potentially more basic and chemically more effective by increased blast temperature.

The highly basic slag chills more abruptly in air and generally requires more attention to the slag runoff, especially on the smaller cupolas where the volume is low. The mildly basic slags are very fluid and give practically no elimination trouble.

From the standpoint of chemical control the basic cupola is generally more complex and requires more careful supervision. Its complexity increases with slag basicity. The problems of sulfur control and sufficient Mn/S balance are practically eliminated. Where high carbons are desired, carbon control is easier; where medium carbons are desired carbon control seems potentially equal with sufficient experience. On low carbon iron basic control seems more difficult.

Silicon consistency can be made to approach acid control, but is inclined to be more difficult to control because of the greater loss. Both the use of large proportions of irregular cast scrap and/or the use of much steel scrap with silicon added in concentrated form increase the risk of chemical variations irrespective of basicity. A high proportion of pig iron is desirable from the control standpoint because of its chemical and physical consistency. The author's experience has been that most of these irregularities from scrap and steel are sufficiently "smoothed out" with a receiving ladle of adequate capacity.

Since iron chemistry in the basic cupola is largely dependent on slag chemistry, factors effecting slag chemistry require better control. The flux must be calculated, weighed, and charged with as much accuracy as any ingredient. Consistent refractory practice, uniform charging, and controlled quantities of

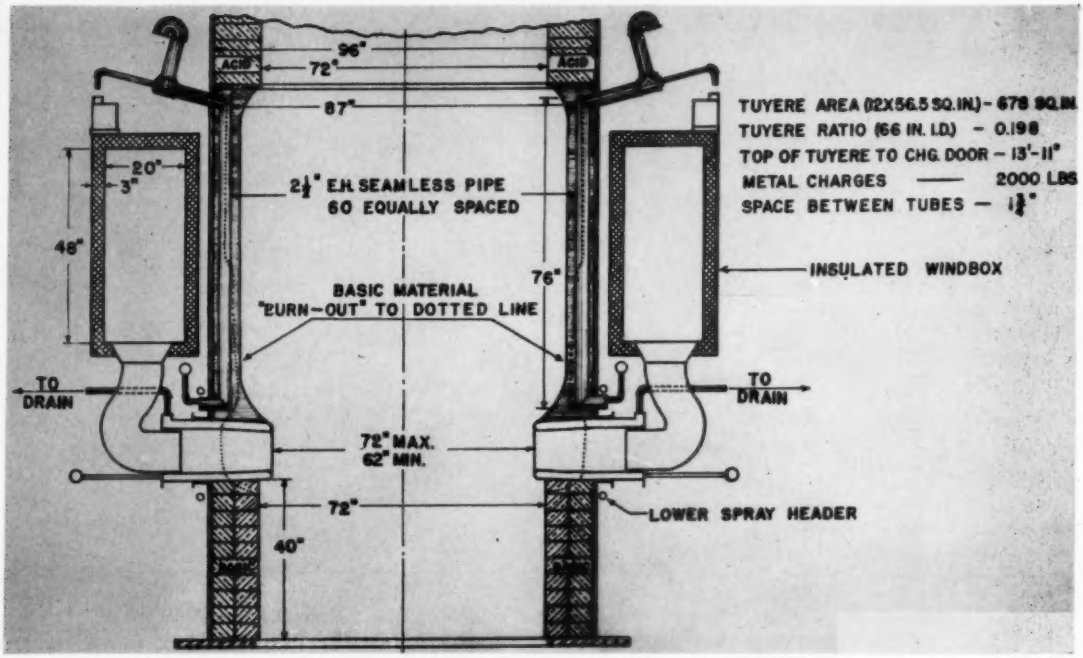


Fig. 3—Sketch of basic-lined, water-cooled, hot-blast cupola, first revision.

dirt and sand on the scrap are necessary for consistent slag control, because of their indirect influences on slag chemistry. With some experience, flux changes can be made to compensate for changes in the physical condition and cleanliness of the metal charge. Sand on uncleaned foundry return requires additional basic flux and can be troublesome in a basic cupola.

From the standpoint of mechanical properties, tests at the author's plant have shown basic iron to be influenced predominantly by the usual relations to chemistry, and especially carbon content. Averages of routine tests in the author's plant[3] have shown basic iron always equal and generally slightly better than acid iron of comparable chemistry (with the exception of sulfur.) Basic irons seem slightly less inclined toward chill, causing a tendency for us to average lower carbon, higher strength chemistry at equivalent chill depths. Some physical superiority seems more clearly indicated in small diameter test bars and thin sections. Basic melting seems chemically and physically more suitable for high carbon iron and thin section castings with doubtful advantages on low carbon iron and heavy gray iron castings.

Extent of Basic Cupola Melting in America

Within the last two or three years over 60 companies have attempted some basic cupola melting. Approximately half of this number have been on a temporary trial basis, while the other half are in regular production.

About 15 or 20 basic cupolas are operated primarily for quality gray iron within controlled strength levels. The greater proportion are in the size range of 36 to 54-in. I.D. melting 4 to 10 tons per hour with a slag of mild basicity and final sulfurs of .050 to .090 per cent. Some have used larger proportions of steel and scrap. Others have used the basic cupola for relief from the high sulfurs and casting troubles resulting from the necessary recycling of a large proportion of shop return. With essentially the same charges, sulfurs of .120 to .180 per cent from acid melting have been reduced to .050 to .090 per cent with basic melting. Coke reductions as high as 15 per cent have been made with equal temperature, and increased output. Most of the basic operators experience cleaner cupolas and better thermal consistency.

Descriptions of American basic refractories and refractory practices have been contributed by Holt[4] and by Demler.[5]

Although some of the above basic furnaces are used for occasional nodular heats several basic cupolas are used predominantly for nodular iron. Flinn and Kraft[6] described an operation typical of this nodular application. A 32-in. cupola is used, melting 3½ tons per hour. A charge of 37 per cent steel, 37 per cent return, and 25 per cent pig iron is melted with 16 per cent coke and a flux of 5 per cent limestone, 1½ per cent carbide and 2½ per cent spar. The resulting iron of 3.50 to 3.80 per cent carbon and .018 to .030 per cent sulfur is suitable for nodular treatment and requires less magnesium addition.

Water-Cooled Basic Cupolas

Levi has reported on an interesting cupola at the Radford, Virginia plant of the Lynchburg Foundry Company.[7] This water-cooled, hot-blast cupola with a basic slag incorporates several progressive developments in cupola operation and construction, and applies several of the advantages of basic operation.

This cupola is water cooled through sixty 2½-in. vertical pipes in the melting zone and projecting

water-cooled copper tuyeres, as shown in Fig. 3. The blast is preheated to 900 to 1000 F by means of an external oil-fired heater.

The blast heated to such a temperature makes the furnace more powerfully reducing and accentuates the reactivity of the basic slag. The reactivity of such a highly basic slag produces very high carbons and very low sulfurs from charges of 70 per cent steel and 30 per cent cast scrap with no pig iron or from charges of 100 per cent steel scrap. With total flux proportions varied from 4 per cent to 10 per cent, sulfurs within the range of .010 per cent to .060 per cent and carbons of 3.70 to 4.30 per cent have been generally obtained. Data on a typical heat is given in Table 8. The tap hole is made from a combination of magnesite and carbon brick and is capable of lasting for several days operation.

The water cooling has made possible continuous operation on this cupola for over 85 hours, and the furnace has been banked for as long as 24 hours and melting resumed.

In this plant the basic cupola has been used for several of the objectives that have been associated with basic operation. In an area where steel and cast scrap are generally more available than pig iron, the basic cupola can be operated with large proportions of steel and cast scrap. For the manufacture of gray iron pipe the basic cupola iron is run into a common forehearth along with the output of an acid cupola, giving an iron mixture lower in sulfur and higher in carbon than could be maintained normally from two acid cupolas. For some high carbon, low silicon gray iron castings the iron is used directly from the basic cupola. Some nodular or ductile iron is made conveniently by treating the basic iron. These activities fill most of the 24-hour day. For the remainder the basic iron is frequently cast into pigs with a casting machine. The cast pigs are used in smaller cupolas at an adjacent plant as a substitute for low phosphorus pig iron. The basic cupola has contributed to the melting flexibility of this plant through these several applications.

At another plant Carrington[8] has been operating a water-cooled basic cupola with the cooling accomplished with external water sprays on the shell. The primary objective has been flexibility to maintain production of gray iron pipe with available materials. This plant frequently has not been able to obtain more than one-fourth to one-half the necessary quantity of pig iron needed for a high carbon content and a sulfur content below a specified maximum. Cold blast is used and a slag of only moderate basicity. The entire production is melted in this one basic cupola (one of a pair alternated daily) of 72 in. I.D. melting approximately 20 tons per hour, continuously tapped with front slagging.

The basic reactions have enabled this plant to melt charges as high as 50 per cent cast scrap and 25 per cent steel and produce carbons of 3.50 per cent to 3.70 per cent and sulfurs of .060 per cent to .090 per cent with approximately 10 per cent flux. Routine slag samples are observed along with the chill test. Rapid analyses of slag sulfurs seem most rapidly indicative of operating conditions (when coke and charge materials are maintained fairly constant). A carbon plastic has been used successfully to resist the basic slag in the basin, tap hole, and trough.

Summary

Experiences at these American plants have indicated that basic cupolas can be operated satisfactorily for foundry production. Several basic refractories and methods of application have been successfully used. Basic refractory costs may be expected to be almost twice as high as acid costs. Further application of water cooling might improve the refractory cost. Due to some added refractory cost and the increased silicon loss, basic operation cannot be justified where sufficient materials of good quality are available to melt acid iron with no serious quality problems. However, the added basic costs have been easily offset in a number of different applications, either by improved metal chemistry or use of cheaper or more

TABLE 8—BASIC CUPOLA HEAT NO. 10351, FROM LEVI[7]

4th-8th Hours of Operation, Inclusive

Cupola Charge

Steel Scrap	1400 lb
Cast Scrap	600 lb
Silvery—15% Si	50 lb
Briquets @ 2 lb Si	1 pc

Flux Charge

Dolomite	150 lb
Calcium Carbide	10 lb
Fluorspar	25 lb

***Iron**

Si	S	T.C.	Temp. F
0.82	0.033	4.15	2792
0.93	0.031	3.88	2827
1.15	0.032	4.08	2775
0.95	0.039	3.98	2810
1.22	0.035	4.07	2775
0.98	0.020	4.25	2775
0.96	0.027	4.14	2792
1.13	0.028	4.05	2810
1.01	0.046	3.89	2792
0.95	0.029	4.08	2792
Avg. 1.01	0.032	4.06	2794

Analysis Cupola Slag, %

FeO	CaO	SiO_2	MgO	Al_2O_3	S
0.37	43.60	27.70	15.12	7.88	1.45
0.35					
0.35					
0.44					
0.73	42.00	27.24	11.43	7.44	1.47
0.45	42.80	27.47	13.28	7.66	1.46

Silicon in charge, %	1.14
Aver. Silicon Loss, %	11.4
Air Blast Temp.	950 F
Carbon in charge, %	1.26
Aver. Carbon pick-up, %	2.80
Coke Ratio	5.3:1

* Note: Iron samples from cupola spout every 30 minutes.

available raw materials.

With increasing experience chemical control of carbon and silicon is becoming almost as good as acid practice. Sulfur control is much better. More careful supervision is required on flux additions, refractory practice, and charge condition as they effect slag composition. Some attention must be given slag composition control and metal-slag contact.

By control of flux additions and slag condition a basic cupola may be operated at any of several levels of basicity. A mildly basic slag may be used with less complication where the primary objective is simply a sulfur content not excessively high for normal gray iron.

Slags of higher basicity may be utilized to obtain lower sulfur, higher carbon irons with quality advantages for some castings. Another application is the use of larger proportions of cast scrap, steel, and/or poor coke with economic or availability advantages. Basic melted iron proved suitable for nodular iron as well as many alloy and special iron applications. From an overall conservation viewpoint operation of a proportion of basic cupolas along with acid cupolas should enable the foundry industry to better utilize our poorer grade materials along with our diminishing select materials, to conserve manganese, and to reclaim scarce alloys from scrap. From the standpoint of metallurgical quality and flexibility, the foundry no longer needs to compromise with the traditional chemical limitations of the acid cupola. By choice of slag composition almost any desired chemistry can be obtained from a wide variety of metal sources.

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FACTORS WHICH DETERMINE RISER ADEQUACY AND FEEDING RANGE

By

William S. Pellini*

ABSTRACT

Results of a series of fundamental studies related to solidification of castings and to riser action are summarized and reduced to generalized concepts. It is demonstrated that metal and mold factors co-determine the general mechanism of solidification and that differences in the conditions of solidification at various positions of a given casting or riser relate only to rates of solidification; the heat transfer characteristics determined by the ratio of volume to surface area establish the specific rates at which the solidification process proceeds. Risering requirements with respect to adequacy are demonstrated to be primarily determined by the solidification time of the casting, while the requirements with respect to feeding range are shown to be determined by the geometry characteristics of the casting. Quantitative data of riser feeding distances for steel castings of various simple shapes are presented.

Introduction

Foundrymen have considered the advancement of the "art" of risering to a science as a highly desirable goal, the probability of its ultimate accomplishment is viewed, however, with considerable skepticism. Such a pessimistic view of the complexity of the risering problem was well founded as long as the true nature of the solidification of castings remained virtually unknown. In view of advancements which have been made in the knowledge of solidification such pessimism is no longer warranted.

It is not to be implied that risering can, as of now, be based entirely on scientific principles, but that basic information is available and it is only a question of time and rate of research effort before the goal is reached. In fact, significant developments have already been made and utilized on production scale.[1,2,3,4,5,6]

These developments relate to the use of calculation methods for determining the proper size and spacing of risers for castings of moderately complex shapes. Caine[4] reported to the Brussels International Conference on the use of methods which he evolved for riser dimensioning and reviewed the advancements in the development of scientific procedures for spacing.

It is most essential for a proper approach to risering problems that it be recognized that risers perform two distinct functions. Risers serve not only to furnish the liquid metal necessary to compensate for the liquid to solid shrinkage but also in a limited degree to promote proper patterns of directional solidification.

Riser size is related to the first function while riser positioning, in certain cases, is related primarily to the second. If the riser size is inadequate, gross shrinkage will extend into the casting proper (under-riser shrinkage); excessive spacing distances will result in dispersed shrinkage (centerline shrinkage). Failure to recognize the two basic functions of risers and their relation to shrinkage defects has confused the problem of risering and exaggerated its complexity.

The requirements of riser size and spacing are basically related to the conditions of solidification. It is essential, therefore, that the solidification characteristics of castings and risers be understood, i.e., the development of a broad knowledge of solidification must necessarily precede the development of truly scientific procedures for risering. The development of empirical rules for riser sufficiency and spacing does not eliminate this need, inasmuch as it is impossible to cover all types of castings by such rules.

The development of rules for simple and moderately complex shapes and the determination of the c nditions of solidification can serve as basic guides for resolving more complex risering problems. This process is in keeping with the established method of approach to complex technological problems; i.e., development of detailed knowledge of simple cases plus basic concepts which provide a rational basis for extension to more complex cases.

It is the purpose of this paper to present an analysis of the relationship of solidification factors to risering requirements, based on newly developed information relative to solidification and feeding characteristics of various casting shapes. It is hoped that the generalized concepts which are evolved will serve to advance further the progress of scientific risering.

Solidification Factors Related to Risering

Process of Wall Growth—The solidification factors of primary importance to risering are the characteris-

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tics of the growth of a solid skin from the various mold walls of the casting and of the riser. It will be shown in the discussions to follow that the process of wall growth occurs by the same mechanism at all positions of a given casting or riser; the only differences are in the rates of growth as determined by the heat flow conditions of the various component sections. The process of wall growth differs, however, with the type of metal cast and the thermal characteristics of the mold wall. These differences are important to the problem of risering the various metals.

Solidification of Steel

The case of solidification of steel will be considered in detail in this report in keeping with the emphasis on the risering of steel castings. The basic concepts of riser action, however, are generally applicable with due consideration of the solidification features of the metal concerned. For a general discussion of the solidification features of the various metals attention is directed to references (7) (8) (9).

The most basic aspect of the solidification process is provided by phase diagrams which relate the physical state (liquid, solid or intermixed liquid-solid phases) to temperature. These diagrams show that alloys solidify gradually over a range of temperatures. Steels of 0.25-0.30 per cent C, for example, start to solidify at approximately 2760 F (liquidus) and complete freezing at approximately 2680 F (solidus). At temperatures between the liquidus and solidus the metal is a mixture of both liquid and solid; the percentage of each phase at any particular temperature may be determined by application of the lever arm principle.

However, the constitution diagram is not, in itself, sufficient to define the mode of wall growth during solidification. Another basic factor which must be considered is the thermal gradient conditions which exist during the solidification period. In other words, the constitution diagram defines the phase conditions which exist at any given temperature, and the distribution of temperatures in the casting at any given time defines the distribution of phases at various points in the casting. During the process of wall growth of a steel casting there will exist: 1) a completely liquid zone, 2) a mushy zone of intermixed liquid and solid and 3) a completely solid zone. Depending on the interval of the solidification period all three zones may exist concurrently at various positions in the casting.

The process by which the nature of solidification is determined by dual consideration of the constitution diagram and temperature gradients is known as thermal analysis. The procedure entails inserting thermocouples at suitable locations in the castings to determine the thermal gradients. The thermal cycles of the thermocouples located at thermal centerpoints (last point to freeze) indicate the liquidus (hold) and solidus (inflection of the inverse rate cooling plot) points which establish the temperatures of the start and end of freezing. The thermal-time records accordingly provide the information required to deduce the solidification conditions which exist during the entire period of solidification.

It is important to recognize that the thermal gradient conditions which are developed within the casting during solidification are determined mutually by the thermal characteristics of the metal and of the mold. Thus, a change in the thermal characteristics of the mold material may alter the nature of solidification of a given metal, inasmuch as the temperature gradients in the casting are affected thereby. The basic laws of heat flow dictate that in a steel-sand mold system, relatively mild gradients should be developed in the steel and steep gradients in the sand. This follows from the fact that heat flow across the casting is considerably faster (high conductivity) than into the sand (low conductivity).

Figure 1 (top) illustrates the gradient conditions which should be expected by analysis of the heat transfer conditions. The figure also illustrates modifications in the thermal gradients which result from the exothermic liquid to solid phase change involved in the solidification process. While the temperature at all positions in the casting may gravitate to the level of the liquidus at relatively early time, further drop is prevented until a "wave" of initial freezing reaches the position in question.

Figure 1 (bottom) illustrates schematically that a position such as "C" drops to the liquidus temperature and thereafter remains at this temperature until

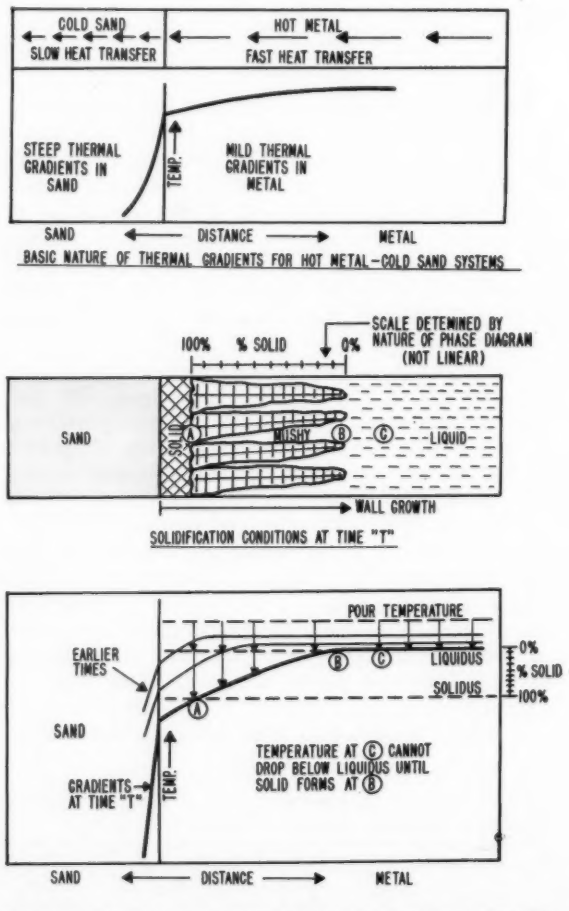


Fig. 1—Thermal and physical features of wall growth.

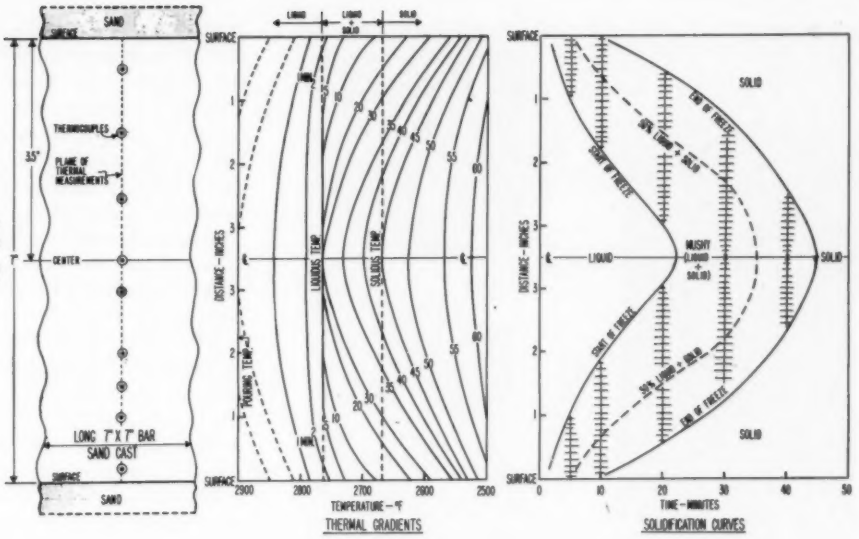


Fig. 2—Wall growth solidification curves for 0.30 per cent C steel as determined by thermal analysis.

the contiguous position "B" begins to develop solid. Obviously, position "C" cannot drop in temperature until a gradient required for heat flow is established between "C" and "B" and such a gradient is not possible until solid begins to form at "B." The process of wall growth is recognized from this illustration as the gradual movement of a mushy zone (center figure) from the mold wall to the thermal center of the section; during this time the central regions of the section remain isothermal at the liquidus temperature (liquidus hold).

For a given metal the width of the mushy zone is determined by the thermal gradient conditions which result from the mutual heat transfer relationships of the metal and mold, as described. The process of wall growth may also be represented graphically by a time-distance plot of the movement of two waves of freezing, i.e., a wave of initial freezing and a wave of final freezing, as represented by the movement of points "A" and "B" from the mold wall to the center of the section during solidification.

Figure 2 presents detailed thermal analysis data[9] for the solidification of sand-cast commercial steel of 0.25-0.30 per cent C. A schematic representation of the wall growth process is provided as an aid to the interpretation of the thermal data and to the visualization of the graphical method of plotting wall growth solidification curves. The data presented are specific to the wall growth from the lateral walls of a 20 x 7 x 7-in. bar, poured at 2900 F in a green sand mold. Except for a change in the distance and time scale and for differences in rates due to casting geometry factors, the mechanism defined is characteristic of the solidification of this steel at any sand wall position of a casting or riser. In other words, solidification proceeds in the described fashion (same mechanism) but at different rates depending on factors to be discussed.

The thermal gradient conditions shown in Fig. 2 indicate that immediately following pouring thermal gradients are established in the liquid metal which adjoins the mold wall. The gradients move inward toward the thermal center of the casting (geometric centerline or center plane for simple castings of this type) and the superheat present in the metal is gradually removed. It is observed also that the thermal gradients through the solidifying steel are relatively mild, i.e., temperatures near the surface are only moderately lower than temperatures at the center. During most of the course of solidification the maximum difference between the center and surface temperatures does not greatly exceed the freezing range of the metal.

The relative mildness of the thermal gradients which transverse the casting result in wide regions of "mushy" solid and liquid structures, as required by the phase diagram for positions at temperatures between the liquidus and solidus levels. The isothermal "hold" at the liquidus temperature is observed for portions of the casting from approximately 3 to 23 min.

For example, while superheat is eliminated from the center of the casting 3 min after pouring with a resultant fall to liquidus temperatures, a further drop in temperature requires a delay of approximately 20 min which represents the time required for the movement of the wave of initial freezing to the center. The progression of the initial wave of freezing to the center of the casting section is plotted as the "start of freeze" curve on the solidification diagram. After the wave of initial freezing reaches the center the process of solidification consists of growth of the dendrite structure throughout the major portion the casting section with the rate of growth being more rapid at positions near the surface.

The movement of the final wave of freezing, plotted as the "end of freeze" curve, initiates at the surface after approximately 10 min and is completed at the center at approximately 45 min, the time of final solidification of the casting. It is observed that the wall growth process is symmetrical with respect to the opposite casting surfaces, this is always the case for sections of uniform thickness irrespective of casting orientation (vertical or horizontal).

Figure 3 presents a radiograph of a ⅛-in. thick, longitudinal section from a casting which was pressure bled with nitrogen gas at the time that the solidifica-

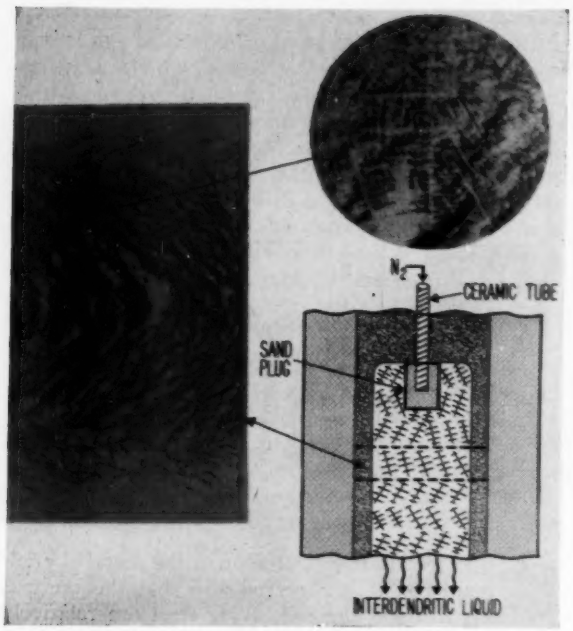


Fig. 3—Radiograph of a ⅛-in. thick section cut from a 20 x 7 x 7 in., 0.30 per cent C steel casting pressure bled at 30 min. The dendrite plus liquid structures in the center regions and the completely solid skin of 1 in. thickness evidence conformance to the predictions of the solidification curves of Fig. 2.

tion curves indicated a 1-in. thick completely solid skin. The interdendritic liquid which existed at the time was expelled leaving behind an interlaced dendrite structure. Experiments of this type served to establish the validity of the solidification curves which provide the basic data of this report.

Rates of Wall Growth—Directional solidification results from variations in rates of wall growth at various casting positions. Figure 4 illustrates the nature of the directional solidification process for the case of a long 4 x 4-in. bar, as deduced from wall growth rates at various lateral positions along the length of the bar. It is observed that positions near the end of the bar develop the most rapid rates of wall growth (curve "1") and consequently solidification to the centerline is completed relatively early. The early solidification of casting end positions must be ascribed to the relatively small ratio of casting volume of heat extracting sand area (V/SA) which characterizes the casting end. Intermediate positions which are not influenced by the casting end (or the riser) develop slower rates of wall growth and accordingly solidify completely at a later time.

The term "semi-infinite" is applied to such regions to indicate that heat flow occurs only in a lateral direction (no end effects). Wall growth therefore proceeds at equal rates at all positions within this region as is indicated by curve "4" which is common to all positions within this region. Positions near the riser solidify at a slower rate than in the semi-infinite region due to the combined effects of heat flow from the riser and slower rate of heat transfer due to the hot sand "pad" developed at the re-entrant corner of the riser-casting junction. Accordingly, the "effective" V/SA ratio of near-riser positions is greater than indicated by the geometric V/SA ratio. It will be shown in the discussions to follow that increasing the length of the bar, or any other section of uniform thickness, results only in increasing the length of the semi-infinite zone. This fact is of basic importance to risering problems from two aspects:

1. The semi-infinite zone represents a region over which solidification is not directional.

2. Solidification time is not increased by increasing the length above the minimum required to develop the semi-infinite zone (length greater than approximately 3* times the thickness for bars and plates).

The semi-infinite zone concept applies only to shapes such as bars, cylinders, plates, etc., having one or two dimensions which from a thermal or solidification viewpoint may be considered infinite. For ex-

*Note: Assuming a riser is located at one end of the section.

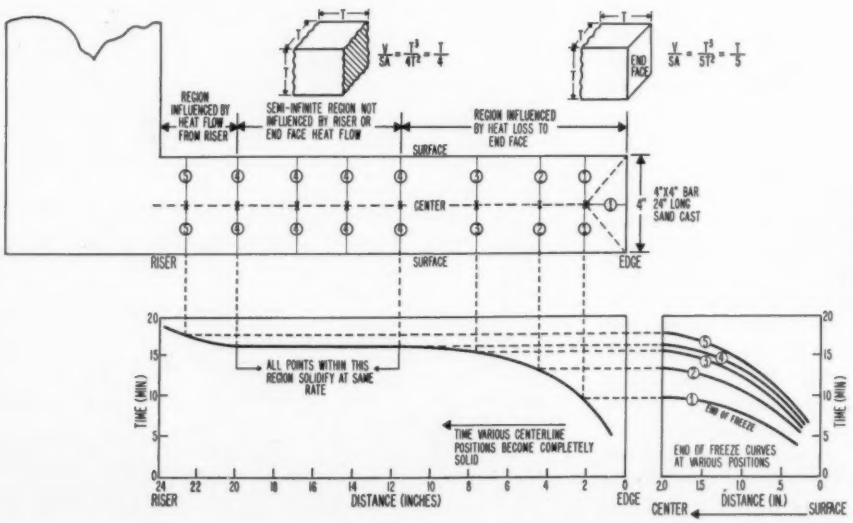


Fig. 4—Illustrating that directional solidification results from variations in rates of wall growth as determined by heat transfer conditions at various casting positions.

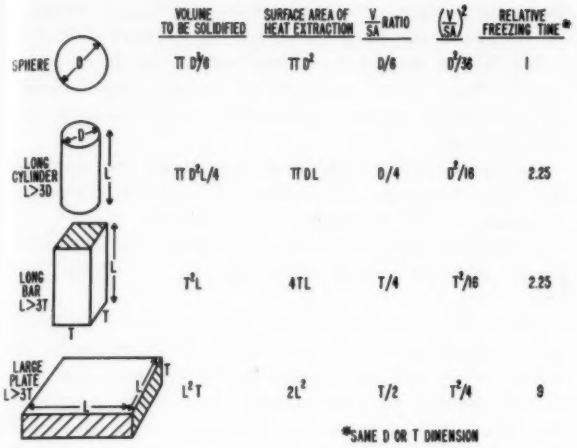


Fig. 5—Geometric V/SA relationships for various simple shapes.

ample, increasing the length of a bar above the 3T minimum required to develop a semi-infinite condition does not further affect the V/SA ratio inasmuch as the limiting V/SA ratio is developed when heat transfer occurs solely through the four side surfaces. Similarly, for the case of the plate the limiting V/SA condition is developed when heat transfer occurs solely through the two side surfaces. Obviously, for the case of cube or spherical shapes all possible dimensions are fixed geometrically and there are no semi-infinite positions; for such shapes the V/SA ratio is invariant.

The limiting V/SA characteristics of the various geometrical shapes determine the minimum rates of wall growth and accordingly the limiting time of solidification. Figure 5 illustrates the wide variations in the geometric V/SA ratios of various shapes. The smallest ratio of volume to surface area possible is obtained with a spherical shape while the largest is obtained with a plate shape of semi-infinite (limiting) characteristics. For example, a plate of thickness T having the same surface as a sphere has a volume three times as large as the sphere.

TABLE 1—DIMENSIONS OF VARIOUS SHAPES HAVING EQUAL V/SA RATIOS

V/SA	Sphere Diameter, in.	Cylinder Diameter, in.	Bar Thickness, in.	Plate Thickness, in.
1	6	4	4	2
2	12	8	8	4
3	18	12	12	6
4	24	16	16	8
5	30	20	20	10
Formula for V/SA Calculation	D/6	$\frac{T}{4}$	$\frac{T}{4}$	$\frac{T}{2}$

Table 1 presents the dimensions of various simple shapes having equal V/SA ratios. Thermal analysis "end of freeze" curves for various 4 in. thick casting shapes of widely different volume to surface area ratios are presented in Fig. 6. It is observed that the ratio of volume to surface area determines the solidification time. As indicated previously the length of the casting affects solidification time only if the lengths are less than the maximum required to develop a "semi-infinite" zone. It may be deduced that as the length of the 4-in. plate shown is decreased to less than 12 in. (same width) a bar shape is approached with the result that the V/SA ratio approaches unity and the solidification time is reduced from 50 to 17 min (time for 4 x 4-in. bar). Decreasing the length of the 4 x 4-in. bar causes an approach to a cube shape with a V/SA ratio of 0.67 and a solidification time of approximately 7 min.

According to Chworinov[10] the relative solidification

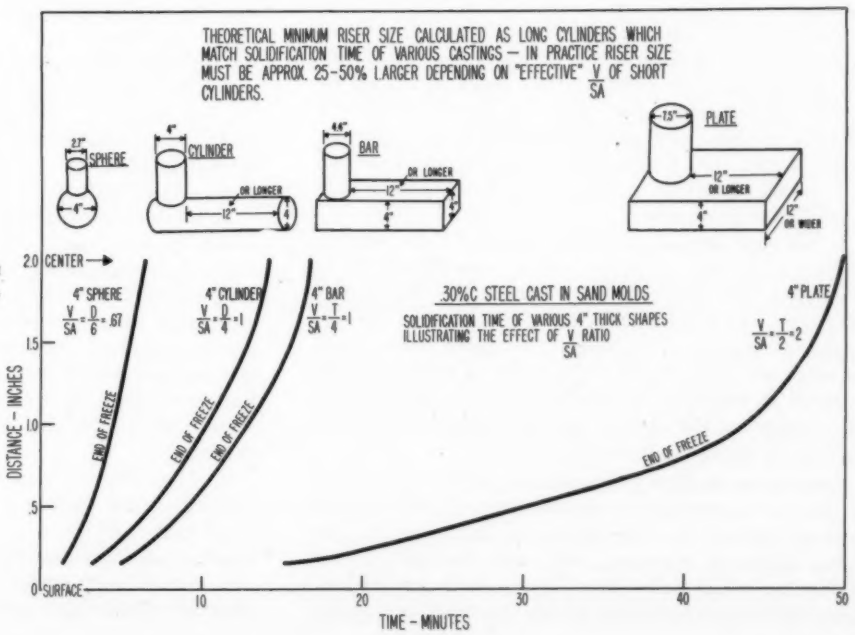


Fig. 6—Effect of V/SA ratio on solidification rates and relative risering requirements.

times of castings are in relation to the squares of the geometric V/SA ratios, i.e.

$$\frac{\text{Solidification time of A}}{\text{Solidification time of B}} = \frac{(\text{V/SA})^2 \text{ of A}}{(\text{V/SA})^2 \text{ of B}}$$

Using this formula the relative solidification times of the shapes shown in Fig. 5 would therefore be as indicated in the last column of the figure. The validity of Chworinov's concept, as well as its significance and limitations, have been the subject of considerable contention.

The data presented in Fig. 6 are representative of results which have been obtained in the course of extensive investigations of the validity of the $(V/SA)^2$ relationship. These studies have shown that the $(V/SA)^2$ relation is essentially correct for different thicknesses of the *same shape* (plate, cylinder, bar, etc.). When a change of shape is involved (for example, calculation of the solidification time of a plate from the known solidification time of a cylindrical shape) corrections are required.

Briefly, for the same geometric V/SA ratio, "chunky" shapes such as spheres or cubes solidify at a later time than the "less chunky" bar and cylindrical shapes, and these in turn solidify at a later time than the "rangy" plate shapes. The differences due to effects of shape *per se* have been established sufficiently to permit practical allowances in the V/SA calculation. The cause of "shape effects" is inherent to the relative amount of specific heat which must be removed from near surface positions prior to completion of solidification at the center of the section. For a given V/SA ratio, chunky shapes present a longer surface to center distance (for example V/SA = 2 represents a 4 in. thick plate and a 12 in. diameter sphere) than rangy shapes, hence must develop lower surface temperatures at the time that the center reaches the solidus temperature.

The additional specific heat which is removed from near-surface positions during the solidification interval results in a delay in the time of final solidification at the center of the section. It has been determined that variations of superheat level also cause significant deviations from the solidification times indicated by V/SA relationships. For example, plates develop a delay in the time of final solidification which approximates +25%/100 F superheat while bar and cylindrical shapes develop a delay which approximately +10%/100 F superheat.

It has been concluded as the result of these various investigations that at the present state of knowledge it is possible to adjust V/SA relationships to permit calculation of relative solidification times at various forms of simple and moderately complex shapes with an accuracy of approximately ±15 per cent. Further research is required to establish correct methods of calculation for the case of complex castings which involve heat exchange between sections.

Application to Riser Dimensioning—The importance of the V/SA concept to the problem of riser dimensioning cannot be overemphasized inasmuch as the V/SA approach represents the only feasible method of developing scientific riser dimensioning procedures. The fundamental research which has been performed by mathematical, electrical analogue[11,12] and thermal analysis methods to establish the significance of V/SA relationships should be recognized as the great practical value. Two general procedures have been adopted in the various approaches to the use of V/SA relationships:

1. Direct use of V/SA calculations on the assumption that the ratios of relative solidification times are the same as the geometric V/SA ratios.
2. Indirect use of V/SA calculations based on empirical relationships of shrinkage and soundness limits.

The direct V/SA calculation methods were based on assumptions which have either been shown to be incorrect or not yet proven to be correct. For example, several methods which have been proposed are based on the incorrect assumption that the relative solidification times are in same ratio as the V/SA ratio rather than in the ratio of $(V/SA)^2$.

Caine's method[4] based on indirect use of V/SA calculations recognized the limitations of V/SA concepts at the time of its development. According to his method the V/SA ratio of the casting is related to a required V/SA ratio of the riser by means of charts which indicate empirical shrinkage and soundness limits for various types of castings and methods of risering. Caine's method, while basically semi-empirical in nature, is technically sound and provides for a gradual transition to more direct calculation as the background knowledge is expanded by continuing research.

Basic Feature of V/SA Calculation

The basic feature of the V/SA calculation method as applied to risering is illustrated by the upper portion of Fig. 6. The theoretical "riser" sizes indicated for the various shapes represent the diameters of long cylinders which match the solidification times of the castings. Except for the use of strong exothermic liquidizers it would not be possible to use cylindrical risers of *less* than the indicated diameter for risering the various shapes, since the solidification time of the long cylinder is the minimum required to match the solidification time of the casting and also the maximum for the diameter in question. The diameter of practical cylindrical risers for these shapes must be approximately 25-50 per cent greater due to several factors, the effects of which have not as yet been established quantitatively. These are:

1. In practice the risers are used as short cylinders which have a numerically smaller V/SA ratio (end effects not completely eliminated) as compared to a long cylinder (V/SA = D/4).
2. Near riser positions in the casting solidify at later times than determined by geometric V/SA factors because of the heating effect of riser and of the hot spot condition created by the "L" and "T" type section formed at the point of junction of riser and casting.
3. The drop in liquid level which occurs during feeding of the casting generates a concentric ring of solid metal at the riser surface which continues to remove and dissipate heat to the surrounding sand and accordingly lowers the effective V/SA of the riser at intermediate and late stages of solidification.

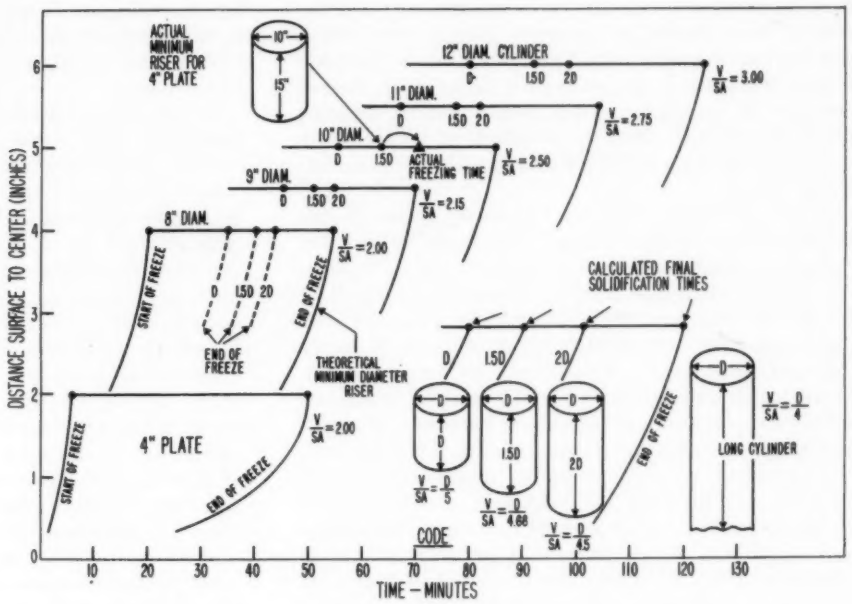


Fig. 7—Illustrating the application of V/SA concepts to the risering of a plate casting.

In addition, it is recognized that at late stages of solidification the riser must remain sufficiently open to transport feed metal with ease. A riser which is in late stages of solidification at the same time as the casting cannot accomplish this purpose effectively inasmuch as the passage of the small amount of available feed metal is highly restricted by the narrow feed channels then available.

Figure 7 provides data of the solidification characteristics of a 4-in. thick plate as compared to various long cylinder and riser shapes. The solidification times indicated for the 1D, 1½D and 2D high risers were calculated on a basis of the geometric V/SA ratios (V/SA = D/5, D/4.66 and D/4.5, respectively, as compared to D/4 for a long cylinder) on the assumption that the heat loss rate of the top face is the same as that of the riser sides and that no heat loss occurs at the contact face. The calculations were based on the Chworinov relationship, for example:

Known: The solidification time of a long 8-in. diam cylinder is 55 min (thermal analysis data).

Calculate: The solidification time of a 1½D high, 10-in. diam riser.

$$\text{Riser } \frac{V}{SA} = \frac{D}{4.66} = \frac{10}{4.66} = 2.14$$

$$\text{Long Cylinder } \frac{V}{SA} = \frac{D}{4} = \frac{8}{4} = 2$$

$$\frac{\text{Solidification time Riser}}{\text{Solidification time Long 8-in. Cyl.}} = \frac{(V/SA)^2 \text{ Riser}}{(V/SA)^2 \text{ Long 8-in. Cyl.}}$$

$$\frac{\text{Solidification time Riser}}{55} = \frac{(2.14)^2}{(2)^2}$$

$$\text{Solidification time Riser} = \frac{(2.14)^2\ 55}{(2)^2} = 63 \text{ min}$$

The calculation is simple and would yield accurate information if the riser heat loss conditions were as assumed. Obviously, the difficulty in using this method lies in the presently unknown corrections which should be applied to the geometric V/SA ratio of the riser so as to establish an "effective" V/SA ratio. Research effort at present is directed toward establishing the relationship between geometric and "effective" V/SA ratios of various riser types when used on castings of "chunky," "intermediate" and "rangy" characteristics. It is interesting to note that 10-in. diam 1½D high risers (see Fig. 7 relationships) are of minimum adequacy for 4-in. thick plates when used with conventional risering compound covering, indicating a near agreement with the geometric relationships.

The foregoing discussions have been presented to illustrate the general approach which is required for evolution of methods for the scientific proportioning of risers and also to emphasize the limitations of basic knowledge which must be surmounted to fully achieve the goal. The problem of "effective" V/SA ratio as contrasted to geometric V/SA ratios is also inherent to the casting. Inasmuch as complex castings actually represent combinations of simple shapes, information is required as to the intereffects of simple shapes joined in various combinations. For example, information that "L" joints of plate sections solidify at 1.3t (t = time of flat plate) and "T" joints at 1.5t provide a means of adjusting the known time of solidification of a flat plate to that of the joint.

If the casting is composed of thick sections joined to thinner sections, it is necessary to know the effect of the thin sections on the solidification time of the heavy section which serves as the riser position. Recent work has shown that for cases of large differences in section size the heavy section may be considered, for practical purposes, as an entirely separate body. For the case of cores having a diameter of less than one-half the section thickness the core surface area

may be discounted in the V/SA calculation; for the case of cores having a diameter greater than twice the section thickness the V/SA relation is approximately the same as that of a plate of equal thickness. Sections of complex castings which exceed semi-infinite lengths are readily calculated as independent sections of V/SA ratio typical of the shape (bar, cylinder, plate).

Riser positioning does not ordinarily present a difficult problem if the casting is composed of obvious "chunky" sections joined to rangy sections. In such cases the position and number of risers is determined by the distribution of the individual "chunky" regions. However, if the casting is of uniform or nearly uniform geometry, it is necessary to know the distance which individual risers will feed to soundness before the position and number of risers can be established. The following sections of the report present a summary of the feeding distance relationships which have been determined for single and combinations of uniform sections. The solidification and feed metal flow conditions which determine the development of soundness or of shrinkage are discussed in detail for the purpose of presenting a comprehensive analysis of the feeding problem.

Characteristics of Feed Metal Flow

The development of soundness, gross shrinkage or centerline shrinkage in a casting is determined by the nature of the channels available for the movement of feed metal. Analysis of the characteristics of feed metal channels requires consideration of the aspects of wall growth and also of the aspects of directional solidification. As described previously the wall growth of steel in sand molds results in the development of a mushy condition of interlaced solid and liquid; accordingly, feed metal must flow through complex interdendritic paths (microfeatures of feed metal flow). The direction of the flow must necessarily be such as to compensate for liquid-solid shrinkage at positions of more rapid solidification, i.e., feed metal must flow from positions which solidify slowly to faster solidifying positions (macro features of feed metal flow).

Figure 8 illustrates the changing nature of the interlaced solid-liquid structure (microfeatures) as wall growth approaches completion at any given position. The conditions at the thermal center of the section illustrate the gradual constriction and ultimate stoppage of feed metal flow through the last available interdendritic channels. As dictated by the phase diagram a decrease in temperature is accompanied by growth of the dendritic structures, narrowing of the liquid metal regions and concomittant changes in composition such that the composition of the dendrites approaches the average composition of the metal while the remaining liquid becomes enriched in the solute elements (C, Si, etc.).

The decreased solidification temperature resulting from the segregation process is fundamentally responsible for permitting flow of feed metal to the lowest temperatures in the solidification range, i.e., to complete soundness. It is obvious that flow of liquid of composition of lower carbon content than called for by the temperature then existing at the location in question should result in the rejection of a large quantity of solid with consequent plugging of the channel. Such effects have been noted at points of large change in section, i.e., a tendency to plug a feeding channel as higher temperature liquid is drawn rapidly into narrow feed channels.

The conditions of directional solidification determine the gross "taper" features of the channels and accordingly the direction of liquid flow. Figure 9 illustrates schematically how the wall growth solidification rates at various points in a section establish the gross nature of the solidification channels and accordingly permit feed metal flow to soundness or prevent such flow with resulting development of shrinkage. Channels with open taper toward the

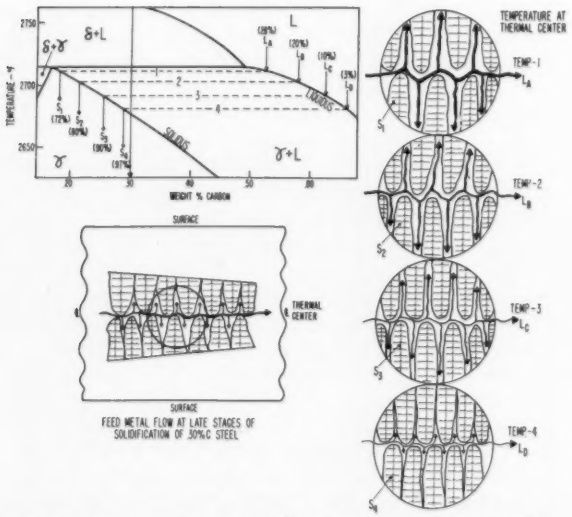


Fig. 8—Micro-features of feed metal channels related to the nature of solidification as determined by the phase diagram.

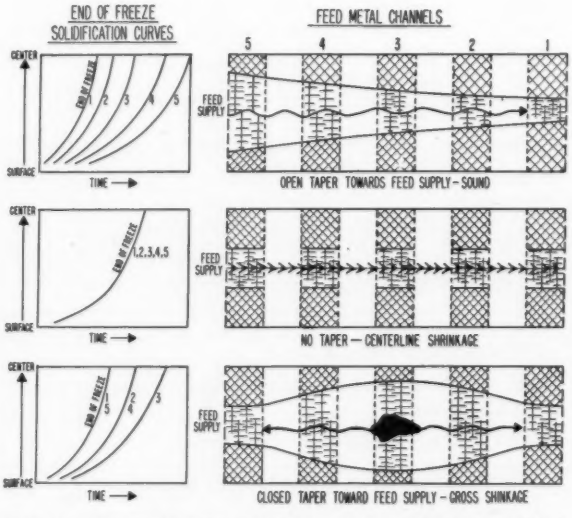


Fig. 9—Macro-features of feed metal channels related to the system of wall growth rates determined by casting geometry.

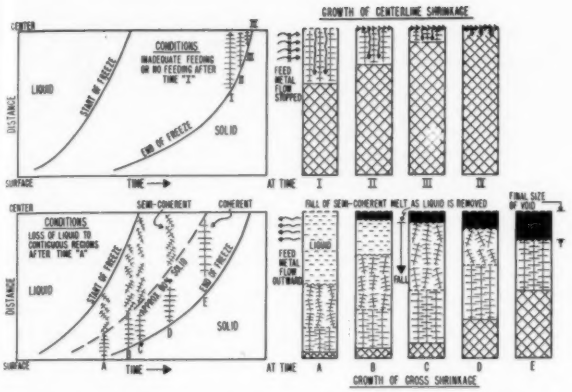


Fig. 10—Mechanism of the development of centerline and gross shrinkage.

main feed supply develop complete soundness, channels without taper develop centerline shrinkage, and channels with closed taper toward the main feed supply result in gross shrinkage pockets. The term "main feed supply" is used to indicate that the concepts apply to the case of large sections acting as feeders to smaller sections as well as to risers.

The mechanism by which centerline and gross shrinkage develop is indicated by Fig. 10. Parallel wall channels are capable of feeding during most of the solidification period inasmuch as the feed metal moves inward as the result of hydrostatic and capillary forces to fill the voids left by liquid-solid shrinkage. However, at the last stage of solidification the interdendritic channels become highly obstructed and passage of feed metal from the riser through long expanses of such channels is not possible. As the result, the small pools of interdendritic liquid at the centerline are drained to feed contiguous lateral regions. The drained regions along the centerline cannot be refilled and hence remain as small shrinkage pockets in a dispersed line-like arrangement. Figure 11 illustrates typical dendritic outlines of centerline shrinkage regions developed in thick sections (3-4-in. bar and plates); in thinner sections the size of the voids decreases and the dispersion increases.

Gross shrinkage is developed in the case of channels with closed taper toward the main feed source inasmuch as feed metal flow is cut off at relatively early stages of solidification. Therefore, the natural thermal center-spot of an isolated region must provide feed metal for most of the total solidification time.

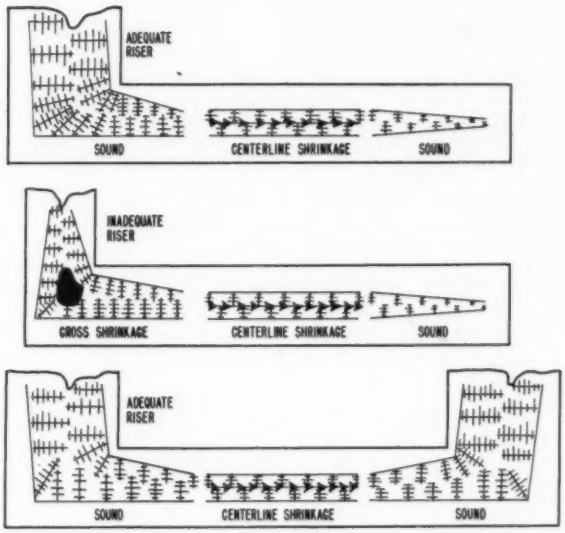


Fig. 12—Feed metal channels in plate and bar castings of uniform thickness.

The size of the gross shrinkage cavity is determined by the amount of liquid lost by the thermal center zone prior to the time that the mushy metal becomes coherent, i.e., before a sufficiently high proportion of solid (approx. 60 per cent for steel) is developed to prevent further settling. Thereafter, further loss of feed metal occurs by out-flow from the coherent mass resulting in a general "sponginess"; however, the gross size of the cavity is not enlarged further. This analysis also indicates that the general outline of the shrinkage pipe in risers represents the wall thickness of the solid layer plus the coherent mushy layer existing at the time that the level of liquid and associated non-coherent mushy metal fell below the various vertical positions. The lowest position of the riser pipe represents the point reached at the time the

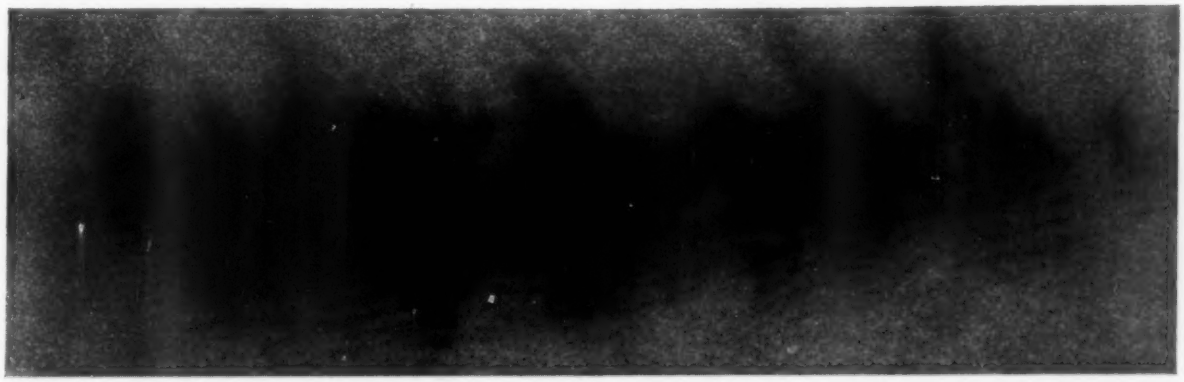


Fig. 11—Enlarged radiograph of centerline shrinkage. The dendrite structure is developed as the result of inadequate feeding at the final stage of solidification; see Fig. 10.

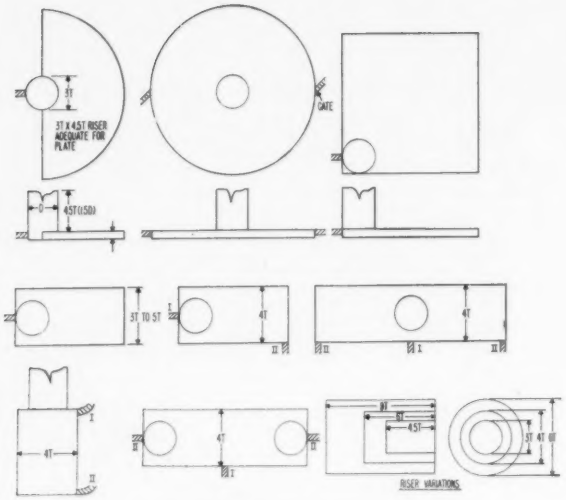


Fig. 13—Plate casting systems.

centerline of the riser developed coherency.

Figure 12 illustrates the feed metal channel conditions in bars and plates, fed by single or multiple risers, which lead to soundness or shrinkage. This simplified illustration is basic to the considerations of feeding distance conditions to be discussed.

Resume of Feeding Distance Studies

Plate and Bar Castings of Uniform Section—Plate and bar castings of a variety of systems, shown in Figs. 13 and 14, were cast in various thicknesses and lengths to determine the distances which could be made sound and the location and extent of shrinkage, when developed. The studies [13,14,15,16] were made with induction melted steel of 0.25/0.30 per cent C, 0.50/0.80 per cent Mn, 0.35/0.50 per cent Si, deoxidized by the addition of 0.10 per cent aluminum to the ladle and poured at 2900 ± 25 F. In all cases risers were of sufficient size to eliminate under-riser shrinkage. The molds were made of synthetic silica sands of AFS No. 80 grain size, bonded with 3 per cent bentonite and containing 3 per cent water. Several series of castings of each thickness were made with 1-in. variations in length. The castings were sectioned to remove 1-in. thick longitudinal strips and radiographed at 1½ per cent sensitivity to determine the presence of shrinkage. It is evident that this represents a near ultimate test for solidity inasmuch as it reveals the presence of single voids as small as 0.015 in. and also of smaller voids if aligned to produce an equivalent effect.

The maximum distances which could be made completely sound are indicated by the formula and graphic relationships shown in Figs. 15 and 16. As the castings are made longer than the maximum distances, centerline shrinkage develops at the positions indicated. Castings with ends (single riser systems) always developed a sound end zone resulting from casting edge gradients; Figure 17 presents a typical radiograph of such a casting. The length of this zone may be regarded as the contribution of the casting edge to the feeding distance while the difference between this distance and the maximum distance which may be fed to soundness may be regarded as the riser contribution. In cases where casting ends are absent (dual riser systems) the maximum feeding distance is simply the sum of the contributions of the two individual risers.

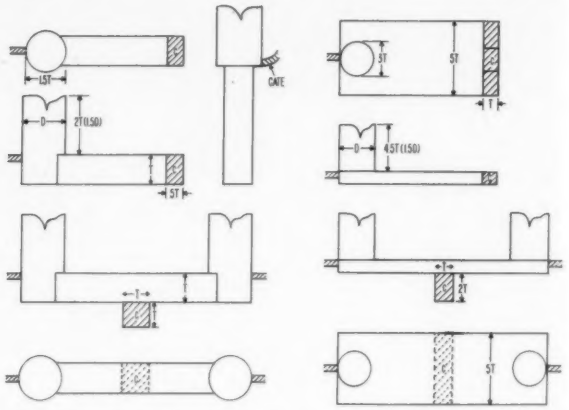


Fig. 14—Bar casting systems used for sand and chill studies and plate systems used for chill studies.

It was also established that the feeding distance provided by risers, as measured on the basis of the maximum length which can be made completely sound, is not maintained if the length is sufficient to develop centerline shrinkage. Under such conditions shrinkage, particularly in plates, often tended to extend to positions closer to the riser. The variable riser feeding distance contribution obtained under these conditions is indicated by the dashed areas in Figs. 15 and 16.

The use of a less conservative criteria of solidity to establish the feeding distances, such as, 4 to 5 per cent sensitivity, indicates a marked increase in feeding distance to 8-10T as against 4½T for 2-in. thick sections; however, a barely significant increase is ob-

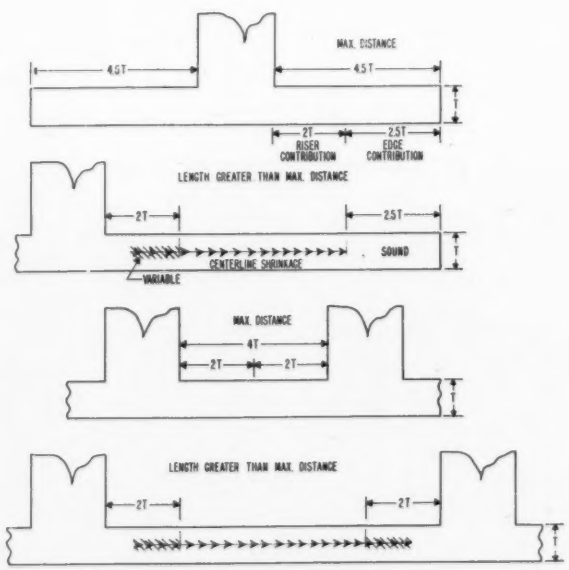


Fig. 15—Feeding relationhips for plates.

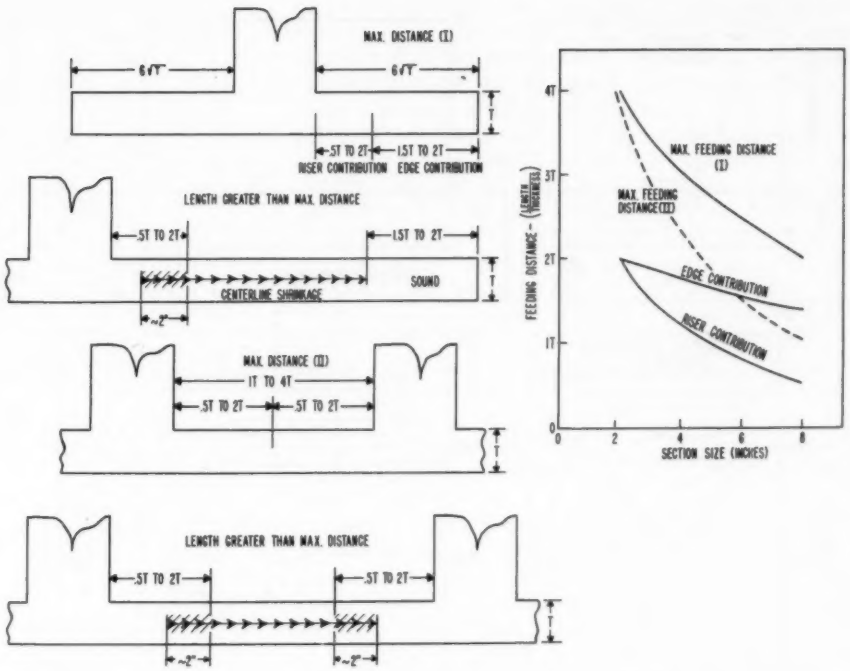


Fig. 16—Feeding relationships for bars.

tained for the case of thicker sections. This is understandable from consideration of the relative lateral distance of the solidification regions which contribute to centerline formation of thick and thin sections (see Fig. 10). In the case of thin sections a relatively narrow "drain" region is operative so that the small voids which result are barely measurable at 1½ per cent sensitivity. In the case of the thick sections the relatively broad "drain" region results in the formation of relatively large voids observable at 4 to 5 per cent sensitivity, as the initial condition.

It is apparent that the feeding distances promoted by risers *per se* are disappointingly short. Without directional solidification aids such as padding, casting edges, etc., risers cannot be expected to serve as efficient means of promoting feeding to soundness.

Effects of Position, Risering and Gating Variables—Variations of casting position from horizontal to vertical, as shown in Fig. 13, were determined to have no effect on feeding relationships except in very heavy sections as typified by 8-in. bars. In the case of the 8-in. bar an increase of approximately 4 in. in maximum feeding distance was noted for the vertical position (no difference for 6-in. bar). This effect was determined by thermal analysis studies to be related to the development of convection currents which produce a significant delay in the solidification of the riser and near riser positions of the vertical casting, thus promoting more extensive directional solidification in slowly solidifying heavy sections.

Accordingly, it is indicated that the popular practice of using extra-high risers to develop higher static pressures with the aim of increasing the feeding efficiency of the riser actually is ineffective. This conclusion is not surprising in view of the advance solidification conditions which have been shown to exist in the casting and riser at the time such feeding must be effective. The small increased feeding distance obtained in the case of heavy vertical castings represents a thermal rather than a ferrostatic pressure effect.

Increasing riser diameters greatly above the minimum required to eliminate under-riser shrinkage was found to have no effect on feeding distance for horizontal castings. Variations of riser size up to 7 times the volume of a sufficient riser of minimum size were investigated. A small effect (approximately ½ to 1T increase in feeding distance) was observed for top risered vertical castings when risers twice the minimum adequate diameter were used. The increased

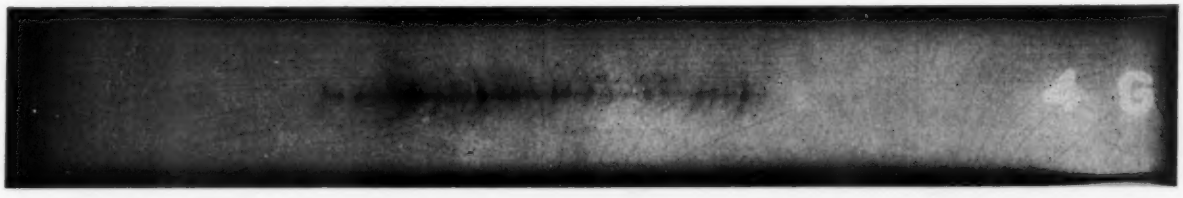


Fig. 17—Transverse radiograph of a 1-in. thick longitudinal strip cut from a 4 x 4-in. bar casting of length greater than the maximum feeding distance. Centerline shrinkage area separates the sound casting edge zone (right side) and sound riser zone (left side).

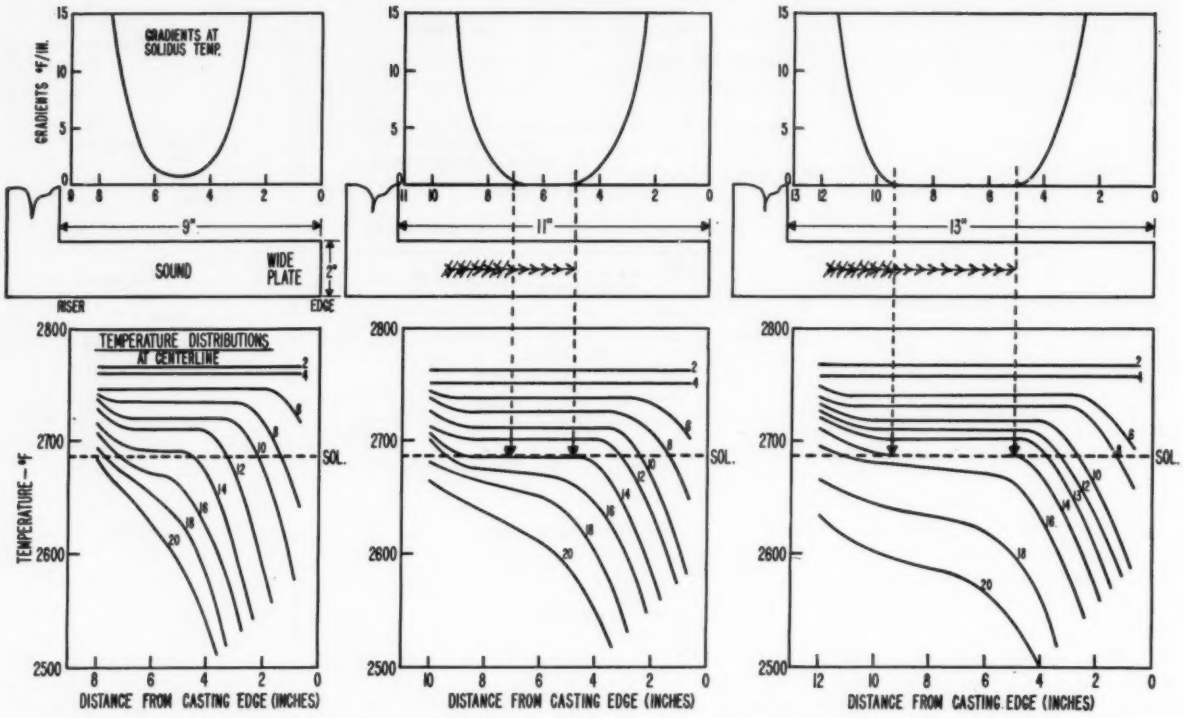


Fig. 18—Thermal conditions for the development of centerline shrinkage in plate castings.

feeding distance was ascribed to the riser overhang which created a hot pad area (see discussion of joined sections) at the riser-casting junction. The practice of using oversize risers to improve feeding distances must be considered ineffective in the light of these results.

Variations of gating procedures in which the gates were attached to the riser or conversely to casting ends (conditions I and II of Fig. 13) did not result in significant changes in feeding range.

Systematic studies of the behavior of "knock-off" and "blind" risers have not been conducted to date. However, check evaluations indicated that knock-off and blind risers behave similarly to full or partial contact risers.

Gradient Conditions Required for Feeding to Soundness—The use of thermal analysis techniques permits an accurate determination of the thermal gradient conditions and therefore of the feed metal channels which are developed in various types of castings. Figures 18 and 19 illustrate the characteristics of the temperature distributions and thermal gradients for plates and bars of lengths less than and greater than the maximum distance which may be fed to soundness. The values of the thermal gradients which exist at the various longitudinal positions at the

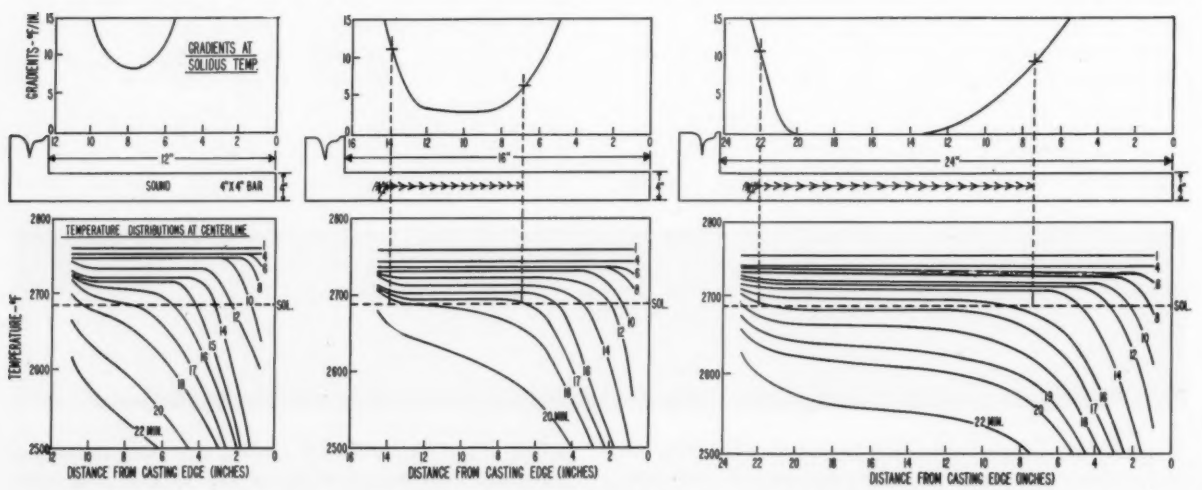


Fig. 19—Thermal conditions for the develop ment of centerline shrinkage in bar castings.

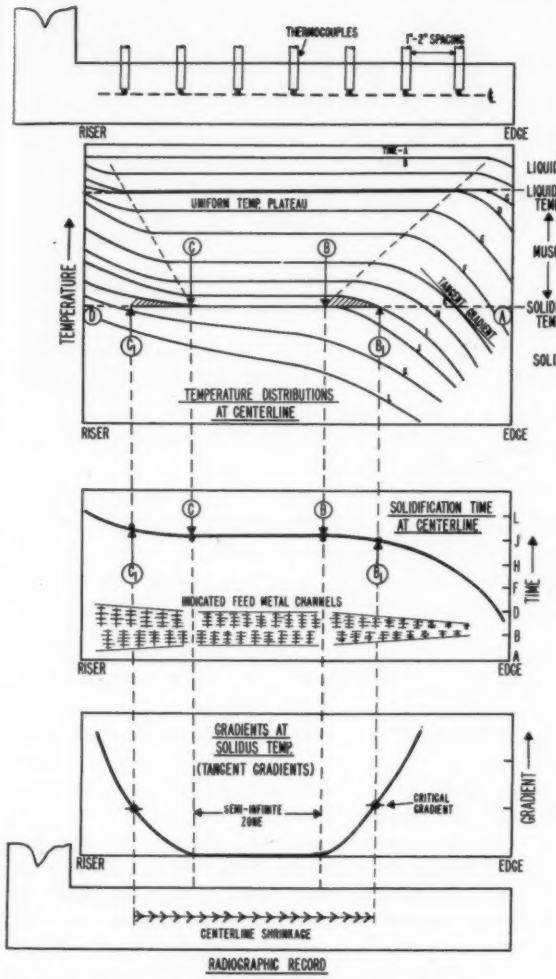


Fig. 20—Interpretation of thermal analysis data.

time that solidification is complete (solidus temperature is reached) are indicated by the upper graphs of the figures.

Figure 20 illustrates the general features of thermal gradient data and the method of interpretation of the data. The data are representative of bars or plates of lengths greater than the maximum distance which may be fed to soundness. In general the temperature along the centerline falls to the liquidus in a short time (1-5 min for most cases to be considered) —the relative thickness and superheat conditions determine the actual time required. During this time no measurable longitudinal gradients exist except at positions very close to the end surface and to the riser. As wall growth progresses relatively steep gradients are established at the casting end in comparison to those developed at the riser end; the major portion of the casting centerline remains gradientless as indicated by the uniform temperature plateau. The riser and casting end gradient conditions gradually encroach on the plateau zone reducing its length as the temperature drops.

It is deduced that regions such as *A* to *B* develop tapered feed metal channels inasmuch as the extent of solidification at positions near *A* is always in advance that of positions near *B*. This is indicated from the fact that the temperature distribution curves at any given time denote lower temperatures, hence more advanced states of solidification, at *A* as compared to *B*. Directional solidification with consequent soundness should be expected in zone *A* to *B*.

In a zone such as *B* to *C* the feed metal channels are indicated to be of a parallel wall type to the end of solidification. This is deduced from the fact that no longitudinal gradients exist between point *C* and *B* up to the time that this region drops below the solidus temperature. Feed metal flow should be possible through such a zone until the channel becomes highly constricted (near end of solidification at center.) Since a position at *C* completes its solidification at the same time as a position at *B*, feeding should not be possible at near-ultimate stages and centerline shrinkage should be developed along the zone between *B* and *C*.

Zone *C* to *D* indicates a tapered channel similar to the one of zone *A* to *B*, and therefore should be supplied with the feed metal required for soundness. The actual values of the gradients which exist at the time that the various positions become completely solid are observed to continually decrease in value. Accordingly, regions of the type which are shown as dashed areas to the right of *B* and to the left of *C* develop gradient conditions of such mildness (hence channel conditions of minor taper) that there may not be sufficient time for the entry and flow of feed metal during the end-point period of solidification for these regions. The time effect may be appreciated by consideration of the rapidity of final freezing at positions of Fig. 4 equivalent to B-B_1 and C-C_1.

The nature of solidification of the metal is an important factor in this respect for metals which have wide regions of mushy freezing require more feed metal at late stages of solidification than metals which develop narrow regions. Geometry factors are also important since the over-all contour of the feed metal

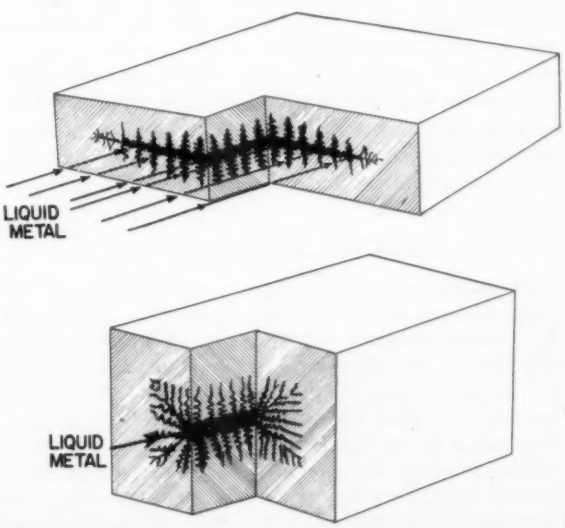


Fig. 21—Features of feed metal channels in bar and plate castings.

channels is determined by such factors. As the result of these factors centerline shrinkage may cover a distanc of the order of B_1 to C_1 rather than B to C. In other words, shrinkage may be developed in regions of moderate thermal gradients (such as B to B_1) despite the indicated *directional* nature of solidification.

The effects of casting length on the nature of the thermal gradient and soundness conditions of 2-in. plates and 4 x 4-in. bars, Figs. 18 and 19, illustrate the general relationships for such shapes. Consideration of the zones which develop soundness and shrinkage

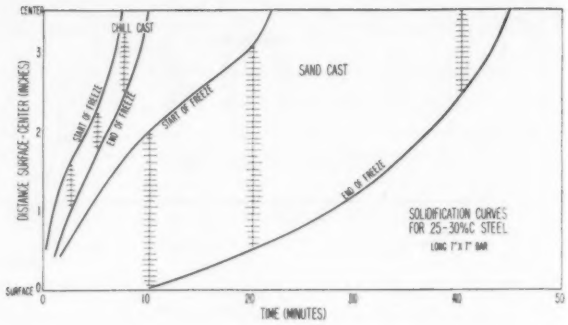


Fig. 22 (above)—Comparison of chill and sand wall solidification.

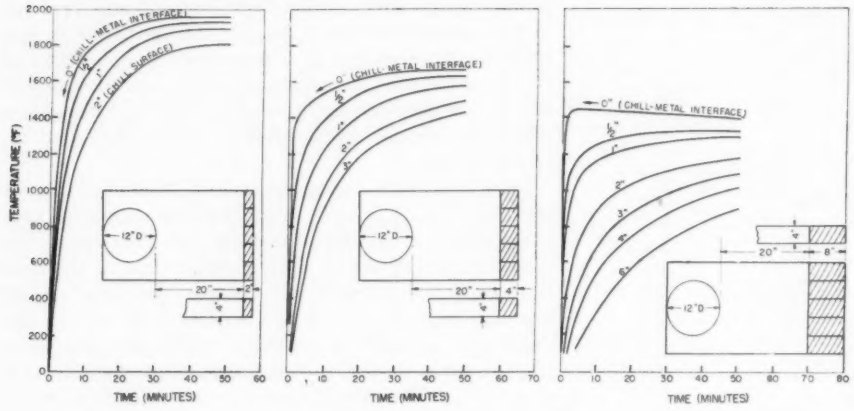


Fig. 23 (left) — Thermal cycles indicating the relative thermal adequacy of chills.

respectively indicate that gradients of approximately 1-2°F/inch are effective in promoting feeding to soundness in the case of plates. In the case of bars much steeper gradients are required; shrinkage develops in regions which have gradients of less than 6-12°F/inch. This indicates more difficult feeding for bar shapes. An explanation for this is indicated schematically in Fig. 21. In the case of plates longitudinal flow occurs along a wide channel with lateral feeding required in only two directions. In the case of bars longitudinal flow must take place along a single rod-like channel with lateral feeding required in a full 360-degree sector; thus, a greater amount of feed metal is required and a smaller opening is provided. This suggests that shape factors are primarily responsible for the fact that the maximum feeding distance in bars ($6\sqrt{T}$) is less than that obtainable in plate shapes ($4\frac{1}{2}T$). For example, a 4-in. bar may be fed to soundness for a distance of 12 in. and a 4-in. plate to 18 in.

Use of Chills to Increase Feeding Distances

Investigations[7] of the effects of chills on solidification indicated that a steel bar completely surrounded by chills completes solidification in approximately one-fifth of the time required for one cast in sand; for plates the time is approximately one-tenth of that required in sand. Also, the nature of solidification was noted to be different in that narrow regions of mushy freezing are developed as compared to general mushy conditions typical for sand castings. Figure 22 illustrates these differences by solidification curves and related schematic diagrams of wall growth. These investigations suggested that chills suitably placed at casting edges or between risers would act to increase the rate of solidification and accordingly to increase the effective feeding distance. Systems of the type shown in Fig. 14 were therefore investigated, using suitable chill thicknesses. For the purpose intended it was necessary to ensure that the chills were of sufficient thickness to develop a continuing rapid rate of heat extraction throughout the entire period of solidification. Thin chills which provide short time heat extraction effects obviously would not be suitable.

Figure 23 illustrates the thermal analysis method used to determine chill adequacy for plate castings. Chills of various thicknesses were placed at the ends of castings of a length somewhat greater than the maximum distance which may be fed to soundness when cast in sand. The thermal cycles at various interface to surface positions of the chills indicate the sufficiency of the chills for continued rapid heat extraction during the known time (50 min) of solidification of the castings.

The thermal cycles show that chills equal to the thickness of the plate (1T) maintain a near constant interface temperature during intermediate and late solidification times indicating the absorption of heat at the same rate as released by the casting; ½T chills show a continuing temperature rise at the interface until relatively late times indicating approach to heat saturation; 2T chills show a decreasing temperature at interface positions after a relatively short time indicating a thickness greater than sufficient to maintain a heat balance with the casting. Accordingly, it was concluded that 1T chills are of sufficient thickness for plate castings. Chills of ½T thickness were

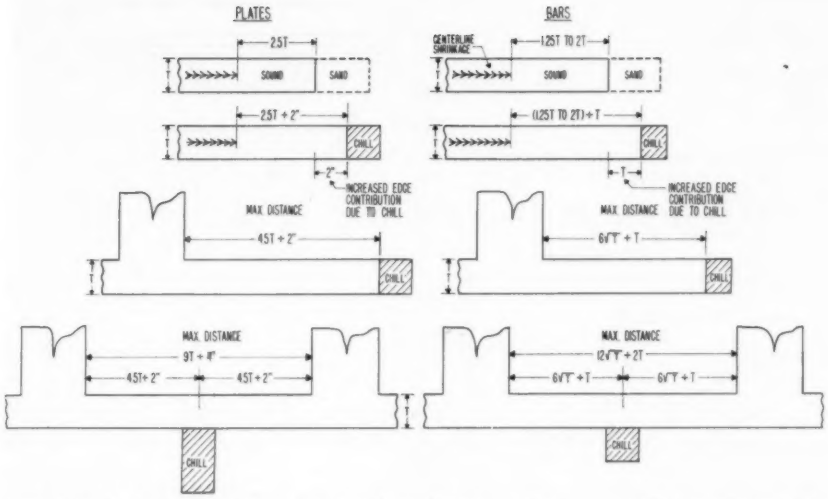


Fig. 24—Effect of chills in increasing feeding distances.

found to be sufficient for the faster solidifying bar castings. Radiographic evaluation indicated equivalent effects for the sufficient and oversufficient chills and a lesser effect for the insufficient chill, thus corroborating the thermal indications.

The effects of chills on the maximum distance which can be fed to soundness for different systems are summarized in Fig. 24. It is observed that the increase in feeding distance (2 in. for plates, 1T for bars) obtained by using chills at casting edges is disappointingly small; however, the effect when located between risers (casting edges absent) is very large. For example, while a 3-in. plate section located between two risers may be fed for a maximum distance of 12 in. (6 in. per riser) the addition of a sufficient chill midway between the risers permits a spacing of 30 in. (15 in. per riser); similarly risers on a 3 x 3-in. bar may be separated by only 10 in. (5 in. per riser) while the addition of a chill permits a spacing of 24 in. (12 in. per riser).

It is fortunate that a marked increase in feeding distance is obtained by the use of chills for the case of multiple risers which normally present the most difficult problem. The more extensive feeding distances which may be obtained by this simple use of chills should result in a net increase in casting yield inasmuch as fewer risers are required to provide the necessary coverage.

The primary effect of adding the chill to the casting end entailed the creation of a longer sound end zone. In cases where bars or plates were of sufficient length to develop shrinkage it was observed that the length of the sound end zone was greater than that obtained in sand by the exact amount that the maximum feeding distance was increased by the use of the chill. Figure 25 presents thermal data for plates and bar castings illustrating a shift in the critical 6-12°F casting edge gradient zone for bars by 1T and a shift of the 1-2°F casting edge gradient zone for plates by approximately 2 in. This observation corroborates the radiographic findings that the increased feeding distances results entirely from an increase of the casting end effects.

Thermal studies indicated also that chill casting edge effects are primarily attributable to the faster solidification developed during the period of *wall growth* from the casting end. For example, Fig. 26 illustrates that the relative difference in the distance of penetration of the "end of freeze" point along the centerline is established at relatively early times. An approximately five fold faster rate of solidification develops from the chill end face as compared to the sand end face. As the result the "end of freeze" wave representing *wall growth from the end face* reaches a point of common junction with the lateral "end of freeze" wave from the side walls at a greater distance

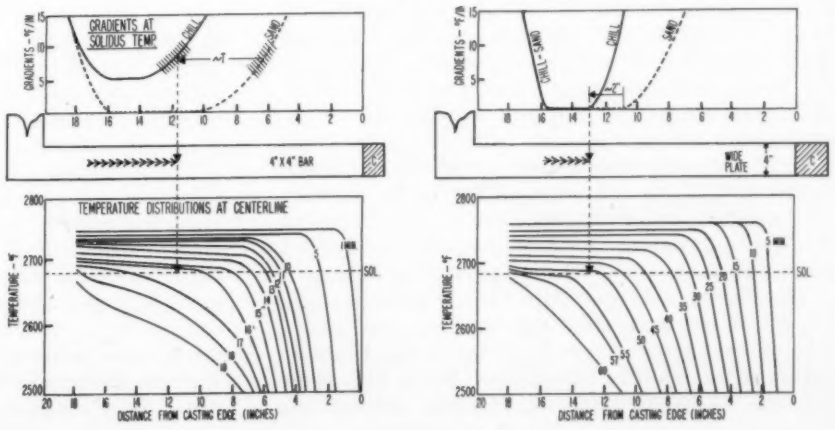


Fig. 25 — Shift of the critical gradient zones of bar and plate castings due to chills.

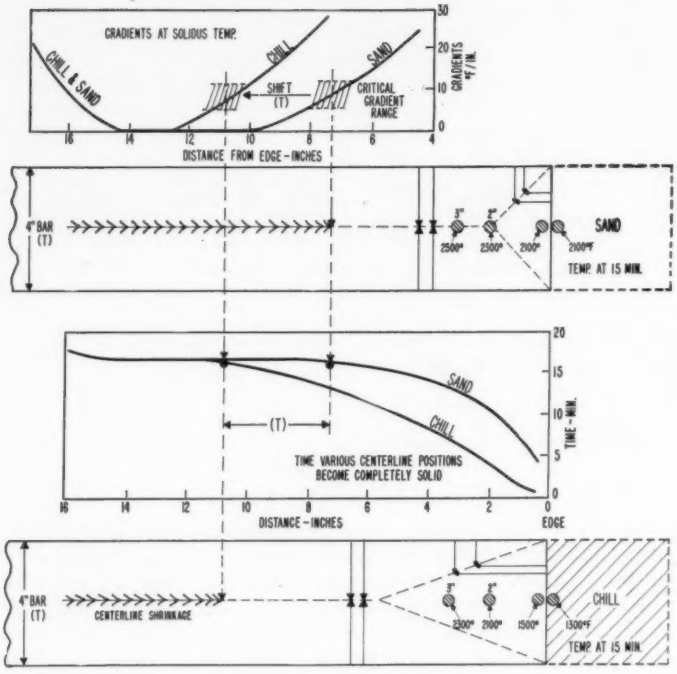


Fig. 26 (above)—Illustrating the short range thermal effect of chills.

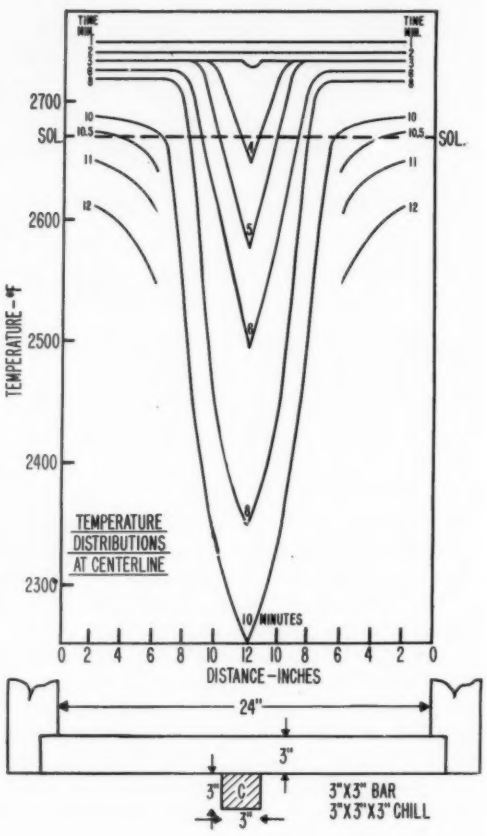


Fig. 27 (right)—Practical utilization of the short range thermal effects of chills to obtain large increases in feeding distances.

from the end than in the case of the sand end-face. This is indicated in Fig. 26 by the apex of the triangular area, which marks the position at which directional solidification by end-face wall growth along the centerline terminates and directional solidification solely by *lateral* wall growth begins.

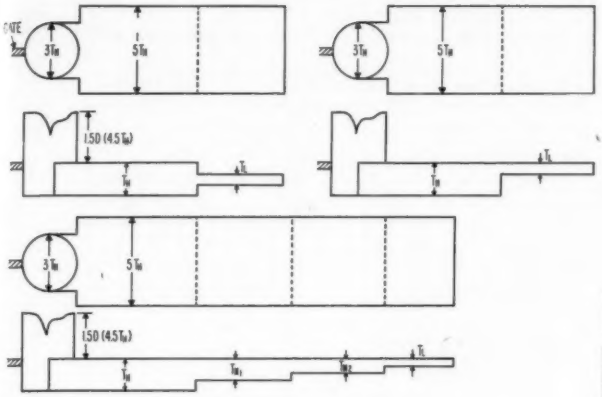
The inability of chills to promote greater distances of directional solidification is ascribed to the tendencies of chills to undercool the adjacent solid metal in preference to extracting heat from deeper positions in the casting. The end zone temperatures which exist at the time centerline shrinkage begins to form in the casting show this effect clearly (Fig. 26). In the case of sand the end-face is cooled to 2100 F while in the case of the chill the end face is cooled to 1500 F; similarly a position 2 in. from the end-face is cooled to 2300 F and 2100 F respectively. Because of this effect the use of high conductivity chills (copper blocks and water-cooled shells) as well as overthick steel chills does not add significantly to the small effect obtainable with sufficient chills. Such chills develop still lower interface temperatures which result only in greater undercooling of the already solid end-face metal. In summary, it may be stated that chills do not develop long range thermal effects but promote intensive short-range cooling effects.

The thermal effects of chills located between risers, Fig. 27, is very great since this represents the utilization of short range action resulting in rapid solidification at the point of application. For a 3-in. plate, 3½ min is required for solidification through the thickness as compared to a normal 36 min, thereafter the solidified region acts as a chilled casting edge. The midspan region which normally behaves in temperature plateau fashion (gradientless) is thus changed to a synthetic casting edge which is equivalent to splitting of the casting into two separate parts and chilling the split ends.

Feeding Range of Joined Sections

The empirical feeding distance data and the generalized concepts of solidification and geometry factors may be recognized as having both immediate practical value and long range fundamental value. From the practical viewpoint the data are useful in handling uniform sections which present difficult feeding problems. From a fundamental viewpoint the data provide a base line for analytical study. Similar systematic study of the feeding and related thermal characteristics of joined sections was undertaken to provide information required for more complex, non-uniform castings.

The first step entailed a study of plate castings joined in series of decreasing thicknesses. The relationships evolved from studies of uniform sections suggested that heavy sections should promote feeding in adjoining thinner sections and that the thinner sections should decrease the effects of thick section casting edges. Also, it was suggested that if the small sections are very thin compared to the thick sections the converse would be true; i.e., the thin sections may act as heat radiators and thus serve as mild chills.



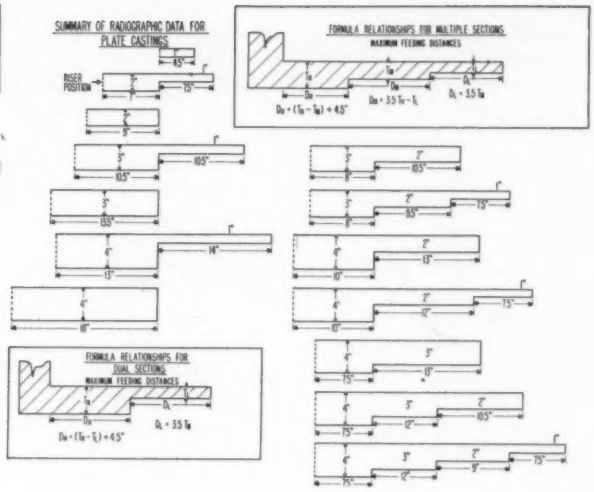


Fig. 28 (above)—Joined plate casting systems.

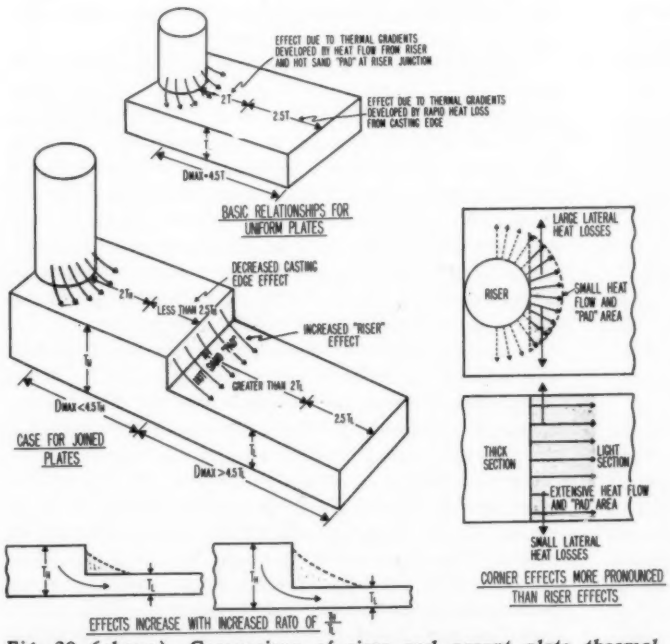
Fig. 29 (right)—Feeding relationships for joined plate casting systems.

However, this effect should not be pronounced since the thin section only could approach the effect of a chill (2 in. added feeding distance).

The casting systems shown in Fig. 28 were studied in various combinations with thicknesses of 1, 2, 3 and 4 in. Figure 29 presents the maximum distances which can be fed to complete soundness for the various combinations and the indicated empirical formula relationships.

It is deduced that the maximum feeding distance of the smaller or "parasite" plates of a given thickness increases as the thickness of the larger or parent plate is increased. Further, the feeding distance in all parasite plates, regardless of thickness, is essentially the same when joined to a parent plate of a given thickness. The maximum feeding distances in parasite plates are as great as, or exceed, those obtained when the plates are cast separately.

The improvement for the 4 to 1 thickness combination amounts to approximately 200 per cent (4½ in. to 14 in). As the thickness ratio is decreased, by increasing the thickness of the parasite plate, the improvement in feeding distance decreases until for the 4 to 3-in. combination the maximum feeding distance of the 3-in. plate is essentially the same as that obtained in a separately cast plate (4½T). As the parasite plate approaches the thickness of the parent plate its feeding distance falls below 4½T inasmuch



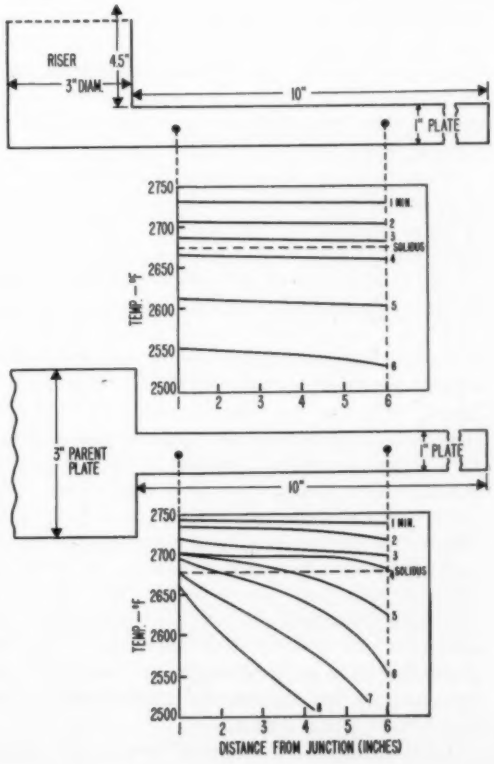
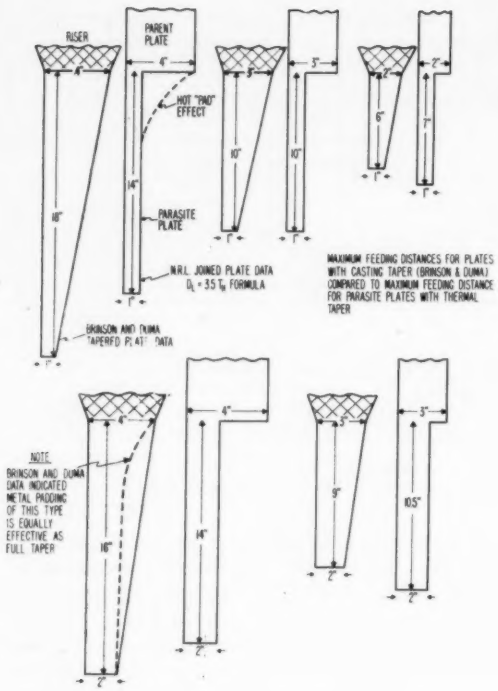


Fig. 30 (above)—Comparison of riser and parent plate thermal effects.

Fig. 31 (right)—Thermal gradient conditions developed in a 1-in. thick plate due to a conventional riser and to a 3-in. thick parent plate.



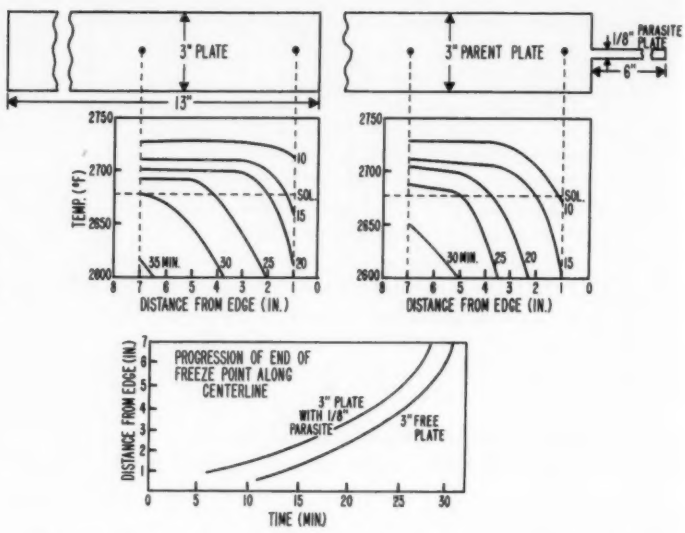


Fig. 32 (left)—Comparison of the effect of metal taper and thermal taper.

Fig. 33 (above)—Illustrating the mild chill effects of thin parasite plates.

as the combined feeding distances of parent and parasite plates necessarily approach the $4\frac{1}{2}$T value of a plate of uniform thickness.

The increase in feeding distance of the thinner sections is countered, in part, by a decrease in the feeding distances of the thicker or "parent" plates. The feeding distances in intermediate plates, which act both as parent and parasite, are governed by the resultant effects of the parent plate which tends to increase the distance and of the parasite plate which tends to reduce it. As a result the feeding distances of intermediate plates fall between the values obtained when the plate serves as strictly a parent or a parasite type.

When parent plates exceed the thickness of parasite plates by a ratio of 1.4 or greater the parent plate is more effective in promoting feeding in the parasite plate than a conventional round riser. This is ascribed to the more pronounced thermal effects produced in the parasite plate by the re-entrant angle which covers the entire plate width. With risers the re-entrant angle is limited to a smaller contact area which moreover is of lesser heat intensity due to lateral heat loss into the surrounding cold sand.

Figure 30 illustrates the thermal conditions which have been described. Figure 31 which presents the gradients developed in a separately cast 1-in. thick plate and the same plate joined to a 3-in. thick plate illustrates the respective thermal effects of the riser and of the parent plate. It is observed that relatively steep gradients are developed in the region which is essentially gradientless for the separately cast plate and that the solidification time at a position 1 in. from the riser or parent plate is increased from approximately $3\frac{1}{2}$ min for the riser system to 7 min for the joined plate system.

Such pronounced thermal effects are required to produce the marked increase in feeding distances which are observed; in fact the thermal effects developed at the junction area are essentially equal to those produced by the use of metal taper. This is demonstrated by comparison of the feeding distances of parasite plates joined to parent plates with the feeding distances obtained by Brinson and Duma[17] using metal taper, Fig. 32. The comparisons indicate that metal taper is more efficient than thermal taper only for the most extreme conditions.

The mild chill effect predicted for very thin parasite plates is demonstrated in Fig. 33. A $\frac{1}{8}$-in. parasite plate joined to a 3-in. parent plate promotes an increased feeding distance of approximately 1 in. in the parent plate. This indicates that thin sections have relatively insignificant thermal or feeding distance effects on heavy sections. From a practical viewpoint of riser positioning or dimensioning the inter-effects of sections of great thickness difference may be neglected. Thus, V/SA calculations need not account for the presence of thin members of complex castings.

It is apparent from the extensive feeding distances which may be obtained by joining plates in a series of decreasing thickness and from the effects of thermal tapers produced by metal or hot sand padding, that the solidification characteristics of the casting determine the distances which risers feed to soundness. The feeding distance which is promoted directly as the result of riser thermal effects are comparatively minor. The term "feeding range of risers" should be recognized therefore as having little significance unless qualified to specific casting systems. Thus, risers must be considered primarily as reservoirs of liquid metal having great potential feeding capacity which may or may not be made use of depending upon the solidification characteristics of the casting.

The basic concepts of solidification relationships to

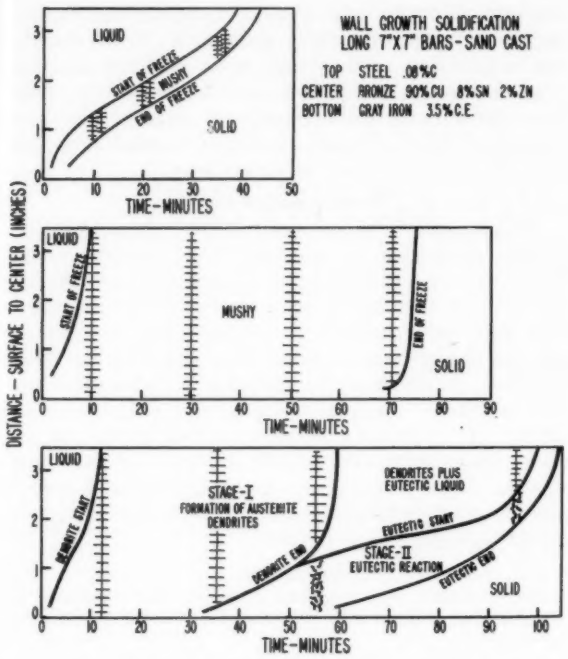


Fig. 34—Solidification curves for sand cast 20 x 7 x 7-in. bars of various metals.

feeding may be applied to the case of complex "problem" castings by thermal analysis of critical locations of such castings. The necessary thermal data are readily obtained by the insertion of a small number of thermocouples at the critical areas. This procedure should permit considerable savings of time and money in contrast to the process of repeated pouring of castings with haphazard variations of risering and gating details arrived at intuitively.

Extension of Feeding Concepts to Other Metals

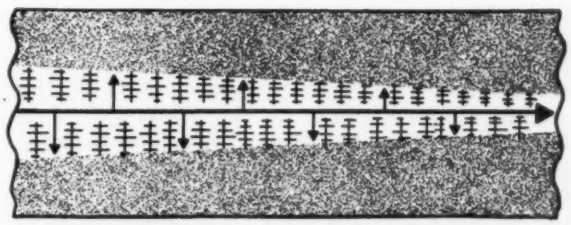
The development of the relationships between feeding and solidification characteristics permits a qualitative analysis of the behavior of metals other than the 0.20-0.30 per cent C steels. Exploratory investigations of various other metals have shown that the differences in feeding characteristics are predictable from consideration of the mode of solidification. In this respect the solidification diagram depicting the nature of the progression of "start" and "end of freeze" waves provides the basic information required.

Figure 34 presents wall growth solidification curves of various metals representative of possible deviations from the solidification characteristics of 0.25-0.30 per cent steel. The low carbon steel represents a metal characterized by highly progressive wall growth, i.e., by narrow mushy regions and early growth of a completely solid skin. The bronze alloy represents the opposite case of a metal characterized by slightly progressive wall growth, i.e., by development of a general mushy condition throughout the casting and with very late growth of a completely solid skin. The hypoeutectic gray iron represents two-step solidification entailing initial development of a general mushy condition and final solidification by highly progressive eutectic formation featuring the anomalous characteristic of expansion during the liquid-solid reaction rather than the shrinkage shown by most metals.[8,18]

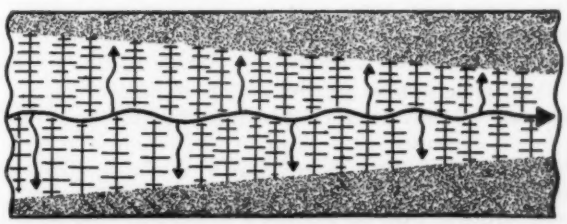
The highly progressive solidification features of the low-carbon steel are due primarily to the narrow temperature range of solidification, inasmuch as the thermal constants of the two steels are essentially the same. The feed metal channels developed as the result of this mode of freezing are relatively free as compared to the 0.30 per cent C steel (Fig. 35); accordingly flow of feed metal is much easier, particularly at late stages of solidification. Steels of this type feed to longer distances than the 0.30 per cent C steels prior to developing centerline shrinkage; for example the feeding distance of 2-in. plates was increased approximately 30 per cent. This suggests that gradients of lower value than required for the 0.30 per cent C steel provide effective feeding for metals which solidify in a highly progressive fashion.

The exaggerated mushy freezing of the bronze results primarily from the very high conductivity of the metal which effectively restricts the thermal gradients established during solidification to very low values. This feature coupled with the very wide temperature range of solidification dictates that the condition of solidification at the surface is never greatly in advance of that at the center. Such a metal becomes completely mushy from surface to center for a period which may be as long at 90 per cent of the total solidification time. Gas pressure bleeding tests of the type shown in Fig. 3 have corroborated the thermal analysis indications. Solidification during this period proceeds by gradual thickening of the solid dendrites until very little liquid remains.

When the final freezing wave leaves the surface there is very little liquid remaining to solidify with the result that the "end of freeze" wave proceeds at high rates (approximately 2 in. per minute indicated



STRONGLY PROGRESSIVE SOLIDIFICATION RESULTS IN FREE CHANNELS CONDUCIVE TO EASY FEEDING



SLIGHTLY PROGRESSIVE SOLIDIFICATION RESULTS IN HINDERED CHANNELS FEEDING IS DIFFICULT

Fig. 35

by Fig. 34). The advanced state of solidification prevents effective flow of feed liquid at the high rates demanded by the rapidity of movement of the "end of freeze" wave, and accordingly lateral shrinkage channels are developed throughout the casting. This feature accounts in part for the lack of pressure tightness which characterizes these alloys. The fact that centerline shrinkage is not developed in bronzes of this type may be ascribed to the development of lateral shrinkage cavities which in effect prevents the concentration of shrinkage to the centerline.

Gross shrinkage cavities are readily formed in bronzes of this type, however, the development of the shrinkage cavity is usually accompanied by a puncture to the surface since a completely solid skin is not developed until almost to the end of solidification. If a puncture does not result, there is a tendency to develop "dishing" over the shrinkage area due to the atmospheric pressure over the weak semi-solid skin. Feeding range studies based on pressure tightness evaluations have shown[19] that gradients having a minimum value of 25°F/in. are required for feeding to soundness. Such gradients may be obtained on uniform sections only by use of tapered chills placed parallel (short range use) to the path of directional solidification. End face chills such as described previously (long range use) are ineffective, thus confirming that the concepts of proper chill utilization (short range action) described for steel are fully applicable to other metals.

The most significant aspect of the complex solidification mechanism of hypoeutectic gray iron is the two-stage solidification cycle. The first stage, dendrite formation, is similar to that of steel in that it requires feed metal to compensate for liquid-solid shrinkage; the second stage, the eutectic reaction, does not require feed metal inasmuch as formation of the low density graphite phase results in a net volume increase. The two cycles do not follow in immediate sequence—to the contrary the first cycle essentially goes to completion throughout the entire casting before an appreciable progression of the second cycle is developed.

Unlike the cast of steel for which feed metal flow is always counter to the path of directional solidification, the flow of feed metal in the case of iron is reversed during the eutectic period, resulting in purging through the riser if open, or in an expansion of the casting if the riser is solidified. Thus, while at intermediate and late stages of solidification steel castings tend to drain heavy sections to satisfy the feed metal requirements of the lighter sections, gray iron castings actually pump liquid back from the light to the heavy sections.

The sufficiency of small bobs for feeding of rangy sections of gray iron is explained by this freezing process inasmuch as a positive flow of feed metal from a feed source is only required during the early stage of freezing. In fact, feeding need not be complete during this period since a feeding deficiency will permit room for subsequent relief of the eutectic reaction expansion. It is also apparent that centerline shrinkage should not be possible with this mode of freezing. The feeding range of gray iron sections of uniform thickness with respect to centerline shrinkage is therefore essentially unlimited. Gross shrinkage in gray iron castings may occur as the result of a gross feeding deficiency for the case of chunky castings, particularly for hypoeutectic types of low carbon equivalent; adequate risering is needed as in the case of steel to counter this deficiency.

Acknowledgments

This report is a summary of the comprehensive studies conducted by Messrs. H. F. Bishop, E. T. Myskowski, F. A. Brandt, R. P. Dunphy and W. H. Johnson of this Laboratory. It should be recorded that their supervision of the research, and their analysis and interpretation of the data provided the basis for this contribution to foundry technology. The administrative support and active technical interest of Dr. O. T. Marzke, Superintendent of the Metallurgy Division, made possible and encouraged these studies. The preparation of the illustrations represents a valuable contribution by Mr. E. E. Layne, also of the foundry research staff.

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THE STUDY OF VERTICAL GATING

Progress Report

By

K. Grube, J. G. Kura, and J. H. Jackson*

ABSTRACT

Sprues with rectangular cross-sections which enter the bottom of the casting seem to be preferable for vertical gating of simple plates made in small molds. For heavier plates made in large molds, a tapered sprue offers some advantages. In general, the use of a tapered sprue appears to permit the use of higher flow rates with a minimum of turbulence, and it is believed that when tests are made the use of a tapered sprue will be universally beneficial. These are the essential findings of the first seven months of the current research program on vertical gating systems sponsored at Battelle by the Research Committee of the Light Metals Division of A.F.S.

The research program on vertical gating systems is now in its preliminary stages. It is directed toward a study of conventional, vertical gating systems used in small and large molds. To date, two plate castings have been studied. One is ½ in. by 6 in. by 12 in. (giving a casting weight of about 2¼ lb of magnesium, 3½ lb of aluminum, or 10¼ lb of iron). The second is 2 by 6 by 12 in. (giving a casting weight of about 9 lb of magnesium, 14 lb of aluminum, or 41 lb of iron), simulating a larger mold.

Vertical sprues of six different basic designs were studied with the smaller mold. Three of these permitted the liquid to enter the mold at one side edge near the bottom. With the other three, the liquid entered the bottom of the mold near one edge. The effect of circular versus rectangular cross-section was studied for each of these six basic sprues.

Two basic sprue designs were examined for the larger mold. Both permitted the liquid to enter the mold at one side edge near the bottom. One of the sprues was tapered and had a rectangular cross-section. The other was a simple type used to study the effect of circular versus rectangular cross-section.

As in the previous A.F.S. gating research, the studies are being conducted on Lucite molds using water as the flowing liquid. Photographic methods are used to record the results.

Little over-all difference has been found with the different sprue systems. The tapered sprue for the large mold and the rectangular sprues for the small mold showed some advantage in reducing turbulence. The rectangular sprue which delivered the liquid through the mold bottom was somewhat superior to the side-entry sprues in reducing gas entrapment, perhaps because of the lower flow rate achieved by means of this sprue design.

This research will be continued with investigations of other typical types of gating designs. Where indicated, the gating designs will oe modified in an attempt to determine the optimum methods to reduce turbulence and gas entrapment.

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† This is the fourth progress report on a research project conducted at Battelle Memorial Institute, initiated and sponsored by the Light Metals Division of A.F.S. with some financial assistance of Battelle Memorial Institute. The project was under the general guidance of the Research Committee of the Light Metals Division which consisted of the following members: W. E. Sicha, *Chairman*, W. Bonsack, *Co-Chairman*, R. F. Thomson, C. E. Nelson, T. D. Stay, W. J. Klayer, S. C. Massari, and J. G. Mezoff.

Introduction

Previous research[1,2,3,4] using transparent models and transparent liquid indicated that useful design information could be obtained, which leads to improved gating procedures. This previous research has been limited to gating of horizontal castings. All of the possibilities for improving horizontal gating systems have not been exhausted. However, it is the opinion of all concerned that many of the basic principles for these systems have now been established. Any additional effort would probably be more effective in establishing basic principles for the vertical gating systems. Therefore, the Light Metals Research Committee† requested that a research program be initiated to study vertical gating as it applies to (1) permanent molds, and (2) shell molds.

Although the general procedures for the production of permanent-mold castings, and applications of such castings, have been discussed in the literature,[5] little has been published on vertical gating for this field. Shell molding is new and attention in the published literature has been devoted almost entirely to sand-bonding practices. Considerable work has been done at the Naval Research Laboratory[6,7] on vertical gating of sand castings, and a review of this work was helpful in outlining the present research program.

Of greatest value in planning the present program have been the general comments of the Light Metals Research Committee; the comments of L. W. Eastwood, assistant director of research at Kaiser Aluminum and Chemical Corporation; and plant visits to Acme Aluminum Alloys, Inc., at Dayton, Ohio, and Aluminum Industries, Inc., at Cincinnati. Other plant visits are planned.

Information gained from the literature and from the plant visits suggested that a complete study of all possible vertical gating systems would be far too extensive for the proposed research. Some 178 possible designs were considered. Each of these designs could

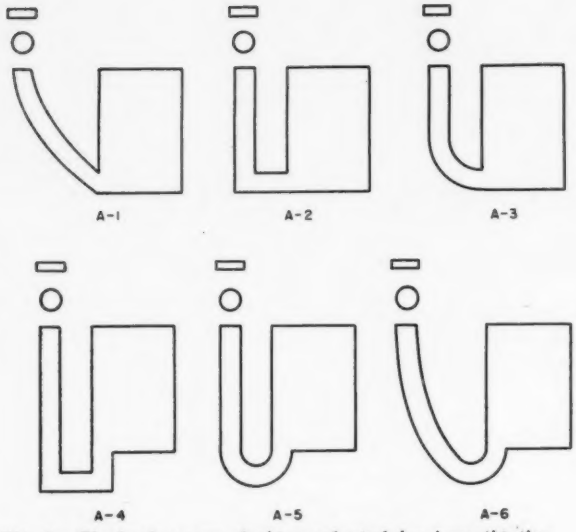


Fig. 1—Six basic sprue designs selected for investigation of vertical gating in permanent molds.

be amended to permit different degrees of tapering, different cross-section geometry, and other configurations. Other new designs would undoubtedly be developed as the research progressed. To reduce the magnitude of the problem to one which could be reasonably handled, it was decided to investigate basic designs first. Thereby, principles would be established to limit the total number of designs to be covered.

At the inception of this program, it was decided to investigate, first, six basic sprue designs as used with a simple, thin, plate-type casting. Sprue systems of interest would also be checked on a heavier plate casting.

This report describes work done on vertical gating during the period from October 1, 1952, to April 30, 1953.

Experimental Work

Equipment—In this research on vertical gating, it was desired to employ an inexpensive method of constructing the molds and sprues. At first, it was believed that molds could be constructed from plaster and Lucite sheet. However, as the research progressed, it became clear that this method was not practical where repeated tests were involved. Therefore, it was decided to construct the two molds from Lucite sheet. The sprues were made either of glass tubing or laminated Lucite sheet. By these procedures, equipment was constructed which was relatively inexpensive and had good clarity for visual observation and photographic purposes.

Two molds were made of Lucite sheet. One mold had a cavity ½ in. thick, 6 in. wide, and 12 in. long. The other mold had a cavity 2 in. thick, 6 in. wide, and 12 in. long to represent a heavier casting. The sides of the molds were machined from ½-in. Lucite plate and were fitted with ¼-in.-thick Lucite sheet. Openings to accommodate either round or rectangular sprues were provided on one side edge near the bottom, and on the bottom near one side edge. When one opening was in use the other was sealed. To provide venting, holes were drilled across the top of each Lucite mold.

Six designs of sprues were selected as a basis for the start of the investigation. These designs are shown in Fig. 1, and are identified as A-1 through A-6. This code will be employed throughout this report for ease of identification. Likewise, the term "sprue" will apply to the channel through which the liquid passes prior to its entry into the mold.

Each of the six types of sprues were investigated with both a circular and a rectangular cross-section. The circular sprues for the small mold were formed from glass tubing having an inside diameter of 0.375 sq in., and thus had a cross-sectional area of 0.110 sq in. The rectangular sprues were formed from laminated Lucite sheet. The sprue cavity was ⅛ in. by 1 in. and had a cross-sectional area of 0.125 sq in. All sprues, both circular and rectangular, had straight walls, i.e., they were not tapered.

The sprues with these cross-sectional areas filled the small mold at a rate of approximately 0.70 lb of aluminum per second. Variation in sprue design caused this rate to fluctuate slightly.

It is realized that a pouring box is of little or no use by those employing vertical gating such as in the permanent mold shop. However, in the laboratory, the pouring box was used and provided a means of introducing water free of air bubbles at a constant pressure into the sprue. Thus, air entrainment encountered would be a function of the gating design. Each test was started with a 2-in. head of water in the pouring box. This level was maintained throughout the test.

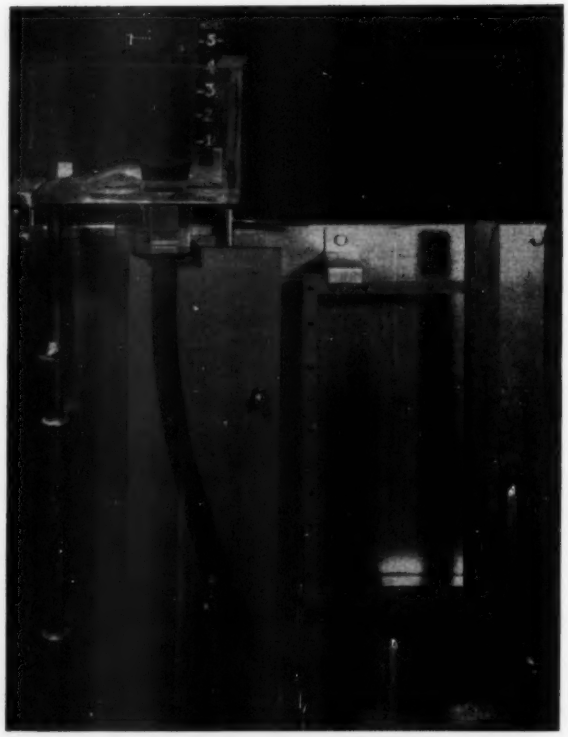


Fig. 2—Photo showing set-up used to investigate sprue design for vertical gating.

The set-up used in the study of vertical gating is shown in Fig. 2. A portion of the pouring box, stopper, the A-6 sprue with rectangular cross-section, and the small mold are shown in the photograph.

Small Mold

Circular—Each of the six circular sprues, 0.375 in. in diameter, were tested with the small mold. The degree of turbulence occurring in the mold was observed as well as the quantity of entrapped air remaining in the mold after the cavity was filled. Since quantitative data with this type of test are difficult to obtain, a photographic record was made of each sprue design during pouring.

Kenny Speed-O-Light pictures were taken at ½- and 2-sec intervals from the start of pour. Also, a photograph was taken just as the mold filled. The filling time varied from 4.6 to 5.0 sec, depending upon the sprue design.

Recorded in Fig. 3B, 3C, and 3D are the flow characteristics obtained with Sprue Design A-1. Turbulence is evident in the mold during the initial pour. The jet action of the side-entry sprue causes air to be entrapped in the liquid. As the mold fills, turbulence is overcome by back pressure. However, the air bubbles entrapped during the initial pour remain in the mold cavity throughout the pour.

The action of the two other side-entry sprues, A-2 and A-3, was very similar to that recorded for the Sprue Design A-1 and are shown in Figs. 4B, 4C, 4D, 5B, 5C, and 5D.

The action of the three sprues designed to permit the liquid to enter the mold from the bottom was studied. Typical results obtained with Design A-4 are shown in Figs. 6B, 6C, and 6D. Results obtained with the A-5 and A-6 designs were similar. The action of these two sprues is recorded in Figs. 7B, 7C, 7D, 8B, 8C, and 8D.

The sprues entering the mold from the bottom created a fountain effect. This turbulent condition entrapped large quantities of air in the liquid. As in the case of the side-entry sprues, these air bubbles were dispersed throughout the mold cavity during the pour. Photographs taken at the end of the pour showed the bubbles trapped along the side of the mold opposite the sprue. There appeared to be some reduction in the degree of turbulence at the end of the pour for the bottom sprue entry. In all cases, with the circular sprue, air bubbles were entrapped within the water casting at the end of the pour.

Rectangular Sprues—Six designs of rectangular sprues, ⅛ in. thick by 1 in. wide, were tested with the small mold. Three of the sprues entered the mold from the side and three from the bottom. None were tapered. Action pictures were taken at ½- and 2-sec intervals. A third photograph was taken just as the mold filled. This end period varied from 5.6 to 7.8 sec, depending upon the design of the sprue.

Although the rectangular sprues had slightly greater cross-sectional area than the round sprue, there was a tendency for slightly lower flow rates. Swift, *et al*,[1] in their work on sprue design, found that the rectangular sprue offered more resistance to flow that did the round sprue.

The action in the mold, using the side-entry sprue Design A-1, is shown in Figs. 9B, 9C, and 9D. Similar results were obtained when Designs A-2 and A-3 with rectangular cross-sections were used. These are shown in Figs. 10B, 10C, 10D, 11B, 11C, and 11D. Because of the slightly lower flow rate with the rectangular sprues, as compared with the circular sprues, there appeared to be a lesser amount of turbulence occurring during the initial part of the pour. As the mold was filled, there was a considerable number of air bubbles entrapped in the casting. The slightly reduced flow rate achieved by the rectangular-shaped sprue was not sufficient to permit all of the entrapped air to escape.

In the sprue design shown in Fig. 10A, an abrupt change in direction of flow occurs. As a result of this abrupt change in direction, an air pocket was created on the cope surface of the horizontal portion of the sprue, as shown in Figs. 10B, 10C, and 10D. This defect should eliminate this design from any further investigation. With curved sprues, this condition did not exist.

Presented in Figs. 12B, 12C, and 12D are photographs taken of the action of one of the bottom-entry sprues, Design A-4. Considerable turbulence occurred during the initial surge of the liquid into the mold. When the bottom-entry type of sprue utilizing a rectangular cross-section was employed, only a small quantity of entrapped air remained in the cavity after the mold was filled. Similar results were also obtained with Designs A-5 and A-6, as shown in Figs. 13B, 13C, 13D, 14B, 14C, and 14D.

A comparison of the circular sprue with the rectangular sprue showed that there was little general difference in the flow characteristics of the liquid delivered to the mold cavity. Of course, it must be appreciated that flow within the mold will differ depending upon the type of sprue entry. The rectangular sprue retarded the flow rate slightly and, as a result, somewhat less turbulence was experienced. The degree of slope to the sprue did not appear to affect the amount of turbulence.

Large Mold

Three sprue designs were selected for testing with the large mold which measured 2 by 6 by 12 in. A circular sprue, Design A-3 in Fig. 1, was made from ½-in.-diam glass tubing. The cross-sectional area was 0.196 sq in. The sprue was not tapered and contained vent holes along one side to provide permeability.

Flash pictures presented in Figs. 15A, 15B, and 15C illustrate the action in the mold when poured with the circular sprue. The familiar jet action observed with the smaller sprues and smaller mold was again present. This is evident in Fig. 15A. The dispersion of the air bubbles throughout the partially filled mold cavity is shown in Fig. 15B. When the mold became completely filled, Fig. 15C, a number of very small air bubbles were still entrapped in the mold cavity.

A straight, rectangular sprue, 3/16 in. thick by 1 in. wide, having a cross-sectional area of 0.187 sq in., was the second sprue tested with the larger mold. The

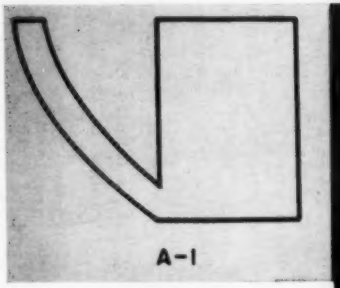


Fig. 3A

Sprue Diam—0.375 In.
Sprue Area—0.110 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —2.22 Lb/Sec
Aluminum —0.76 Lb/Sec
Magnesium —0.50 Lb/Sec



Fig. 3B—½ Sec



Fig. 3C—2 Sec



Fig. 3D—4.6 Sec

Fig. 3—Circular Sprue—Design A-1—Small Mold.

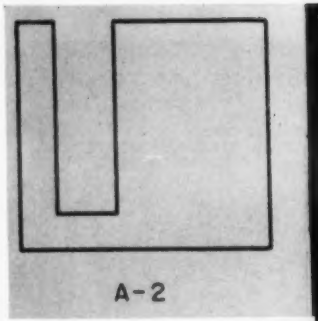


Fig. 4A

Sprue Diam—0.375 In.
Sprue Area—0.110 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —2.04 Lb/Sec
Aluminum —0.70 Lb/Sec
Magnesium —0.46 Lb/Sec



Fig. 4B—½ Sec



Fig. 4C—2 Sec

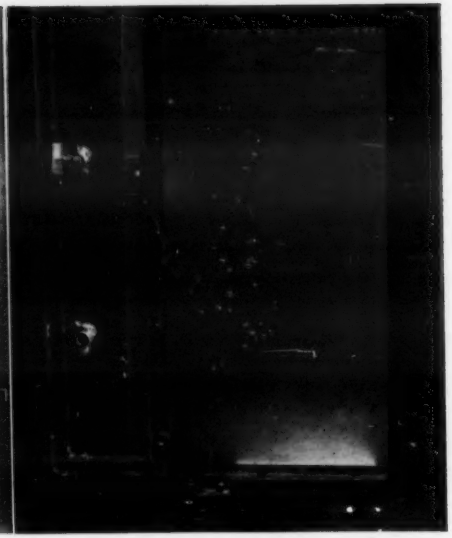


Fig. 4D—5.0 Sec

Fig. 4—Circular Sprue—Design A-2—Small Mold.

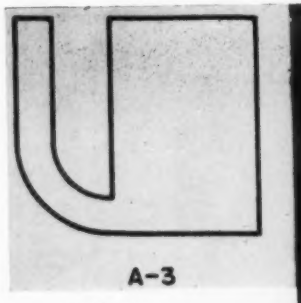


Fig. 5A
Sprue Diam—0.375 In.
Sprue Area—0.110 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —2.04 Lb/Sec
Aluminum —0.70 Lb/Sec
Magnesium —0.46 Lb/Sec

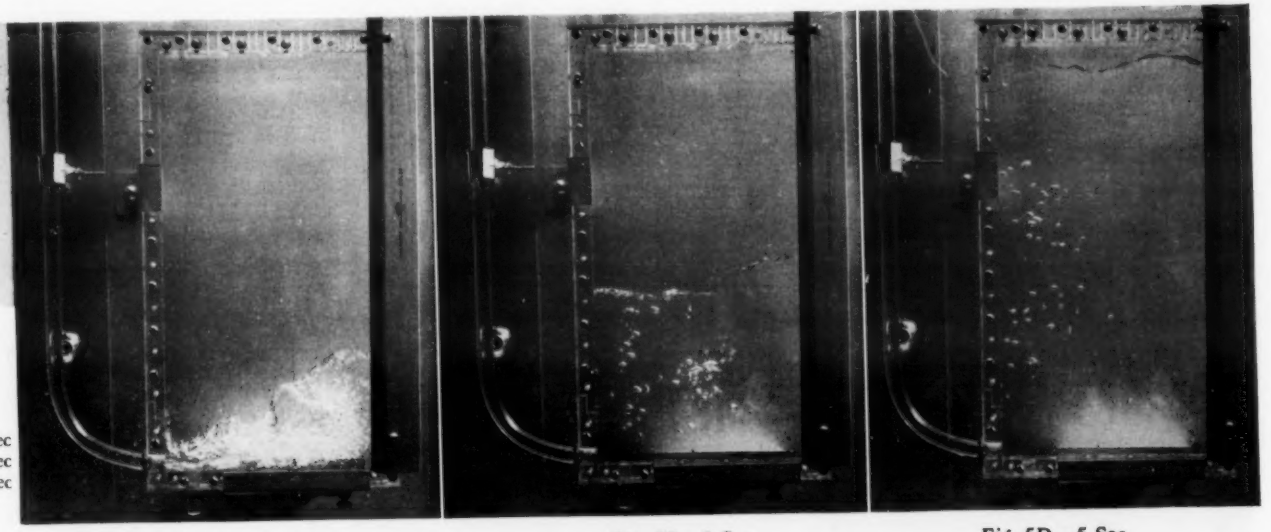


Fig. 5B—½ Sec *Fig. 5C—2 Sec* *Fig. 5D—5 Sec*

Fig. 5—Circular Sprue—Design A-3—Small Mold.

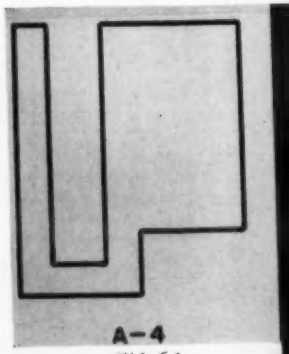


Fig. 6A
Sprue Diam—0.375 In.
Sprue Area—0.110 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —2.04 Lb/Sec
Aluminum —0.70 Lb/Sec
Magnesium —0.46 Lb/Sec

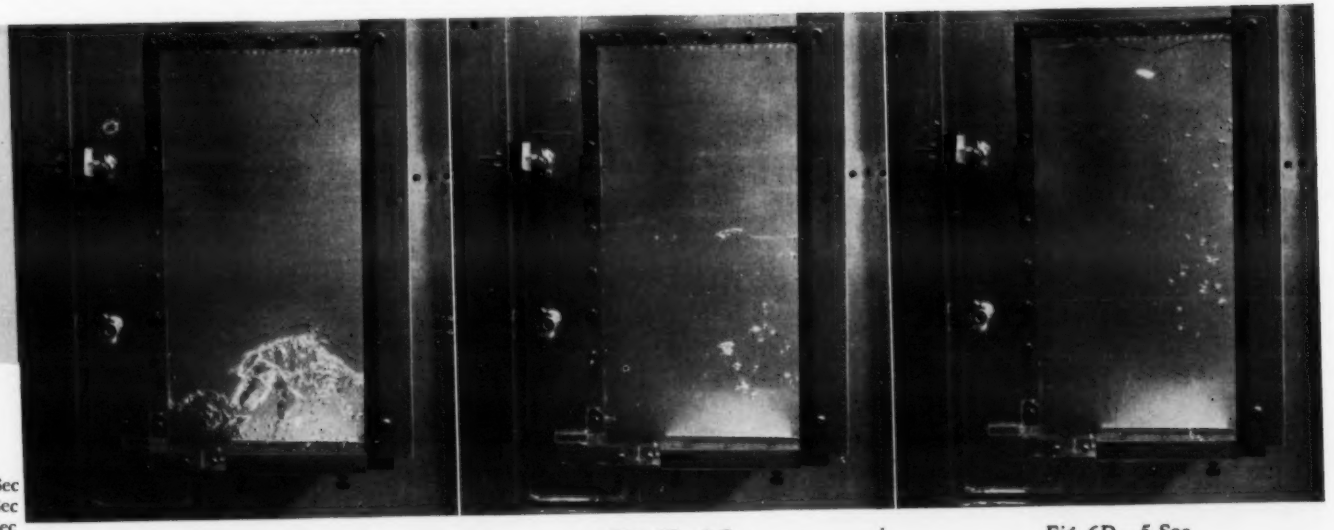


Fig. 6B—½ Sec *Fig. 6C—2 Sec* *Fig. 6D—5 Sec*

Fig. 6- -Circular Sprue—Design A-4—Small Mold.



Fig. 7A

Sprue Diam—0.375 In.
Sprue Area—0.110 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —2.04 Lb/Sec
Aluminum —0.70 Lb/Sec
Magnesium —0.46 Lb/Sec

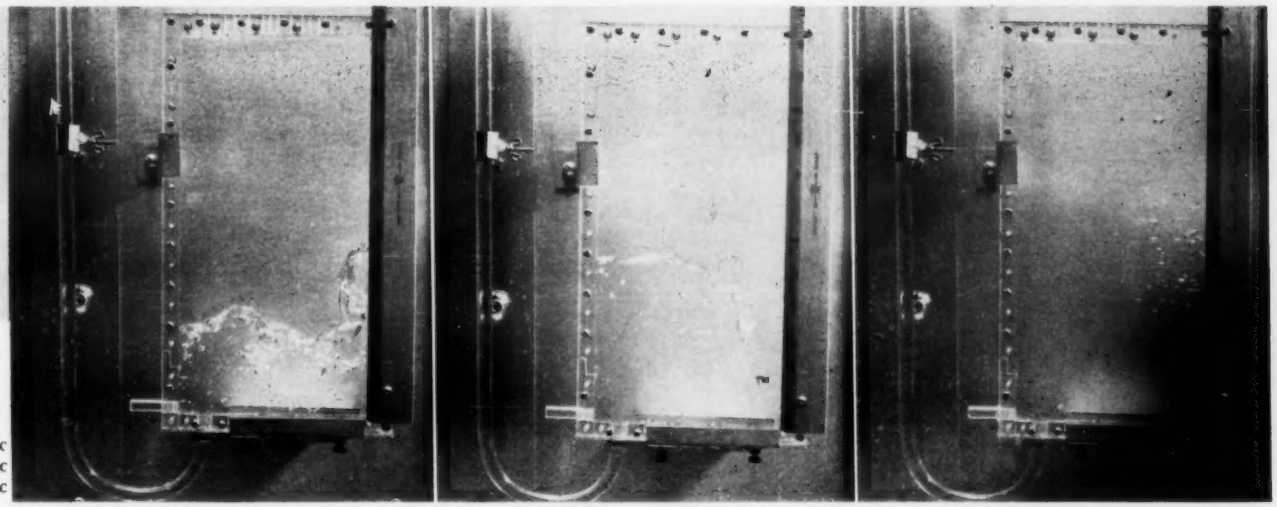


Fig. 7B—½ Sec *Fig. 7C—2 Sec* *Fig. 7D—5 Sec*

Fig. 7—Circular Sprue—Design A-5—Small Mold.

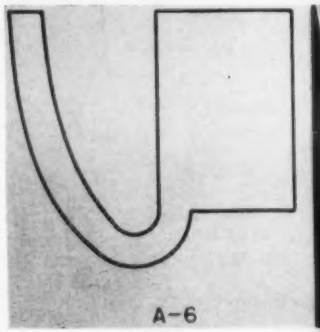


Fig. 8A

Sprue Diam—0.375 In.
Sprue Area—0.110 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —2.04 Lb/Sec
Aluminum —0.70 Lb/Sec
Magnesium —0.46 Lb/Sec

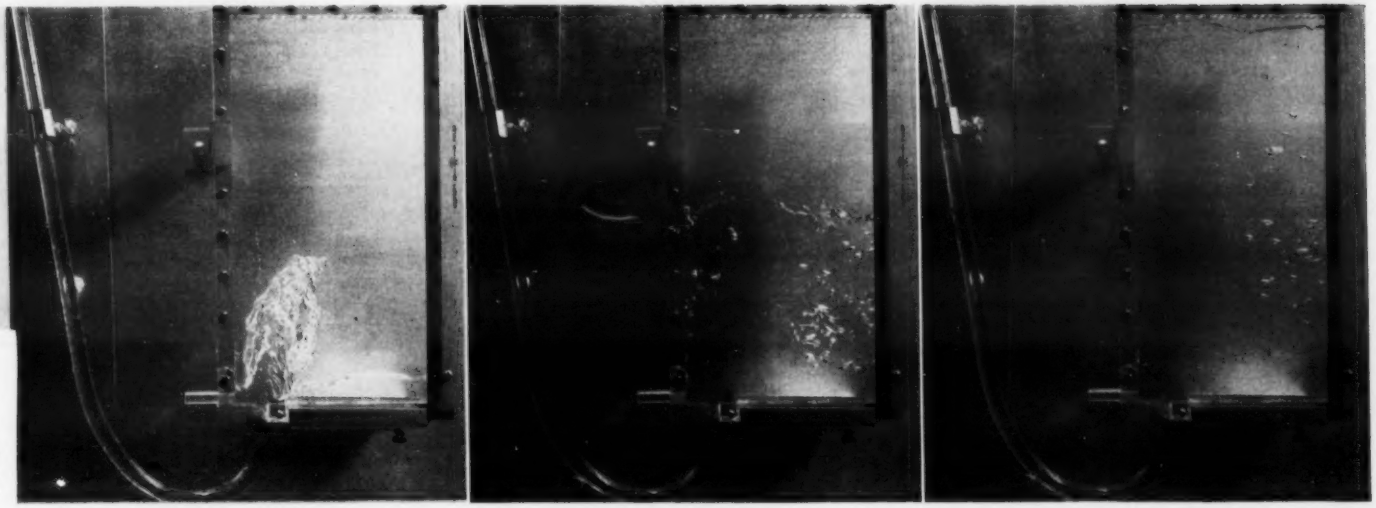


Fig. 8B—½ Sec *Fig. 8C—2 Sec* *Fig. 8D—5 Sec*

Fig. 8—Circular Sprue—Design A-6—Small Mold.

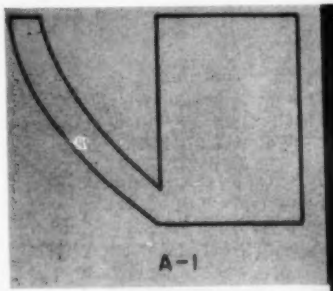


Fig. 9A

Sprue Dim—⅛x1 In.
Sprue Area—0.125 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —1.83 Lb/Sec
Aluminum —0.63 Lb/Sec
Magnesium —0.40 Lb/Sec



Fig. 9B—½ Sec

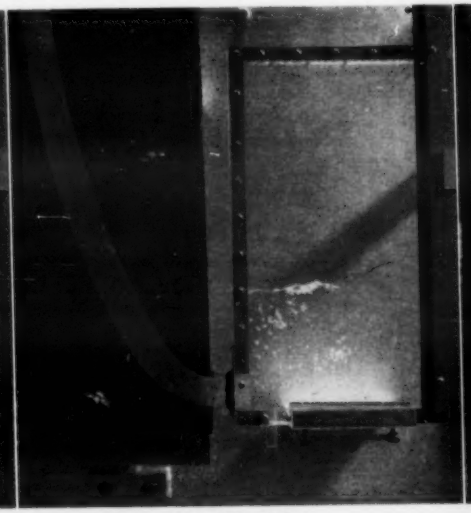


Fig. 9C—2 Sec



Fig. 9D—5.6 Sec

Fig. 9—Rectangular Sprue—Design A-1—Small Mold.

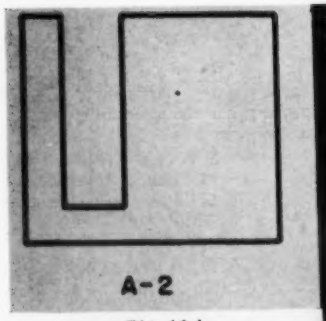


Fig. 10A

Sprue Dim—⅛x1 In.
Sprue Area—0.125 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —1.83 Lb/Sec
Aluminum —0.63 Lb/Sec
Magnesium —0.40 Lb/Sec

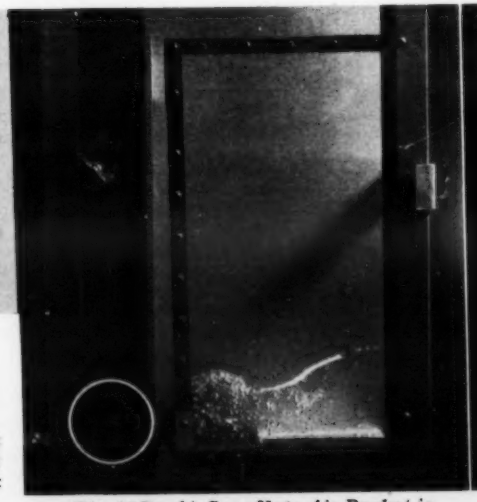


Fig. 10B—½ Sec. Note Air Pocket in Runner Near Gate Opening.

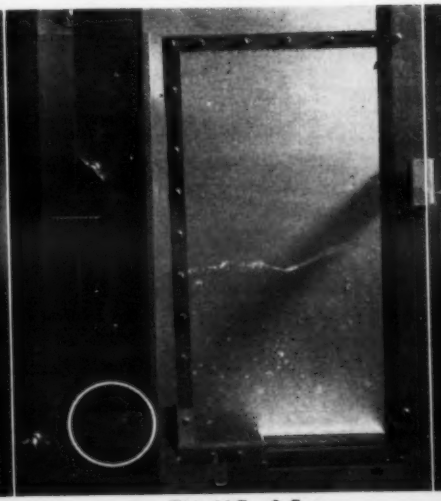


Fig. 10C—2 Sec

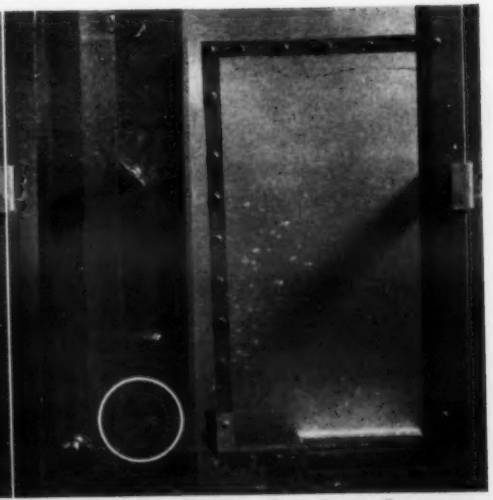


Fig. 10D—5.6 Sec. Note Large Quantity of Entrapped Air Bubbles as Mold Fills.

Fig. 10—Rectangular Sprue—Design A-2—Small Mold.

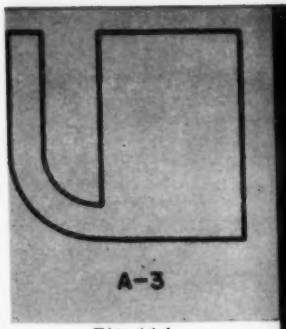


Fig. 11A

Sprue Dim—1/4x1 In.
Sprue Area—0.125 Sq In.
Mold—1/2x6x12 In.
Calculated Flow Rate—
Iron —1.83 Lb/Sec
Aluminum —0.63 Lb/Sec
Magnesium —0.40 Lb/Sec



Fig. 11B—1/2 Sec



Fig. 11C—2 Sec



Fig. 11D—5.6 Sec

Fig. 11—Rectangular Sprue—Design A-3—Small Mold.

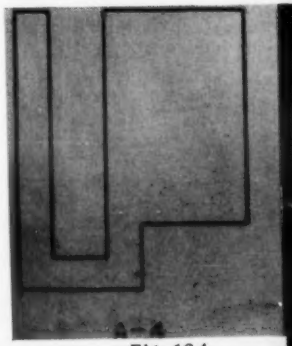


Fig. 12A

Sprue Dim—1/8x1 In.
Sprue Area—0.125 Sq In.
Mold—1/2x6x12 In.
Calculated Flow Rate—
Iron —1.31 Lb/Sec
Aluminum —0.45 Lb/Sec
Magnesium —0.29 Lb/Sec



Fig. 12B—1/2 Sec



Fig. 12C—2 Sec

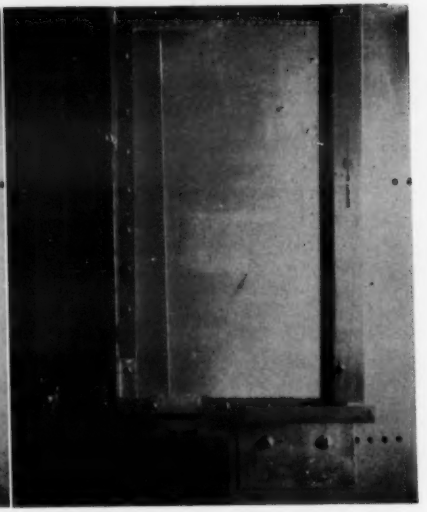


Fig. 12D—7.8 Sec

Fig. 12—Rectangular Sprue—Design A-4—Small Mold.



Fig. 13A

Sprue Dim—⅛x1 In.
Sprue Area—0.125 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —1.42 Lb/Sec
Aluminum —0.49 Lb/Sec
Magnesium —0.32 Lb/Sec



Fig. 13B—½ Sec



Fig. 13C—2 Sec



Fig. 13D—7.2 Sec

Fig. 13—Rectangular Sprue—Design A-5—Small Mold.



Fig. 14A

Sprue Dim—⅛x1 In.
Sprue Area—0.125 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —1.60 Lb/Sec
Aluminum —0.54 Lb/Sec
Magnesium —0.36 Lb/Sec



Fig. 14B—½ Sec



Fig. 14C—2 Sec

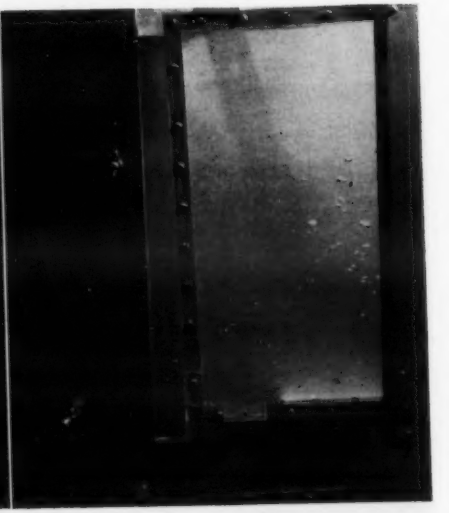


Fig. 14D—6.4 Sec

Fig. 14—Rectangular Sprue—Design A-6—Small Mold.

Sprue Diam—0.500 In.
Sprue Area—0.196 Sq In.
Mold—2x6x12 In.
Calculated Flow Rate—
Iron —3.72 Lb/Sec
Aluminum —1.26 Lb/Sec
Magnesium —0.83 Lb/Sec

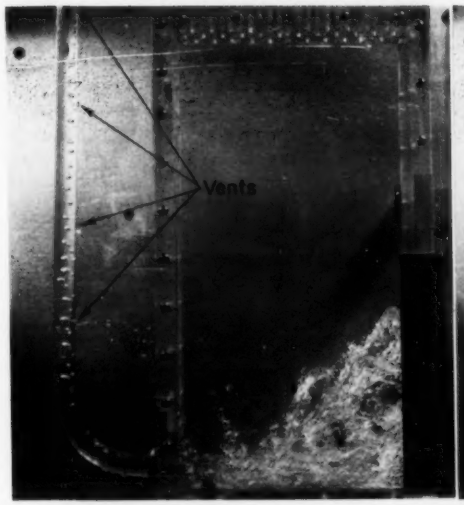


Fig. 15A—½ Sec. Note Air Bubbles in Sprue Due to Aspiration.

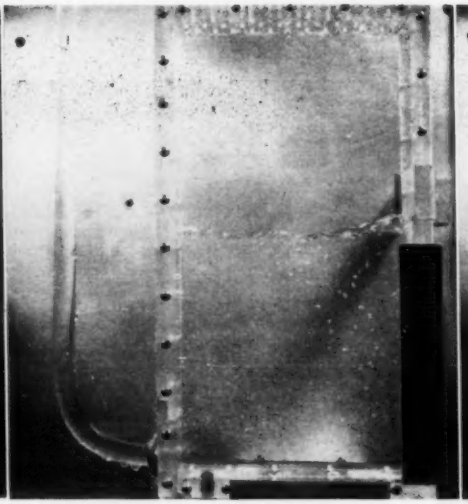


Fig. 15B—5 Sec. Back Pressure in Mold Has Eliminated Aspiration in Sprue.

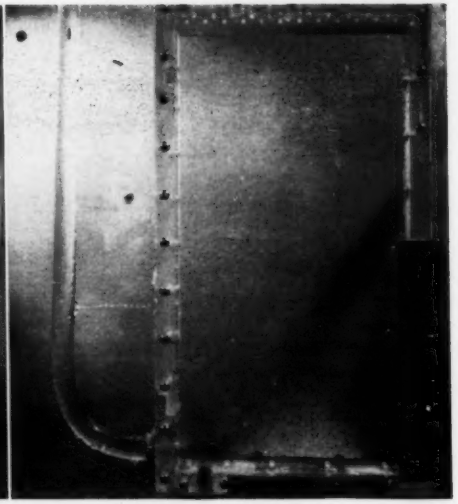


Fig. 15C—11.0 Sec.

Fig. 15—Circular Sprue—Design A-3—Large Mold.

Sprue Dim—3/16x1 In.
Sprue Area—0.187 Sq In.
Mold—2x6x12 In.
Calculated Flow Rate—
Iron —2.80 Lb/Sec
Aluminum —0.96 Lb/Sec
Magnesium —0.62 Lb/Sec

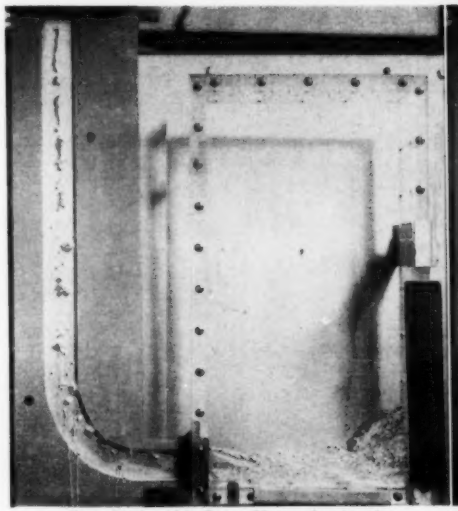


Fig. 16A—½ Sec. Note Aspiration in Sprue.

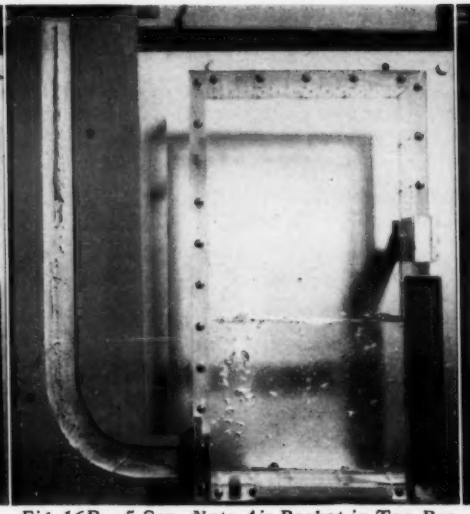


Fig. 16B—5 Sec. Note Air Pocket in Top Portion of the Sprue Caused by Aspiration.

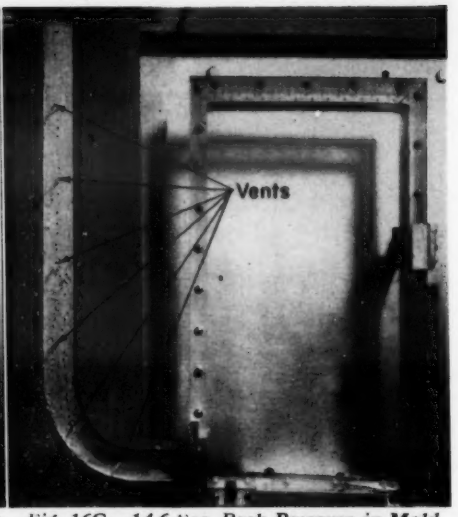


Fig. 16C—14.6 Sec. Back Pressure in Mold has Eliminated Aspiration and Water Drops are on the Outer Surface of the Sprue.

Fig. 16—Rectangular Sprue—Design A-3—Large Mold.

sprue design was of the type illustrated by A-3 in Fig. 1. Vent holes were drilled along one face of the sprue.

Illustrated in Figs. 16A, 16B, and 16C are photographs taken at ½-, 5-, and 14.6-sec intervals after the start of pour with the rectangular sprue. Again, the rectangular shape retarded the flow of the liquid. Consequently, there was a slightly lesser amount of turbulence during the initial pour. The photograph taken at the end of the pour, Fig. 16C, reveals that practically all of the air bubbles escaped from the liquid.

The third design tested was a tapered, rectangular sprue. It had a bottom dimension of ⅝ in. by ¼ in., with a cross-sectional area of 0.156 sq in. The sprue had a 3:1 taper and was tested with the large end up. Vent holes were drilled along one side of the sprue.

The flash pictures taken at ½-, 5-, and 17.2-sec intervals are presented in Figs. 17A, 17B, and 17C. Because this sprue had a smaller cross-sectional area at its bottom than the other two, the flow rate was substantially reduced. This lower flow rate appeared to reduce the quantity of air entrapped in the water during pouring. The photograph in Fig. 17C, taken as the mold completely filled, shows an absence of air bubbles.

From the data obtained on the three sprues tested with the larger mold, it appears that the future work should deal primarily with tapered sprues.

Aspiration of Air in Straight Sprues

It has been established in previous work[1,2,3] that straight sprues aspirate air into the liquid. Aspiration of air into molten metal would promote dross formation. In the earlier work, the sprues had a bottom cross-sectional area as small as 0.500 sq in. It was not known what effect a sprue with still smaller cross-sectional area would have on aspiration.

The gating designs used in permanent molds are generally of smaller cross-section than those employed in horizontal gating of sand molds. It was believed that small, circular or rectangular sprues might not present the aspiration problem, even though the sprue was not tapered. To establish if this was true, one sprue design, A-3 in Fig. 1, was selected for examination. This design was tested in both the circular and rectangular shapes and when attached to the small and to the large molds.

The circular sprue tested with the small mold had a cross-sectional area of 0.110 sq in., and the rectangular sprue had an area of 0.125 sq in. Vent holes were placed throughout the length of each sprue. It was found that each sprue aspirated air only for a small fraction of the total filling time. As soon as back pressure began to be exerted by the filling of the mold, aspiration ceased.

A circular sprue and rectangular, straight sprue were tested for aspiration when used with the larger mold. The circular sprue had a cross-sectional area of 0.196 sq in., while the area of the rectangular sprue was 0.187 sq in. Both sprues aspirated a considerable quantity of air until the mold was nearly full. Aspiration was eliminated when the rectangular sprue was tapered and had a bottom cross-sectional area of 0.156 sq in.

A bottom-entry, rectangular sprue design, A-4 in Fig. 1, was also tested for aspiration. This sprue had an area of 0.125 sq in. and contained two right-angle corners. Although an air pocket was formed when a sudden change in direction occurred in the sprue, there was no aspiration. The U-shape of the sprue created sufficient back pressure to prevent aspiration from occurring.

From these few tests on shape, design, and size of sprue, it appears that the casting size should be considered in relation to the design of the sprue. The data indicate that on small castings (up to 4 lb of aluminum) small-diameter, straight sprues may be used. Above this casting size, a tapered sprue would be more advantageous. If straight sprues are employed on large castings, the liquid should enter the mold cavity from the bottom.

Effect of Sprue Size on Turbulence

The experimental work employing the 0.375-in.-diam sprues indicated that large quantities of air were entrapped in the final casting. It was believed that a reduction in the pour rate would reduce the amount of entrapped air. Three methods were suggested for reducing the rate of flow. They were: (1) use a smaller diameter sprue, (2) use screens, and (3) use steel wool. The simplest and most practical of these methods was to use sprues of smaller diameter. Circular sprue Design A-3 was selected and two sprues were made from 0.156-in. and 0.312-in.-inside-diameter glass tubing. Vent holes were placed in the side of each sprue. As in previous work, the pouring box and stopper were employed as control devices.

Speed-O-Light pictures taken at ½-, 2-, and 16.2-sec intervals indicate the 0.156-in.-diam sprue did not create much turbulence. A small amount of aspiration occurred in the sprue during the initial part of the pour. However, as soon as a slight back pressure was created, aspiration ceased. The photographs shown in Figs. 18A, 18B, and 18C indicate freedom of air bubbles during the latter stages of filling. The calculated flow rate for this diameter sprue was 0.63 lb/sec of iron, 0.22 lb/sec of aluminum, or 0.14 lb/sec of magnesium.

Increasing the sprue diameter to 0.312 in. and, at the same time, increasing the flow rate to 1.25 lb/sec of iron, 0.43 lb/sec of aluminum, or 0.28 lb/sec of magnesium, created a considerable amount of turbulence. Time-lapse pictures taken at ½, 2, and 8.2 sec, shown in Figs. 19A, 19B, and 19C, indicate large quantities of air being entrapped in the final casting. Aspiration of air in this sprue continued for a longer period than in the smaller sprue and contributed to the defective casting.

Pouring Directly Down the Sprue

As mentioned previously, a pouring box was used in testing all of the sprue designs. This method of control was used to eliminate, as much as possible, extraneous variations in the tests. It was also realized that any vertical gating design which appeared to be particularly advantageous in the laboratory may eventually be used in commercial production without the aid of a pouring box. Therefore, one sprue design

Top Sprue Dim—1x9/16 In.
Bottom Sprue Dim—5/8x1/4 In.
Bottom Cross-Sectional Area—
0.156 Sq In.
Mold—2x6x12 In.
Calculated Flow Rate—
Iron —2.38 Lb/Sec
Aluminum —0.82 Lb/Sec
Magnesium —0.53 Lb/Sec

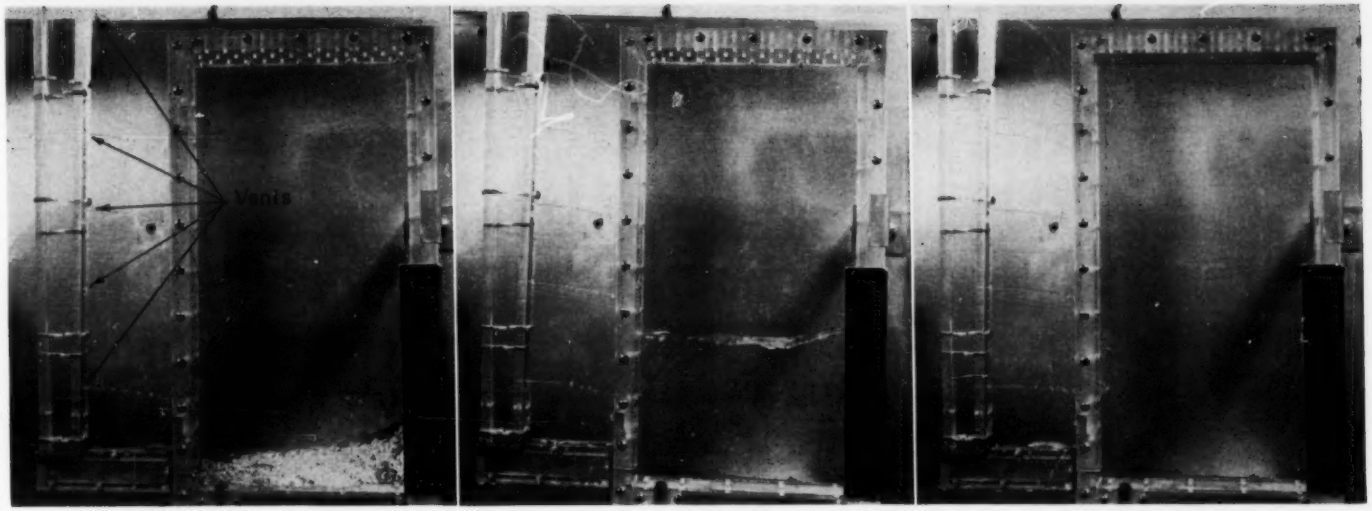


Fig. 17A—½ Sec. *Fig. 17B—5 Sec.* *Fig. 17C—17.2 Sec.*

Fig. 17—Tapered, Rectangular Sprue—Large Mold.

Sprue Diam—0.156 In.
Sprue Area—0.019 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —0.63 Lb/Sec
Aluminum —0.22 Lb/Sec
Magnesium —0.14 Lb/Sec

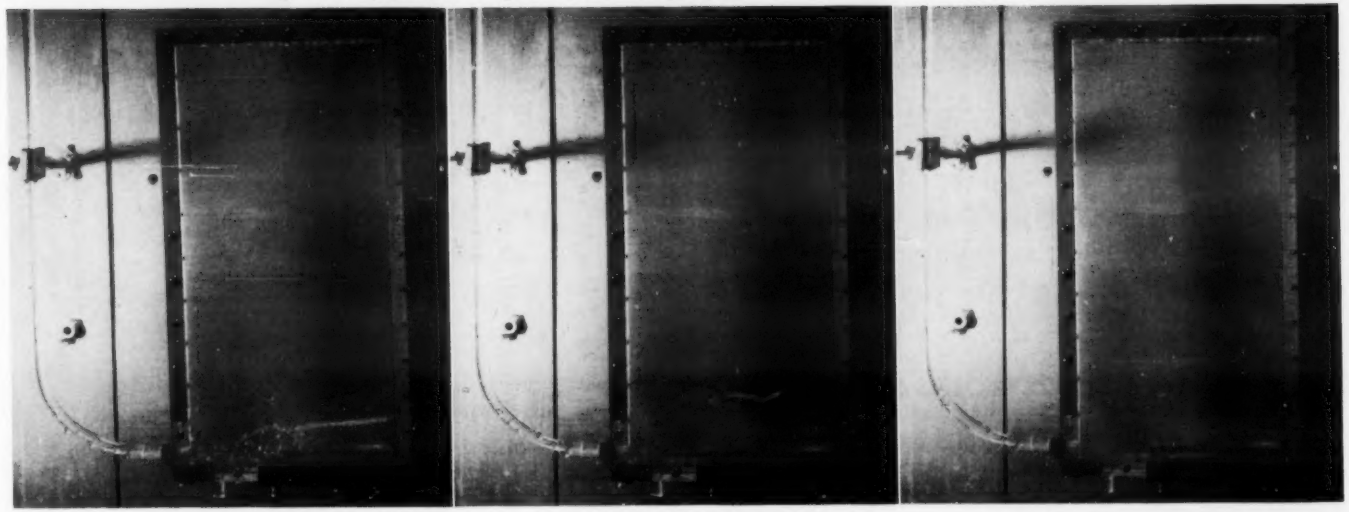


Fig. 18A—½ Sec. *Fig. 18B—2 Sec.* *Fig. 18C—16.2 Sec. Note Absence of Air Bubbles.*

Fig. 18—Effect of Sprue Size, Circular Sprue—Design A-3—Small Mold.

Sprue Diam—0.312 In.
Sprue Area—0.076 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —1.25 Lb/Sec
Aluminum —0.43 Lb/Sec
Magnesium —0.28 Lb/Sec

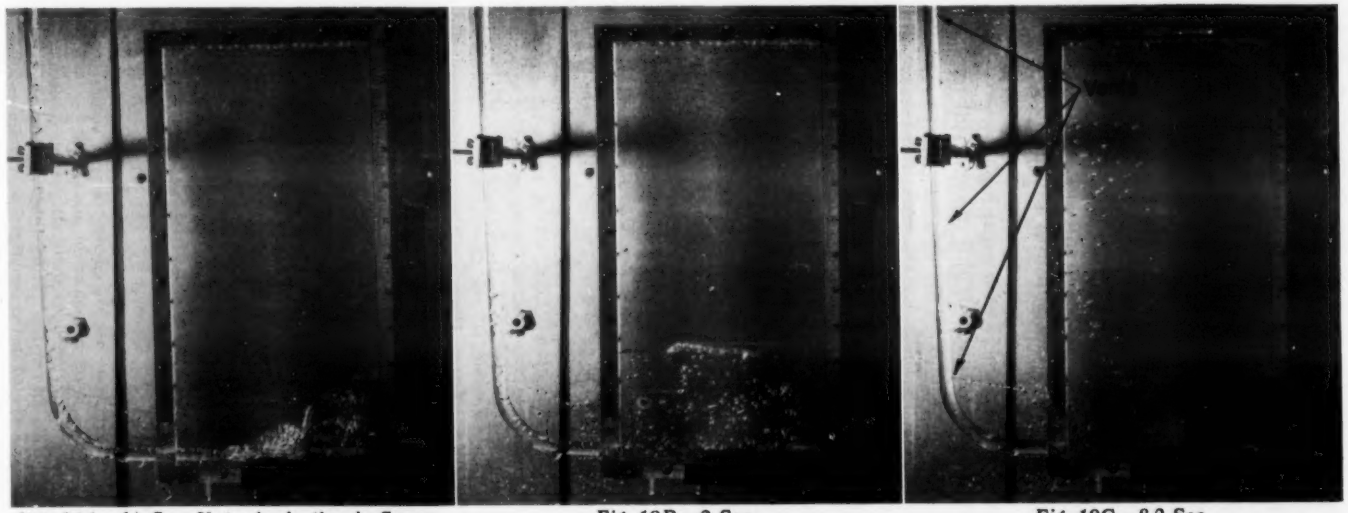


Fig. 19A—½ Sec. Note Aspiration in Sprue. *Fig. 19B—2 Sec.* *Fig. 19C—8.2 Sec.*

Fig. 19—Effect of Sprue Size, Circular Sprue—Design A-3—Small Mold.

Sprue Diam—0.312 In.
Sprue Area—0.076 Sq In.
Mold—½x6x12 In.
Calculated Flow Rate—
Iron —0.79 Lb/Sec
Aluminum —0.27 Lb/Sec
Magnesium —0.17 Lb/Sec

Photos Enlarged from 16-mm Motion-Picture Film.

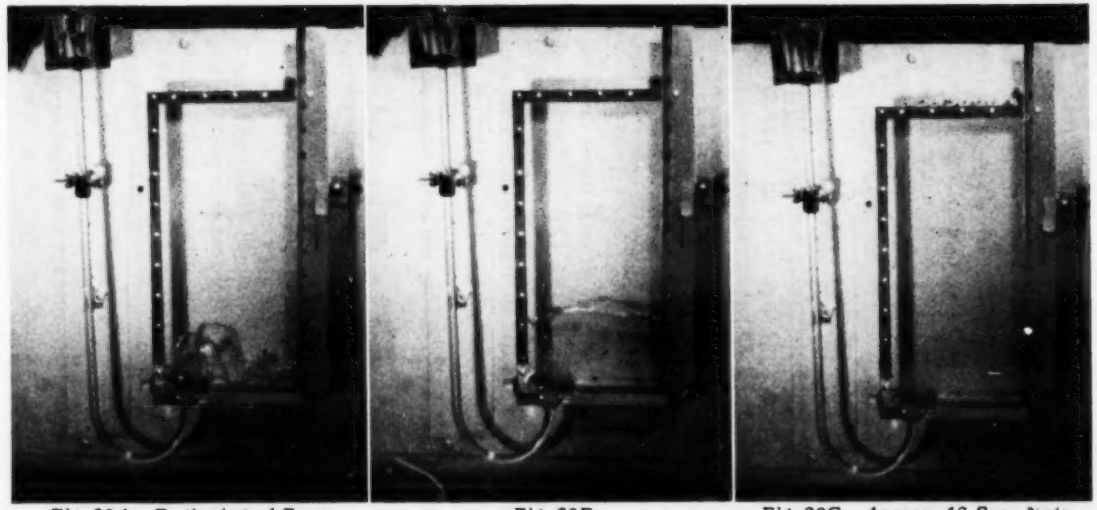


Fig. 20A—Beginning of Pour. *Fig. 20B* *Fig. 20C—Approx. 13 Sec. Note Absence of Air Bubbles.*

Fig. 20—Effect of Pouring Directly Down the Sprue, Circular Sprue—Design A-5—Small Mold.

was selected for testing without the pouring box. The design, A-5, was made from 0.312-in.-inside-diameter glass tubing which entered the small mold from the bottom. This design was selected because it was believed the small amount of back pressure created by the bottom entry of the sprue would permit the sprue to be filled quickly. Previous tests in the laboratory indicated it was difficult to keep a side-entry sprue of larger diameter (0.375 in) full without the aid of the pouring box.

A Lucite enlargement was placed on top of the sprue to permit ease of pouring and to simulate the flaring of the sprue top that exists in a permanent mold. Water was poured from a beaker at a fairly constant rate to maintain a full sprue. Motion pictures were taken at 128 frames per second to record the action during the pouring operation. Three scenes enlarged from the film are recorded in Figs. 20A, 20B, and 20C.

To fill the mold by pouring directly down the sprue required approximately 13 sec. This produced a calculated flow rate of 0.79 lb/sec of iron, 0.27 lb/sec of aluminum, or 0.17 lb/sec of magnesium. This slow rate of pour produced a water casting free of entrapped air bubbles, although considerable turbulence was noted in the sprue during the early stage of the pour.

For purpose of comparison, the same sprue was tested with the pouring box. The time to fill the mold was reduced to approximately 9 sec. The increased flow rate caused a small quantity of air bubbles to be entrapped in the final casting.

It is evident from the few tests conducted that a slower rate of flow was obtained when a pouring box was not employed. This is advantageous and is attributed primarily to the lack of head pressure that exists when a pouring box is used. Also, a certain degree of fluctuation in pour rate during the initial filling of the sprue was present when the pouring box was not used. This aided in decreasing the flow rate but could contribute to aspiration of greater amounts of air down the sprue during the initial pour.

Future Work

Attention will be focused on reducing the amount of turbulence created by the initial surge of liquid into the mold. Such devices as sumps or wells should be of value. The use of webs, risers, and hot shots will be investigated on the more promising sprue designs.

APPENDIX—PREPARATION OF LUCITE

Polishing and Cleaning of Lucite

A plastic, commercially called Lucite or Plexiglas, depending upon the manufacturer, was found to be quite satisfactory for the construction of transparent molds. The advantages of this type of material are that it (1) is readily available, (2) is reasonably inexpensive, (3) is readily fabricated, (4) has a high degree of transparency, (5) has a reasonable amount of strength, (6) is resistant to a moderate amount of heat, and (7) has excellent light-transmission qualities.

This type of plastic can be readily machined by use of the conventional machine tools. Details on the care to be exercised during the various machining operations are given in a Lucite manual published by E. I. du Pont de Nemours and Company.*

The machined surfaces can be brought to a smooth finish with a high degree of luster by the following procedures:

1. Remove tool marks with 3/0 sandpaper, followed by a final sanding with either a 6/0 or 9/0 paper.
2. Apply Du Pont No. 7 rubbing compound with a soft cloth and polish to a high luster. Small pieces may be polished on a metallographic polishing wheel with great rapidity.

Lucite articles polished by this method will have excellent transparency. Dirt and water spots can be removed readily from Lucite with mild soap and warm water. Rinsing with warm water and drying with a clean towel or chamois skin will produce a clear surface. Paint, grease, or adhesives can be removed with xylene soaked on a soft cloth. The warm-water rinse should always be used immediately after any solvent has been used. When articles made of Lucite are not in use, wrapping them in a clean cloth will protect the polished surfaces.

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4. "Tentative Design of Horizontal Gating Systems for Light Alloys," by L. W. Eastwood, SYMPOSIUM ON PRINCIPLES OF GATING, A.F.S. (1951).

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DISCUSSION

Chairman: T. D. STAY, Reynolds Metals Co., Cleveland.

Co-Chairman: P. J. PETTO, JR., Arrow Aluminum Castings Co., Cleveland.

Recorder: P. J. PETTO, JR.

ALFRED SUGAR: [1] Were you considering in your vertical gating of putting a sump in the bottom of the downsprue?

MR. JACKSON: We intend to study all possible variations of the basic designs of vertical gating illustrated in the paper. Sumps will be included in the investigation. Certain established systems will also be studied at a later date.

MEMBER: Was the water used in these tests free from carbon dioxide and, if so, how much?

MR. JACKSON: The water used was tap water.

C. C. HITCHCOCK: [2] Have you considered using any other liquid rather than water which would have a surface tension similar to that of aluminum?

MR. JACKSON: We are considering liquids other than water. We would like to have a liquid that actually freezes, but as yet we have not been able to develop one which could be used in these transparent molds. In most hydraulic studies, it is not a question of surface tension so much but more a question of density and viscosity. Water seems to act very similar to metals in castability in these transparent molds.

* Lucite Manual prepared by the Plastics Department, E. I. du Pont de Nemours and Company, Arlington, N. J.

H. F. DIETRICH:[2] Is there not a vortex effect in a circular gate which would not be present in a rectangular gate?

MR. JACKSON: It certainly is easier to form a vortex at the junction of the pouring box and sprue when a circular gate is used rather than a rectangular box. The pouring box, however, should be kept full enough so that a vortex is not created.

MEMBER: Do you know of any foundry using this gating system, and making good castings, especially the gating illustrated in Figs. 4 and 7?

MR. JACKSON: The gating illustrations in the paper are simpler than those used in the field today. After we learn how these simple systems operate, we will have a better understanding of the more complex systems. Gradually, we are working toward those systems which are in common use today in permanent molding.

MEMBER: Which one of the types of vertical gating systems did you find were the best according to your illustrations?

MR. JACKSON: The best systems studied so far were the rectangular sprue with bottom entry for the small mold and a tapered sprue for the larger mold. Sprues with smooth bends were superior to those with sharp bends. However, none of these designs are recommended at the present time. They represent the initiation of the study of vertical gating which later will lead to specific recommendations.

MR. DIETRICH: Have you used swirl gates in your study of vertical gating?

MR. JACKSON: We have had no experience along those lines yet.

MR. HITCHCOCK: Any agitation whatsoever in a gating system is detrimental to the production of good sound castings. Have you contemplated using a gelatin-type mixture by which you could also study the solidification of the liquid along with the gating?

MR. JACKSON: Our organic chemists are working to develop a liquid by which we might examine the freezing of a liquid as affected by the gating system.

F. N. EATON:[4] It is generally known that the greatest velocity of the metal is at the bottom of the downsprue, both in sand and permanent mold gating. If steps could be taken to reduce this velocity before the metal reaches the bottom of the sprue, a great deal of damage done by the jet effect could be eliminated.

MR. JACKSON: That is a very good suggestion.

MR. JACKSON *(Authors' Closure):* The authors wish to thank the discussors for their contribution to this report. As was stated at the beginning of the paper, this is the first progress report on investigation of vertical gating systems. Naturally, some of the more simple types of gating systems were selected for the preliminary work. As work continues, some of the more complex systems will be studied. The use of gelatinous material, suggested by Mr. Hitchcock, or the use of dyes would provide a valuable tool for the study of temperature gradients as well as flow patterns in vertical gating systems. Development of this technique is in progress.

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[1] Metallurgist, Aluminum Division, The American Metal Company, Ltd., New York.
[2] R. C. Hitchcock & Sons, Inc., Minneapolis.
[3] Professor, Kansas City College, Manhattan, Kansas.
[4] Foundry Manager, Bohn Aluminum & Brass Corp., Detroit.

TWELFTH ANNUAL REPORT ON THE INVESTIGATION OF PROPERTIES OF STEEL SANDS AT ELEVATED TEMPERATURES

By

R. G. Thorpe,* H. Robinson,** P. J. Wisniewski***, and P. E. Kyle†

Preface by Chairman Henry W. Meyer

Sand Division Committee 8-L, Physical Properties of Foundry Sands at Elevated Temperatures, gratefully acknowledges the wisdom of the executive officers of the American Foundrymen's Society in financially sponsoring the study of foundry sands at elevated temperatures. Through alertness and guidance of previous committee 8-L personnel, facilities were developed at Cornell University for determining the physical characteristics of basis sand and sand mixture components.

One important finding as the result of this investigative work was conclusive laboratory evidence that the maximum temperature at which the expansion exceeds the allowable deformation varies and is dependent upon the sand mix components. Also, as this temperature increases the scabbing tendency of the sand mix increases.

The present committee personnel feel it is the committee's duty to continue the investigative work, determining the stress-strain and expansion characteristics of various basis sands and sand mixture components, and correlating this data by using the sand mixtures tested in the production of test castings, thus endeavoring in a technical and practical manner to disseminate this information, hoping it may aid in leading foundrymen to improve their casting quality sandwise.

The committee also wishes to acknowledge the sincerity, accuracy and complete co-operation experienced in their associations with the investigators at Cornell University.

Introduction

This report covers the work on the Sand Research Project sponsored by the Sand Division of the American Foundrymen's Society, which has been completed at Cornell University in the period April 1, 1952 to April 1, 1953. Part I is concerned with the stress-strain and expansion studies of eleven synthetic steel sand mixtures at elevated temperatures. Part II is an extension of the work previously reported and deals with the summation of test casting results on the scabbing tendencies of the sand mixtures studied in Part I.

Two appendices are included at the end of the report. Appendix A covers properties at 2500 F at short exposure times. Appendix B summarizes room temperature properties of the mixtures studied.

The authors wish to acknowledge the many helpful suggestions and the valuable guidance given by the AFS Sand Division Committee 8-L on the Physical Properties of Steel Foundry Sands at Elevated Temperatures, under the Chairmanship of H. W. Meyer.‡

PART I

Stress-Strain and Expansion Test Data

Apparatus and Procedures

The load-deformation apparatus, the hot strength deformation recorder, the expansion apparatus and the procedures employed in conjunction with this apparatus and the preparation of specimens have been adequately described in the Eleventh Annual Report[1] and, therefore, will not be described here.

The procedure for sand mixture preparation for the sands containing only binder and moisture has also been described,[1] but when the mixture contained an additive as well as binder and moisture, this procedure was modified as follows:

a) dried base sand and additive mulled for 5 min dry
b) temper water added slowly
c) mixture mulled for 5 min
d) clay added uniformily over surface of sand in muller
e) mixture mulled for 10 min

Mixtures Studied

The research described in this report has been concerned with eleven synthetic sand mixtures, the constituents of which are given in Table 1. The base sand in all eleven mixes is New Jersey No. 60.

* Assistant Professor of Metallurgy, ** AFS Investigators and † Professor of Metallurgy, Cornell University, Ithaca, N. Y.

‡ Committee 8-L: H. W. Meyer, Chairman; C. W. Briggs, H. W. Dietert, R. H. Jacoby, C. B. Jenni, R. E. Morey, J. A. Rassenfoss, J. B. Stevens, H. F. Taylor, D. C. Williams.

TABLE 1—TEST MIXTURES

Mix No.	Binder Type	Binder Content, %	Moisture, %	Additive Type	Additive Content, %
1	W.B. No. 5	4	3	—	—
2	W.B. No. 5	3	3	—	—
3	W.B. No. 5	2	3	—	—
4	W.B. No. 5	1	3	—	—
5	F.C. No. 8	10	3	—	—
6	W.B. No. 5	4	3	Cereal	0.5
7	W.B. No. 5	4	3	Cereal	1.0
8	W.B. No. 5	4	2	Cereal	0.5
9	W.B. No. 5	4	2	Cereal	1.0
10	W.B. No. 5	4	4	Cereal	0.5
11	W.B. No. 5	4	4	Cereal	1.0
12[1]	W.B. No. 5	4	5	—	—
13[1]	W.B. No. 5	3	5	—	—
14[1]	W.B. No. 5	2	5	—	—
15[1]	W.B. No. 5	1	5	—	—
16[1]	F.C. No. 8	10	5	—	—

[1] Mix numbers 12 to 16 were reported on in reference 1.
W.B. = Western Bentonite.
F.C. = fire clay

Experimental Data

Figures 1 through 5 are graphical representations of the data on hot compressive strength, strain and free expansion at temperatures from 1000 to 2500 F which were collected on the eleven synthetic sands. Each point on the various curves represents the average of at least three determinations at that temperature. Near the critical temperatures, where the strain and expansion curves cross, the points represent the averages of usually four or more individual determinations.

Figure 1, in which hot compressive strengths are plotted against temperature for the sand mixtures containing 3 per cent moisture and 1, 2, 3, and 4 per cent western bentonite, as well as 10 per cent fire clay, shows the recognized fact that the shape of the strength temperature curve is determined by the type of binder present.

The plots of hot compressive strength versus temperature in the top sections of Figs. 2, 3 and 4 all show a rather interesting phenomenon. The three plots represent six sand mixtures, all bonded with 4 per cent western bentonite. The two curves on any one sheet also represent mixtures of constant moisture content, the variable being cereal additive. In all three cases, the effect of adding ½ per cent of cereal to the 4 per cent bentonite bonded mixture is to shift the peak strength to the right, while the addition of 1 per cent of cereal causes the peak strength to shift to the left and down. The 1 per cent cereal additive also produces a "plateau" effect in the curves at temperatures beyond the peak strength temperature. The explanation for this phenomenon may be in the fluxing action of the residue remaining from the combustion of the cereal.[2]

Figure 5 shows the effect on strain and expansion of varying amounts of bentonite binder and of one content of fire clay binder. It may be seen that as the bentonite content is increased from 1 to 4 per cent the critical temperature at which the strain and expansion curves intersect shifts progressively from 2150 to 1640 F. The critical temperature for the fire clay bonded sand is 1990 F. Thus, the trend here at 3 per cent moisture is similar to that for the same series of sands at 5 per cent moisture. This latter series was presented earlier[1] and the curves are reproduced here as Fig. 6 to facilitate comparison of the 3 and 5 per cent conditions. The effect of decreasing the moisture by 2 per cent has been to decrease somewhat the critical temperature for any given binder content.

Critical Temperatures

The critical temperature for the 3 per cent bentonite bonded sand is lower than for the 4 per cent bentonite sand in contrast to the trend which was mentioned above and which is followed in the 5 per cent moisture series. The critical temperature for the 4 per cent bentonite sand is 1640 F, while for the 3 per cent bentonite sand it is 1570 F. However, from 1570 to 1700 F the strain curve lies parallel to the expansion curve and within about 0.002 in./in. of it. In the last report[1] it was noted that there was an inherent time lag in the strain recording apparatus which caused these readings to be in error by approximately 0.005 in./in. Thus, although ordinarily this results in very little error in the critical temperature, in this case the relative shapes of the two curves may result in an error of a hundred degrees or more in the critical temperature.

Figure 3 shows the strain-expansion curves for the 4 per cent bentonite, 3 per cent moisture sand with cereal additives. The curves for the reference mixture containing no additive are also reproduced for easy comparison. It is seen that the cereal has some little effect upon the critical temperature, shifting it to 1550 F when cereal content is ½ per cent and to 1580 F when cereal content is 1 per cent. This is compared to the 1640 F critical temperature of the reference mixture. The strain curve for the ½ per cent cereal sand crosses the expansion curve, and then at a higher temperature drops below and then recrosses the expansion curve. The initial crossing point is assumed to determine the critical temperature in keeping with the proposed theory[3] on scabbing tendencies of sands, which states that if mold-metal interface conditions are such that the expansion of the sand results in a deformation which exceeds the ability of the sand to deform at that temperature without fracture, a surface defect will occur. Thus, the lowest temperature at which such conditions apply is the critical one.

Figure 2 shows the strain-expansion curves, at 4 per cent moisture, for 4 per cent bentonite bonded sands with ½ per cent and 1 per cent cereal additive. The critical temperature for both these sands is 1565 F.

Figure 4 shows the strain-expansion data, at 2 per cent moisture, for the 4 per cent bentonite bonded sands containing ½ per cent and 1 per cent cereal additive. The critical temperature for the 1 per cent cereal sand is 1640 F, and for the ½ per cent cereal sand it is 1865 F. The latter temperature seems to be out of line with the results for the other five sands containing cereal additive and again it must be assumed that this may be due to the lag in the strain recording apparatus. At 1600 F the observed strain for this sand is only 0.003 in./in. below the observed

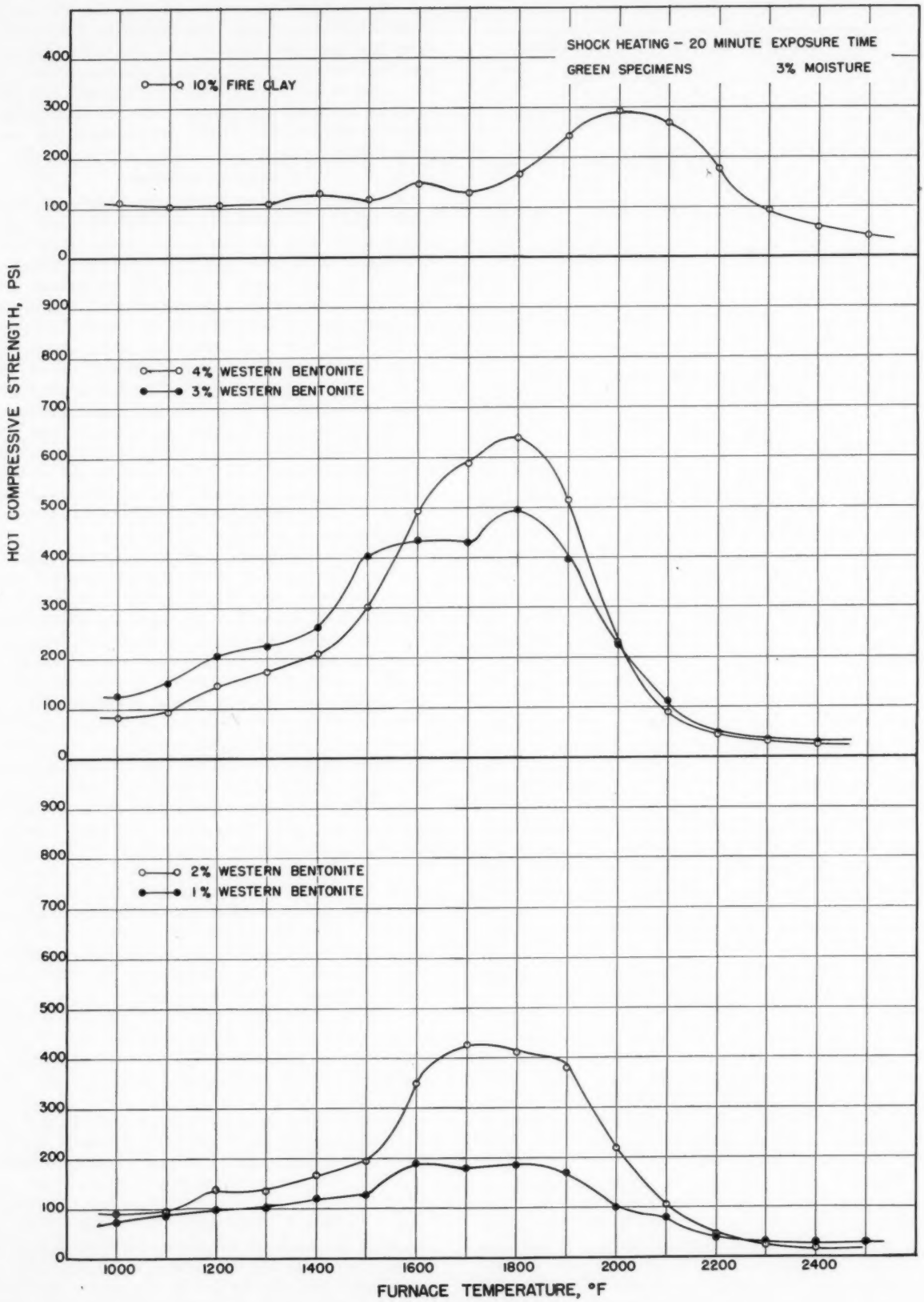


Fig. 1—Hot compressive strength vs. temperature.

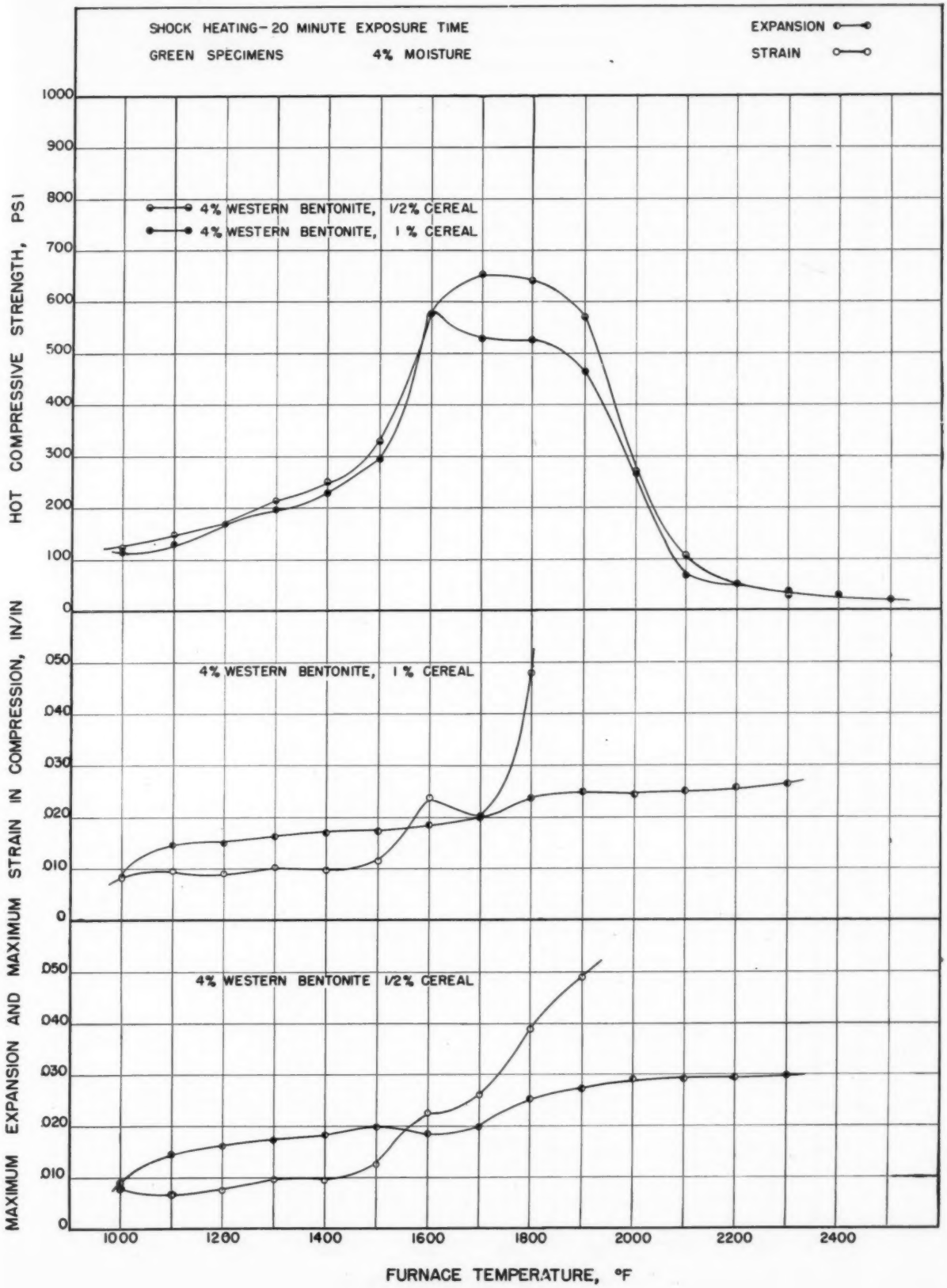


Fig. 2—Hot compressive strength, maximum strain, and expansion, vs. temperature.

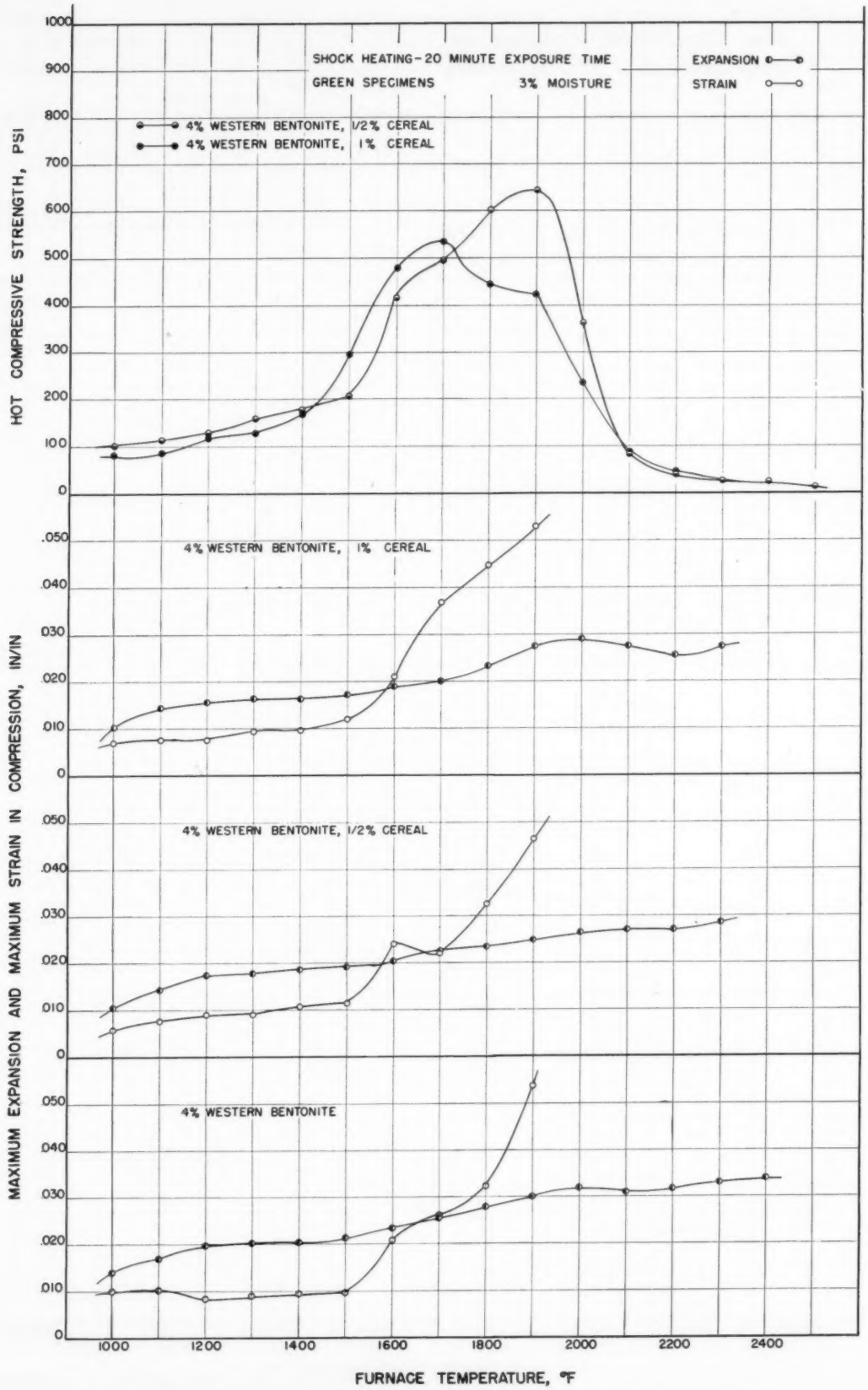


Fig. 3—Hot compressive strength, maximum strain, and expansion, vs. temperature.

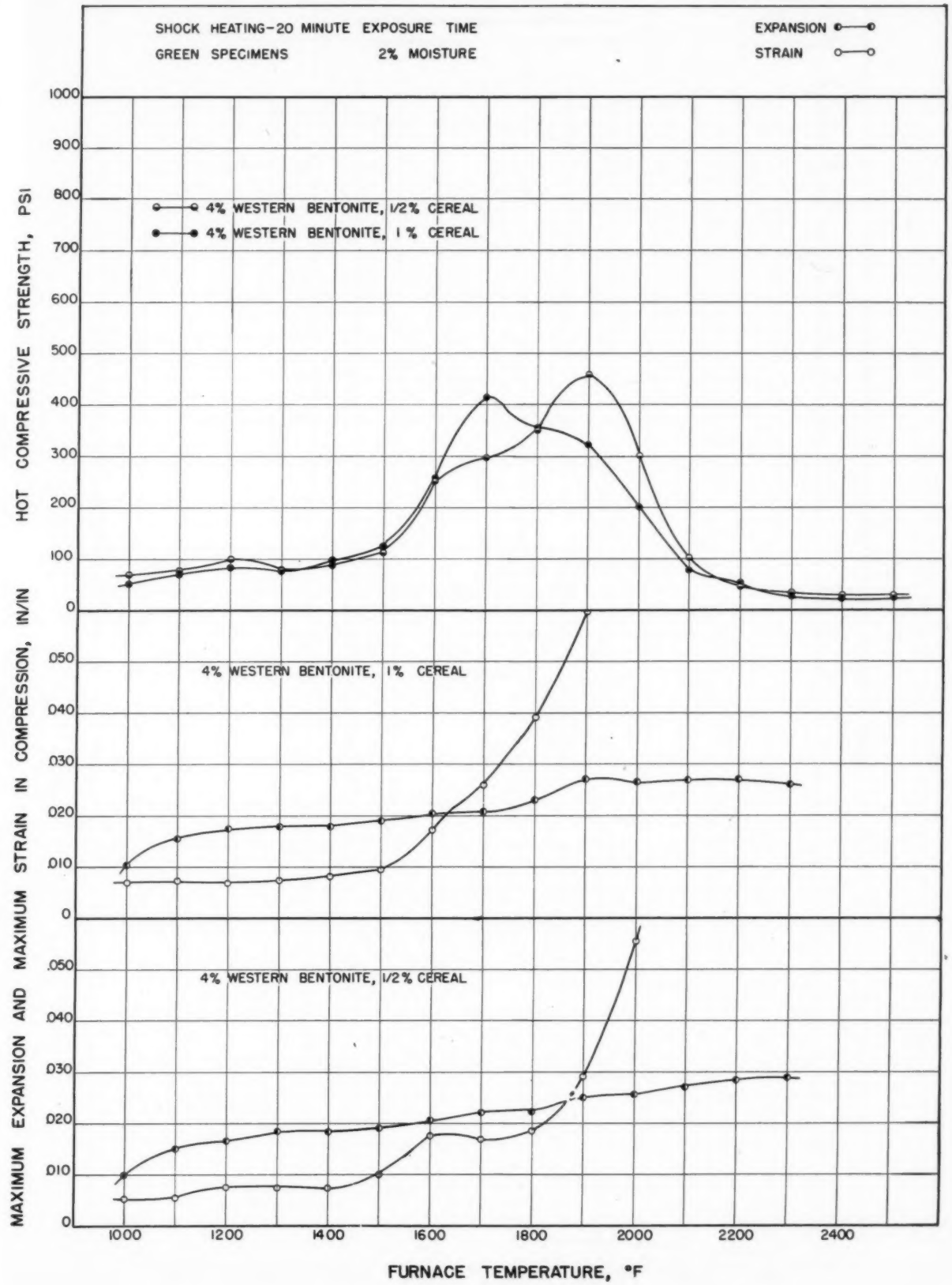


Fig. 4—Hot compressive strength, maximum strain, and expansion, vs. temperature.

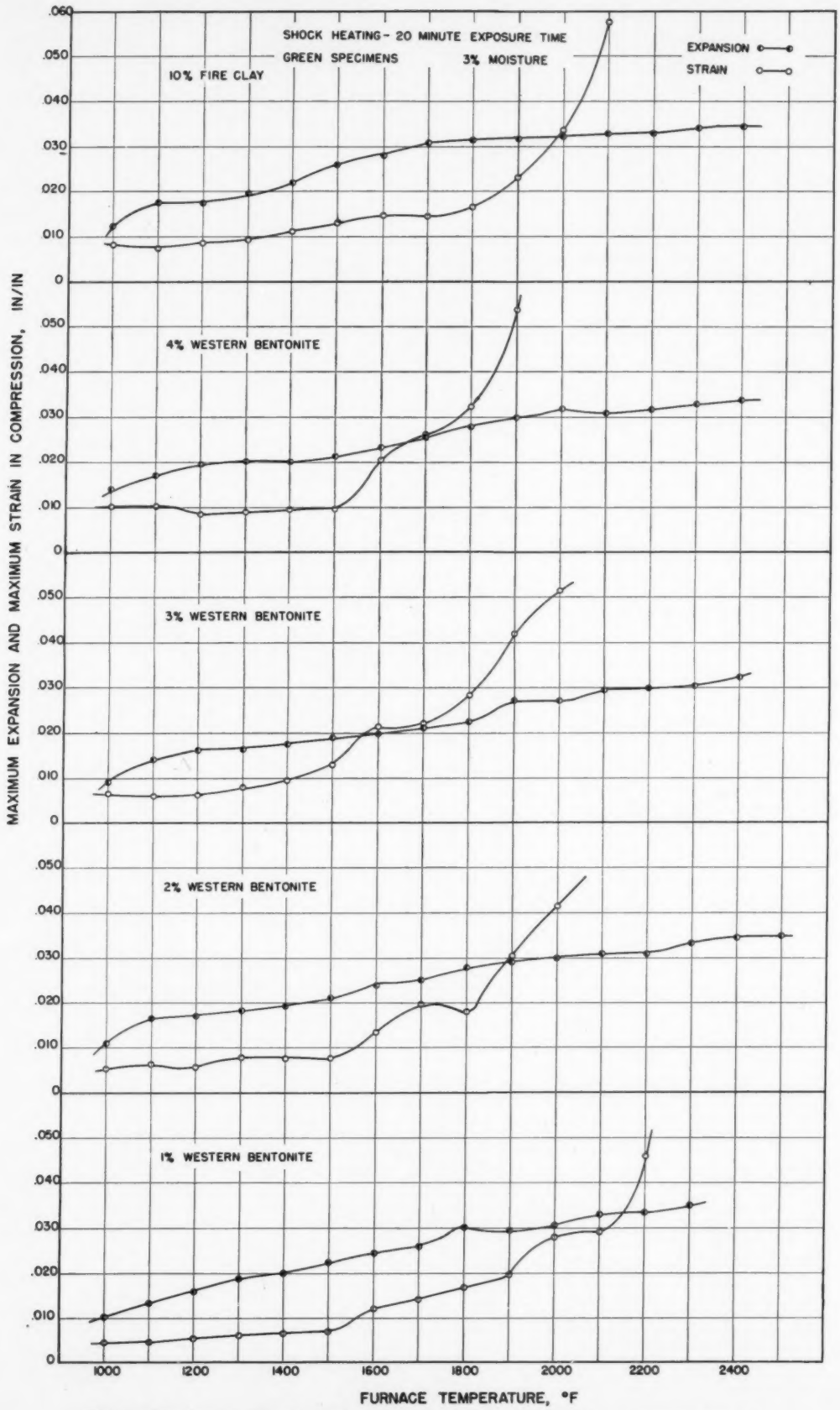


Fig. 5—Maximum expansion and maximum strain in compression vs. temperature.

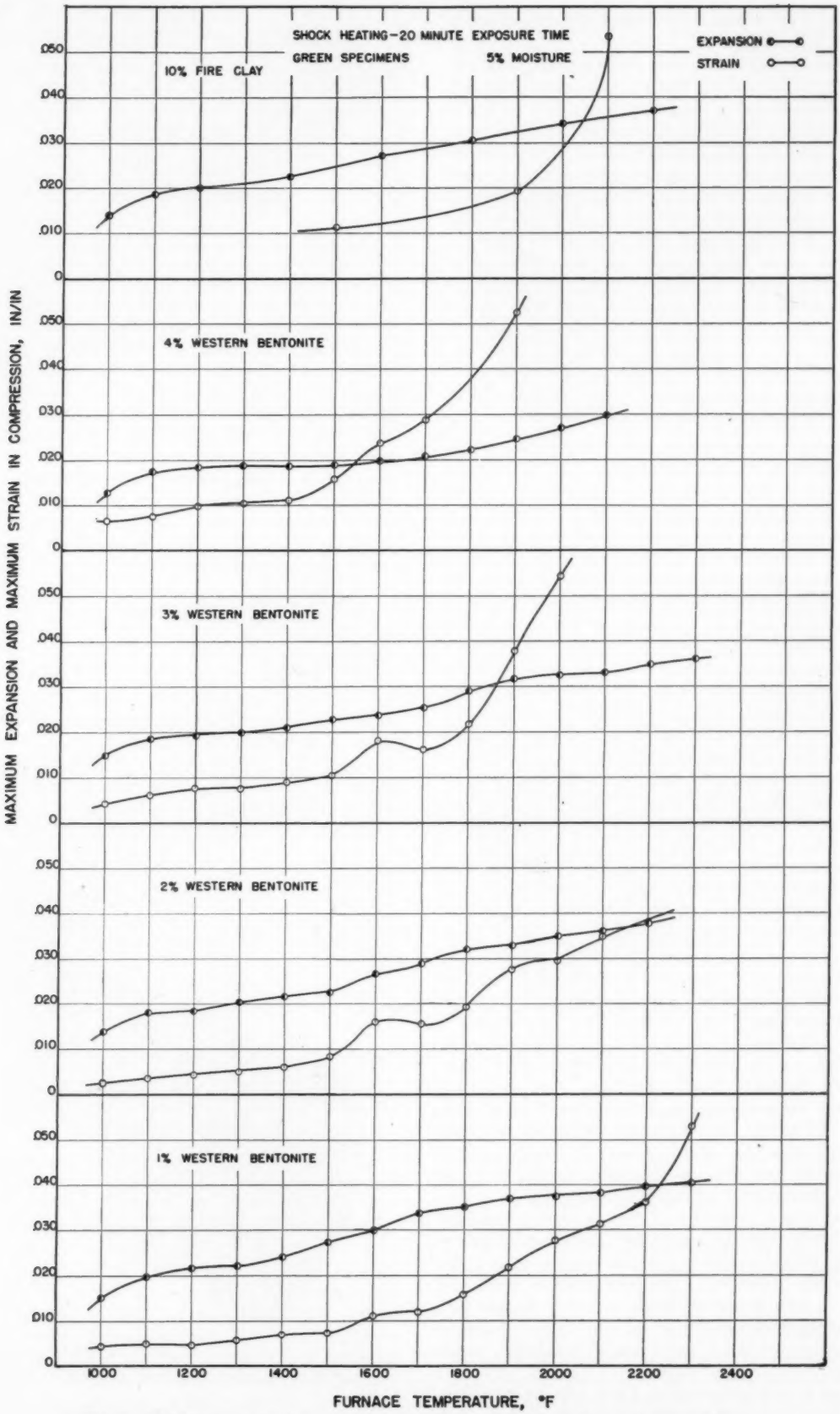


Fig. 6—Maximum expansion and maximum strain in compression vs. temperature.

expansion. This is well within the 0.005 in./in. error of the apparatus.

Inspection of Figs. 2, 3, 4, 5 and 6 reveals that the percentage of bentonite binder, the cereal additive and the percentage of moisture have only a slight effect upon the expansion characteristics of the sand mixtures. The expansion curves for all these bentonite bonded sands are actually quite similar, and changes in critical temperature result mainly from a shifting of the strain curves with changes in the variables. This, of course, might be predicted since silica sand grains exist in all the mixtures as the major constituents and tend to mask the effects of the other lesser constituents.

PART II

Strain-Expansion Characteristics of the Mixtures With Reference to Scabbing Tendency

On the basis of the theory of scab formation previously mentioned, some predictions can be made of the scabbing tendencies of the various synthetic sands described in this report. The scabbing tendency of a sand will increase as the critical temperature at which the strain becomes equal to the expansion increases.

Thus, at 3 per cent moisture, with no additives, the scabbing tendency of a bentonite bonded sand should decrease as the bentonite content is increased from 1 per cent to 4 per cent. All the sands studied with cereal additive should show at least as little tendency to scab as the 4 per cent bentonite, 3 per cent moisture sand. The 10 per cent fire clay, 3 per cent moisture sand should show a scabbing tendency similar to the 1 per cent and 2 per cent bentonite sands, and the 5 per cent moisture, bentonite bonded series should show a greater scabbing tendency than the 3 per cent moisture series for any given bentonite content.

In order to confirm the laboratory data, test castings were poured in steel, using the Steel Founders' Society of America test casting previously described.[1] (Additional test castings will be poured at a later date.)

Five castings were poured in the Cornell University Foundry Laboratory. Molds were rammed by hand to mold hardnesses of 82-87, which correspond to the hardnesses obtained in the laboratory specimens used for the expansion and deformation tests. The sand mixtures under test were used as facing sands over the pattern to a rammed depth of at least 1½ in. All molds were poured in acid-electric steel.

Table 2 gives the sand mixtures in which castings were poured and the critical temperatures of these mixes as determined in the laboratory.

Casting *A* was poured in a 3 per cent western bentonite, 5 per cent moisture sand and showed severe scabbing (Fig. 7). Casting *B* was poured in a 3 per cent western bentonite, 3 per cent moisture sand and showed a scab defect of lesser severity, as was predicted from a consideration of the strain-expansion characteristics of the 3 and 5 per cent moisture sands (Fig. 8).

Casting *C* was poured in a 4 per cent western bentonite, 3 per cent moisture sand and showed no scab defect (Fig. 9). Thus, the relative scabbing tendencies of the 3 and 4 per cent bentonite sands (moisture constant) are observed to be as predicted.

Casting *D* was poured in a 4 per cent western bentonite, 3 per cent moisture, ½ per cent cereal sand, and casting *E* was poured in a 4 per cent western bentonite, 2 per cent moisture, ½ per cent cereal sand. Both castings showed no scab defect (Figs. 10 and 11) which confirms the prediction made that cereal containing sands would show at least as little scabbing tendency as the 4 per cent western bentonite, 3 per cent moisture sand.

TABLE 2

Test Casting	Mixture No.	Binder	Moisture, %	Additive, %	Critical Temperature of Mix, F
A	13	3 Western Bentonite	5	—	1870
B	2	3 Western Bentonite	3	—	1570-1700
C	1	4 Western Bentonite	3	—	1640
D	6	4 Western Bentonite	3	½ Cereal	1555
E	8	4 Western Bentonite	2	½ Cereal	*1600-1865

* For these two mixes there is a range of temperature over which the observed strains and expansions are within the limits of the apparatus error discussed in Part I.

APPENDIX A

Properties at 2500°F

A fairly common foundry control test consists of a hot compressive test at 2500 F after a 2-min exposure at temperature. This test has been and is being used in some foundries as a control test for hot-tearing tendency, and it was felt that this test should be applied to the mixtures studied in this project.

The appearance of a specimen shock-heated to 2500 F for a given length of time is often a valuable means



Fig. 7—Test casting "A."

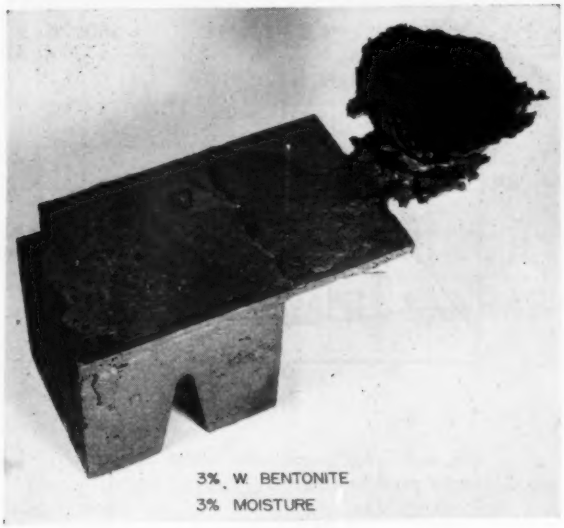


Fig. 8—Test casting "B."



Fig. 9—Test casting "C."



Fig. 11—Test Casting "E."



Fig. 10—Test casting "D."

for estimating the thermal stability of a mold sand and, therefore, this was also instituted as an auxiliary test to be applied in this project.

Data on both these tests are given in Table A-1 for the mixtures investigated. The mixtures are those used in Table 1 of the main report.

The authors would like to point out that the values of hot compressive strengths given in Table A-1 do not represent values for specimens at uniform temperature throughout. A 2-min exposure time, even at 2500 F, is not sufficient for the center of the 2 x 1 1/8-in. diameter specimen to reach equilibrium. This is shown by Fig. A-1 which indicates the time necessary at various temperatures for the specimen to reach uniform temperature throughout. At 2500 F this time is 10 min.

Fig. A-2 shows the difference in the stress-strain curves at 2500 F at exposure times of 2 and 20 min.

Conclusions

This report shows conclusive laboratory evidence that the maximum temperature at which the expansion exceeds the allowable deformation varies in different steel sand mixtures. It was concluded in a

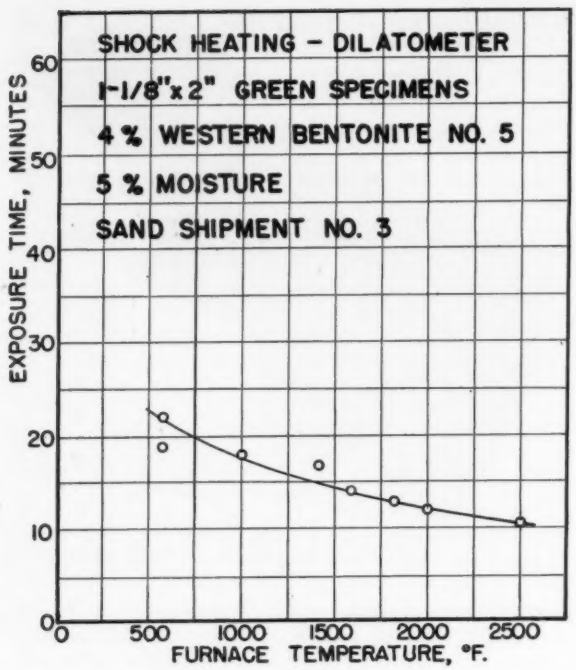


Fig. A-1—Time required for specimen to reach uniform constant temperature throughout.

TABLE A-1—PROPERTIES AT 2500 F

Mixture* No.	Hot Comp. Str. at 2500 F, 2-min Exposure,	Appearance After Shock Heating to 2500 F for 25 min	Test Casting No.
1	95 psi	Network of fine cracks	C
2	99	Network of fine cracks	B
3	73	Network of fine cracks	
4	43	Network of fine cracks	
5	181	Several fine cracks (longitudinal)	
6	65	No cracks	D
7	80	No cracks	
8	55	No cracks	E
9	54	No cracks	
10	66	No cracks	
11	68	No cracks	
12	49	Large cracks on surface	
13	45	Large cracks on surface	A
14	39	Very large cracks—entire surface	
15	31	Very large cracks—entire surface	
16	73	Hairline cracks on length	

*Mixture numbers 1 to 11 are those used in Table 1 of main report. Numbers 12 to 16 refer to mixtures reported on in Reference 1.

previous report that as this temperature increases, the scabbing tendency of the sand increases. This report offers further confirmation of this conclusion.

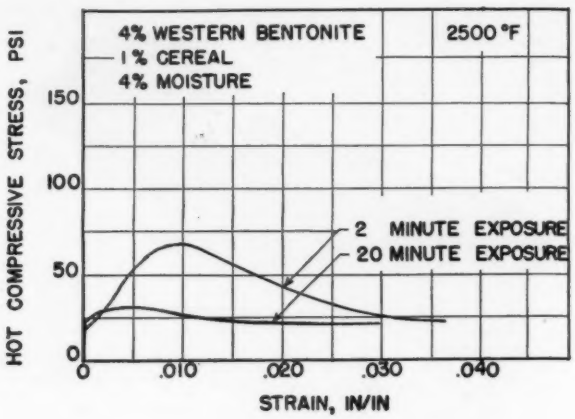


Fig. A-2—Hot compressive stress vs. strain.

APPENDIX B

ROOM TEMPERATURE PROPERTIES

Mixture* No.	Green Comp. Strength, psi	Green Perm. cm⁴/gm, sec	Dry Comp. Strength psi	Dry Perm. cm⁴/gm, sec	Test Casting No.
1	4.4	195	24.7	252	C
2	3.2	203	58.3	261	B
3	2.2	177	50.3	210	
4	1.2	195	38.0	207	
5	7.5	154	11.2	188	
6	6.2	159	55.5	204	D
7	8.4	145	>94	202	
8	7.2	122	53.3	151	E
9	8.7	142	39.8	163	
10	6.1	116	>94	174	
11	6.8	122	>94	170	
12	4.6	133	>94	191	
13	3.8	136	>94	194	A
14	2.6	152	82.2	218	
15	1.8	162	63.2	235	
16	3.0	108	69.6	160	

*Mixture numbers 1 to 11 are those used in Table 1 of main report. Numbers 12 to 16 refer to mixtures reported on in Reference 1.

>94. These values indicate the specimen exceeded the testing range of the machine.

References

1. R. G. Thorpe, A. E. Riccardo, P. L. Widener, and P. E. Kyle, "Eleventh Annual Report on Investigation of Steel Sands at Elevated Temperatures," A.F.S. TRANSACTIONS, vol. 60, pp. 197-210 (1952).

2. R. E. Morey, C. G. Ackerlind and W. S. Pellini, "Effect of Binders and Additives on the High Temperature Properties of Foundry Sands," A.F.S. TRANSACTIONS, vol. 60, pp. 266-273 (1952).

3. R. G. Thorpe, P. E. Kyle and J. P. Fraser, "Tenth Annual Report on the Investigation of Steel Sands at Elevated Temperatures," A.F.S. TRANSACTIONS, vol. 58, pp. 133-143 (1950).

EFFECTS OF MELTING FURNACE ATMOSPHERE ON CASTING PROPERTIES AND ANNEALABILITY OF MALLEABLE IRON

By

E. A. Lange* and R. W. Heine**

In a previous paper,[4] the authors have reported the initial work on an A.F.S. Malleable Division Research Project aimed at determining the influence of melting furnace atmospheres on the properties of malleable iron. The present paper offers results obtained while continuing the work during the past year.

Among the properties studied were the following:

1. Influence of the furnace gases on chemistry of the iron.
2. Mottling.
3. Fluidity.
4. Hot Tearing.
5. Anneability.
6. As-Cast Microstructure.

Since most of the experimental techniques for obtaining data were described in detail in the first paper, they will not be recounted except where new procedures were employed.

The reader is referred to Reference 4 in the bibliography for a description of the methods of testing for mottling, fluidity, hot tearing, pinholing, and for the melting cycle and means of developing the furnace atmosphere. However, a brief review of the more general steps will be presented to develop continuity of thought in this paper.

Melting stock for the project consisted of two types of white iron sprue; one from a cupola air furnace duplexing shop, the other from a foundry using the cold melt air furnace process. The duplex iron sprue had an average analysis of 2.54 C; 1.30 Si; 0.41 Mn; 0.147 S, and 0.11 P. The cold melt iron sprue had an average analysis of 2.48 C; 1.10 Si; 0.37 Mn; 0.087 S; 0.126 P. The duplex process iron was selected first by the committee for detailed study in determining the principles which form the major portion of this paper. The cold melt iron was then studied in a limited way with results presented in the later part of the report. The melting schedule involved the following steps:

1. Nine-pound heats of white iron were melted in magnesia-lined induction melting furnace. Adjustment of carbon and manganese was made by addition of graphite and ferromanganese. The crucible was then fitted tightly with a cover.

2. A gas atmosphere was then introduced by passing a purified and dried gas into the furnace chamber at 2 liters per minute except in the case of air and water. Pure gases introduced were CO, CO_2, N, A, and H_2O. Dried air was introduced to chamber at the rate of 6050 ml per minute in order to maintain a carbon oxidation rate of about 100 pt per hour. Water atmosphere was generated by evaporating H_2O from a tube positioned over the metal surface.

3. After a 1 hr holding period under the atmosphere at temperature, the test castings were poured. The temperatures of holding and pouring employed were 2700 F and 2900 F.

As pointed out earlier, the detailed step by step procedure in the above melting cycle has been given in Reference 4. Data obtained by this procedure includes the following:

1. Change in chemical analyses of the iron over the 1-hr holding period as influenced by atmosphere and temperature. Samples for chemical analyses were taken at the beginning, middle, and end of the holding period.

2. Composition of the furnace atmosphere developed by each of the pure gases. Samples of the furnace atmosphere were taken at intervals during holding period.

3. *Fluidity.* The modified fluidity spiral developed at the University of Wisconsin was used for fluidity tests. This fluidity spiral is also described in Ref. 4.

4. Resistance to hot tearing as measured by the hot tear testing device specially developed for the project. The hot tear test was described in detail in References 3 and 4, reports of the first year of work on the project.

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This is the second progress report on a research project initiated and sponsored by the Research Committee of the Malleable Division of the American Foundrymen's Society. Members of this Committee are as follows: C. F. Joseph, *Chairman,* W. D. McMillan, *Vice-Chairman,* H. Bornstein, W. A. Kennedy, J. H. Lansing, R. P. Schauss, R. Schneidewind, Milton Tilley and P. F. Ulmer.

TABLE 1—HEATS MADE AT 2700 F UNDER CO_2 ATMOSPHERE

Heat No.*	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	CO
Q-1-60	2.13			12.6	76	32.7	67.1
Q-2-0		1.22	.42			37.8	
Q-2-60	2.46	1.22	.43	17.4	73	41.7	
Q-3-60	2.50			16.8	56	48.0	
Q-4-0		1.21	.44				
Q-4-60	2.60	1.21	.45	21.1	47		
Q-5-0		1.22	.44			40.5	
Q-5-60	2.16	1.21	.45	13.7	78	41.9	
Q-6-0		1.21	.44			44.9	
Q-6-60	2.49	1.21	.45	17.7	47	39.3	
Q-7-0	3.42	1.22	.39			39.7	
Q-7-60	2.77	1.22	.42	24.3	55	34.4	
Q-8-0	2.98	1.22	.44			43.0	
Q-8-60	2.29	1.21	.42	17.3	56	39.6	
Q-9-60	2.04			12.6	80	35.7	
Q-10-60	2.12			10.9	63	36.6	

* 0 refers to sample taken at beginning of 60-min holding period. 60 refers to sample taken at end of 60-min holding period.

TABLE 2—HEATS MADE AT 2700 F UNDER CO ATMOSPHERE

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	CO
K-1-60	2.53			23.0	48	1.14	
K-2-0	2.60	1.31	.44			1.23	
K-2-60	2.58	1.32	.43	23.7	49	.92	98.9
K-3-0	2.20	1.34	.42			.87	99.1
K-3-60	2.18	1.31	.43	19.4	—	.95	
K-4-60	2.18			18.6	57	.56	
K-5-60	2.80			25.1	52	.76	
K-6-0		1.31	.48			.85	
K-6-60	2.88	1.28	.47	24.8	57	.76	
K-7-60	2.92			27.6	68	.72	
K-8-0		1.30	.43			.82	
K-8-60	2.08	1.24	.44	17.1	51	.78	
K-9-60	2.08		.44	18.3	81	.83	
K-10-0		1.21	.43			.93	
K-10-60	2.45	1.26	.43	20.3	50	.62	
K-11-60	2.02			15.5	95	.70	

TABLE 6—DATA FOR HEATS MADE AT 2700 F UNDER H_2O ATMOSPHERE

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity, in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	H_2	CO	CH_4
R-1-60	2.44	1.15		19	45				
R-2-0		1.25	.42						
R-2-60	2.41	1.22	.44	18.9	51	6.8	42.7	45.2	5.8
R-3-60	1.80			6.5	No break	3.4	45.3	44.8	6.0
R-4-0	2.63	1.17	.45						
R-4-60	2.08	1.18	.43	11.3	76				
R-5-0		1.21	.42						
R-5-60	2.53	1.22	.44	19	40	5.5	45.5	39.7	9.9
R-6-0	3.36	1.19	.39						
R-6-60	2.70	1.21	.40	23.3	33	5.9	46.2	40.2	7.7

TABLE 3—HEATS MADE AT 2700 F UNDER N_2 ATMOSPHERE

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	CO
P-1*	2.04			13.7	99		
P-2*	2.40			20.0	64		
P-3*	2.24			14.0	52		
P-4*	2.50			21.5	55	.64	.21
P-5-60	2.39			17.0	52	1.55	52
P-6*	2.68			23.5	37		
P-7-60	2.52			—	48	.72	.18
P-8-60	2.13			—	53	.86	.29
P-9-60	2.49			—	54	.31	.15
P-10-60	2.59			—	55		
P-11-0	2.58	1.29	.41				
P-11-60	2.53	1.28	.40	17.8	60		
P-12-60	2.39			16.2	55	0	0
P-13-60	2.53			18.8	40	.19	.19
P-14-0		1.21	.44				
P-14-60	2.07	1.21	.44	11.9	101		
P-15-60	2.92			25.9	25		
P-16-0		1.21	.44				
P-16-60	2.86	1.28	.46	21.2	61		
P-17-0							
P-17-60	2.34			14.8	58		

* Held for ½ hr under N_2 atmosphere.

TABLE 4—HEATS MADE AT 2700 F UNDER A ATMOSPHERE

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	CO
L-1-60	2.36		.43	15.7	77	.29	.09
L-2-60	2.39			17.4	58	.10	.30
L-3-60	2.79			23.7	51	.21	.57
L-4-60	2.13			16.0	72	.16	.48
L-5-60	2.32			16.9	—	.16	.29
L-6-60	2.60			19.5	—	.25	.30
L-7-60	2.13			17.1	54		
L-8-60	1.85			14.1	No break		

TABLE 5—HEATS MADE AT 2700 F UNDER H_2 ATMOSPHERE

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	CO
M-1-60	2.27			11.5	85	12.2	86.8	1.6
M-2-60	2.58			19.7	71	10.0	86.9	1.1
M-3-0	2.88	1.30	.42			13.1	83.4	3.0
M-3-60	2.74	1.25	.43	22.8	72	13.1	85.0	2.5
M-4-0	2.50	1.23	.47			18.6	77.9	1.9
M-4-60	2.40	1.24	.49	18.9	86	15.5	81.3	2.3
M-5-60	2.01			12.2	62	14.0	60.5	
M-6-0		1.22	.44					
M-6-60	2.29	1.24	.47	16.1	92	15.3	82.4	2.3
M-7-0		1.21	.43			14.8	83.0	2.2
M-7-60	2.04	1.22	.45	13.2	93	14.6	83.7	1.7

The data for items 1-4 above are tabulated in Tables 1-13.

5. *Annealability.* The tests on the influence of furnace gases on annealability will be described later in the paper on the section dealing with that subject.

6. *Mottling.* Mottling tendency was observed in the fractured well of the fluidity casting.

The first paper,[4] reporting data on the subjects listed above, presented information relative to a temperature of 2700 F for the 1 hr holding period using the duplex melting stock. The current paper

Table 7—Heats Made at 2900 F Under CO_2 Atmosphere

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity, in.	Hot Tearing Spring Deflection 0.0001 in.	Effluent Gas Analysis % Co_2
U-1-60	1.65			1.74	118	25.0
U-2-0	3.08					29.2
U-2-30	2.58					
U-2-60	2.10			26.1	80	30.6
U-3-0		1.26	.50			
U-3-60	2.40	1.31	.49	30.4	50	30.6
U-4-0		1.29	.50			
U-4-60	2.60	1.31	.50	30.8	68	26.2
U-5-0		1.28	.49			
U-5-60	2.76	1.26	.48	33.2	78	33.0
U-6-0		1.26	.55			
U-6-60	2.30	1.31	.53	25.1	47	28.9
U-7-60	2.07			26.3	83	27.7
U-8-0	3.22	1.29	.49			
U-8-30	2.72					
U-8-60	2.22	1.33	.50	27.5	68	26.6

Table 8—Heats Made at 2900 F Under CO Atmosphere

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity, in.	Hot Tearing Spring Deflection 0.0001 in.	Effluent Gas Analysis % Co_2
S-1-60	2.18	1.32		28.5	68	
S-2-0		1.32	.46			
S-2-60	2.83	1.33	.45	35.5	59	
S-3-0		1.31	.46			
S-3-60	2.63	1.29	.46	31.4	61	
S-4-0		1.33	.50			
S-4-60	2.48	1.33	.51	29.7	70	
S-5-60	2.04				107	
S-6-0		1.33	.50			
S-6-60	2.18	1.33	.49	26.1	69	2.4
S-7-0	2.29	1.31	.47			2.6
S-7-30	2.28					2.4
S-7-60	2.28	1.31	.49	28.3	73	
S-8-60	2.22			28.5	66	3.87
S-9-0		1.31	.49			
S-9-60	2.37	1.29	.49	30.7	57	3.72

Table 9—Heats Made at 2900 F Under N_2 Atmosphere

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	CO
V-1-60	2.22			30.7	78	2.20	2.14
V-2-0		1.36	.54				
V-2-60	2.75	1.36	.55	35.5	No break	1.43	1.22
V-3-0		1.32	.49				
V-3-60	2.60	1.32	.48	31.0	72	1.51	.25
V-4-60	2.38			30.1	90*	1.68	.24
V-5-0		1.31	.52				
V-5-60	2.00	1.31	.51	22.9	72	1.63	.85
V-6-60	2.09			26.9	68	1.85	.53
V-7-0		1.32	.49				
V-7-60	2.27	1.31	.49	27.2	50	1.72	.74
V-8-0	2.48	1.27	.47			1.88	.88
V-8-30	2.45						
V-8-60	2.42	1.29	.48	30.7	61		

* Flash at break.

presents data for a holding temperature of 2900 F using the duplex iron, the principles derived from both sets of data, and results of a limited number of tests of the cold melt iron.

Table 10—Heats Made at 2900 F Under Argon Atmosphere

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	CO
W-1-60	2.22			28.5	58	1.39	2.70
W-2-60	2.61			33.0	75	1.57	1.82
W-3-60	2.38			30.5	55	1.88	—

Table 11—Heats Made at 2900 F Under H_2 Atmosphere

HeatNo.	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis H_2	CO	CH_4
Y-1-0		1.25	.43					
Y-1-60	2.24	1.29	.44	26.9	66			
Y-2-0		1.30	.46					
Y-2-60	2.82	1.31	.45	35.5	98			
Y-3-60	2.54			30.9	48			
Y-4-0		1.24						
Y-4-60	2.67	1.24		29.8	130			
Y-5-0		1.26						
Y-5-60	2.01	1.28		24.7	66	45.3	48.8	4.66
Y-6-0	2.54	1.26	.43					
Y-6-60	2.40	1.34	.43	30.5	47	45.6	51.3	3.04

Table 12—Heats Made at 2900 F Under H_2O Atmosphere

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity, in.	Hot Tearing Spring Deflection 0.0001 in.	Effluent Gas Analysis H_2	CO	CO_2	CH_4
Z-1-60	2.47			31.3	60				
Z-2-0	2.94	1.28	.46						
Z-2-60	2.27	1.31	.46	29.1	65				
Z-3-0		1.26	.48						
Z-3-60	2.70	1.33	.48	33.3	60	40.2	47.0	8.10	3.31
Z-4-0		1.29	.47						
Z-4-60	1.92	1.28	.48	29.8	130				
Z-5-0		1.22	.48						
Z-5-60	2.54	1.30	.47	31.7	63				

Table 13—Heats Made at 2900 F Under Air Atmosphere

Heat No.	Metal Composition % C	% Si	% Mn	Fluidity in.	Hot Tear Spring Deflection 0.0001 in.	Effluent Gas Analysis CO_2	O_2	CO
X-1-60	2.32			26.6	60	19.4	1.9	1.3
X-2-0		1.22	.44					
X-2-60	2.64	1.21	.41	31.5	54	18.2	1.7	2.6
X-3-0		1.25	.45					
X-3-60	2.88	1.23	.43	35.6	51	18.4	2.0	1.5
X-4-60	2.78			32.0	50	18.8	2.1	1.1
X-5-0		1.24	.45					
X-5-60	2.28	1.20	.41	—	52	14.2	6.6	.64
X-6-60	2.13			27	60	17.6		
X-7-0	2.83	1.27	.46					
X-7-60	2.08	1.16	.42	25.3	64	18.0	2.2	2.4
X-8-0		1.25	.49					
X-8-60	2.36	1.22	.44	29.7	44	15.6	1.2	7.6
X-9-60	2.46			30.6	50	18.5	2.1	.45

Composition Changes Caused by Furnace Gases

In all previous work, analyses were made of a sufficient number of heats to show that the composition of the iron was in control when the properties of

fluidity, hot tearing, and mottling were being studied. A number of additional analyses were performed to positively establish the effects of furnace atmospheres on the composition of the melt. However, not all heats were analyzed because of their large number. For convenience of reference, the chemical analyses and other data have been assembled in Tables 1-13. The aforementioned tables include all the heats made under furnace atmospheres at 2700 and 2900 F. Considering all the chemical data in the tables, certain conclusions may be advanced. The present information substantiates and extends principles described in earlier papers.[1,2] To note their application in the present research, these principles are briefly reviewed below:

1. At low temperatures, below 2550 F, silicon in the melt controls chemical reactions between the iron and its environment:

a. Silicon protects carbon from oxidation.

b. An iron silicate slag or scum is present on the melt surface.

c. Silicon losses occur if FeO is introduced into the melt.

2. At high temperatures, above 2670 F and especially above 2800 F, carbon controls reactions between the iron and its environment.

a. Decarburization of the melt proceeds rapidly if an oxidizing atmosphere is present although carbon protects silicon from oxidation.

b. Silicon pick-up occurs when the molten iron is in contact with siliceous refractories.

c. Silicon loss will occur only if iron oxides are introduced in the melt. The source may be a slag or some other carrier or generator of FeO.

3. With carbon and silicon both present in the iron, the reactions between the melt and its environment are dependent on relative concentration, temperature, and the presence of anything forming iron oxide.

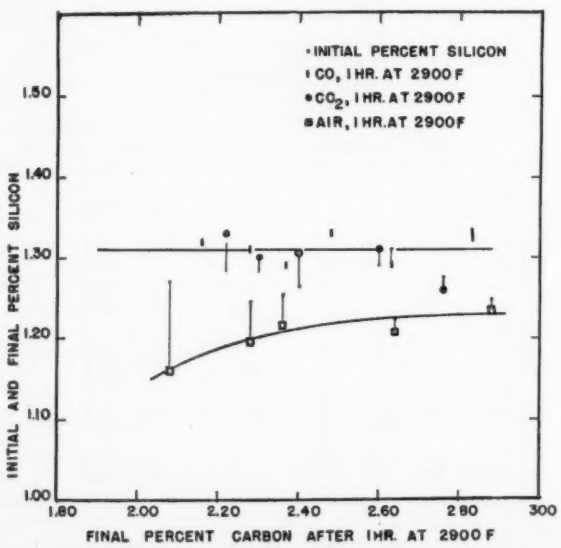


Fig. 1—Graph showing initial and final silicon percentages in duplex iron remelt after holding molten metal iron for 1 hr at 2900 F under indicated gases in a magnesia crucible.

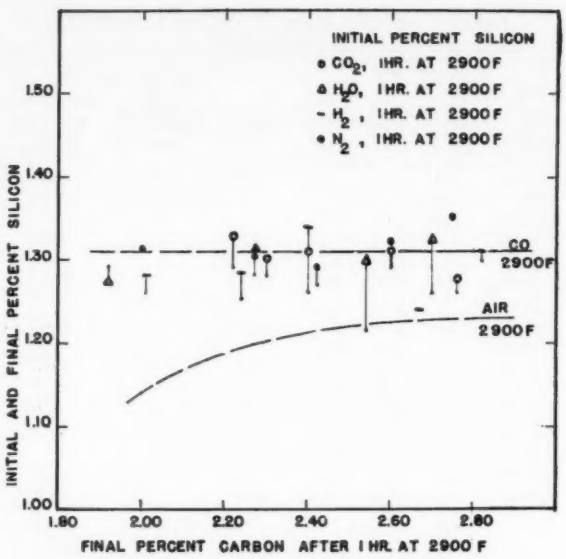


Fig. 2—Graph showing initial and final silicon percentages in duplex iron remelt after holding molten iron for 1 hr at 2900 F under indicated gases in a magnesia crucible.

4. Manganese is protected by carbon at all temperatures above about 2300 F. Losses occur during melting down of the iron or on the introduction of FeO as in the case of silicon but to lesser degree.

The aforementioned principles were established for melting conditions involving siliceous refractories. Melting in the current A.F.S. project was done in magnesia crucibles. Therefore the direct effects of the furnace atmospheres on composition changes can be studied with lessened danger of obscuring silicon losses which can be overshadowed by silicon pick-up from the refractory. The present data may be interpreted with this viewpoint.

Silicon

Relative to changes in silicon content of the melt the furnace gases appear to act either neutral or oxidizing at the holding temperature. A graph of the final and initial silicon percentages as related to the *final carbon percentage of the melt,* held 1 hr at 2900 F under CO, CO_2, and air, is presented in Fig. 1. In evaluating Fig. 1, it should be remembered that only the final carbon percentage of the melt appears on the graph, the initial carbon being substantially higher when decarburizing gases were studied. Several conclusions may be gained from Fig. 1. These are as follows:

1. CO and CO_2 atmospheres are identical in their effect on silicon in the melt. A CO_2 atmosphere causes no silicon loss even though it does oxidize carbon from the melt rapidly. This, in effect, means that carbon in the iron rather than the silicon controls the reactions of the melt with the CO_2 atmosphere at 2900 F. This is true for all the gases except air. If the initial and final silicon percentages of heats made under N, H_2, and H_2O are plotted as in Fig. 2, it can be seen these gases act in the same way as CO and CO_2.

2. Air atmospheres can cause oxidation of silicon at the 2900 F holding temperature but their effect is dependent on the carbon concentration.

Below about 2.50 C, silicon is oxidized by air but above 2.50 C silicon loss virtually ceases. Thus, in the cases studied, reactions of air atmosphere with the melt at 2900 F are controlled by carbon when the carbon content is high, but not when the carbon content is low. Had the melting been performed in a silica crucible, the silicon losses would have been obscured by a silicon pick-up due to silica reduction. In previous work,[2] when melting in a silica crucible under an air atmosphere, silicon losses were not noted in iron of 1.15 Si until the carbon content dropped below 0.87 C at a temperature of 2825 F and below 0.96 C at 2732 F. The limiting carbon concentration which will protect silicon from air oxidation (and cause silicon pick-up from siliceous refractories) is no doubt a function of: (a) the silicon percentage in the iron; (b) temperature; (c) rate of change and type of atmosphere; (d) refractory characteristics; and (e) presence of FeO as, for example, in a slag. With respect to commercial melting, the aforementioned circumstances are such that a net increase in silicon percentage in the melt should occur unless an extremely oxidizing condition (free oxygen) exists in the furnace gases.

3. Using the same melting stock, the level of final silicon analyses is lower in heats held at 2700 F than in those held at 2900 F under the various atmospheres, air excepted. In the higher temperature heats, the majority of final silicon percentages falls between 1.28 to 1.34 pct while at the lower temperatures the range is 1.20 to 1.28 pct silicon.

The foregoing conclusion may be graphically presented. Data for initial and final silicon percentages

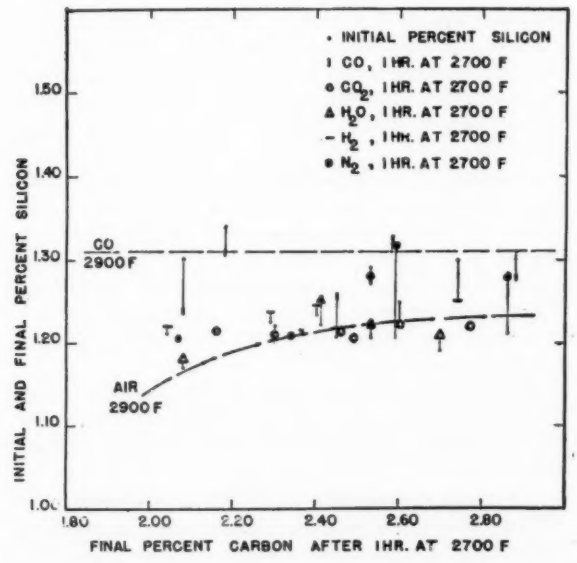


Fig. 3—Graph showing initial and final silicon percentages in duplex iron remelt after holding molten iron 1 hr at 2700 F under gases indicated in a magnesia crucible. Dotted curves are for reference to silicon percentage in irons held at 2900 F.

of heats held 1 hr at 2700 F under various atmospheres except air is presented in Fig. 3. Included on the graph in Fig. 3 are curves showing the upper and lower levels of silicon percentages encountered at 2900 F. It may be observed from Figs. 1 and 2 that most silicon percentages in the 2900 F heats varied above and below the upper curve in the case of the gases not oxidizing to silicon. Figure 3 shows that final silicon percentages in the 2700 F heats are substantially lower. The difference in temperature, 2700 F to 2900 F, appears to mean a difference in final silicon percentage of about 8 to 10 points, higher at the higher temperature.

4. The gases studied, CO, CO_2, N, H_2, H_2O, A, appear to be neutral with respect to reaction with silicon in the melt at 2700 F as they were at 2900 F.

The chemical effects of holding the metal 1 hr at 2700 F in the air atmosphere were not studied. However, the principles applying at 2900 F also apply at 2700 F since it has been amply demonstrated that oxidation of silicon occurs more readily at lower temperatures.[1,2]

Manganese

No appreciable losses or gains of manganese percentage in the melt over the 1-hr holding period were caused by any gas except air at either of the holding temperatures. The variations appearing in Tables 1-13 are mainly within the range of accuracy of the manganese determination. Gains were not expected since no source of MnO existed. However, the air atmosphere (free oxygen) did cause a definite manganese loss at lower carbon contents. Apparently, the manganese loss is concurrent with silicon oxidation. A loss of about 3 to 4 points of manganese to 10 points of silicon occurs at the lower carbon contents. A similar ratio of silicon and manganese losses has been observed in other work when iron oxide was added to a molten cast iron at 2700 and 2900 F.[1]

Endothermic and Exothermic Oxidation

A carbon oxidation rate of about 0.8-1.0 pct per hr was selected as a control on the oxidation variable when carbon loss was a factor caused by the furnace gases. This rate was obtained with H_2O, CO_2 and also with air atmospheres over the melt. While the carbon oxidation rate was the same for the two gases, CO_2 and air, their effect on silicon in the iron was quite different. This is believed to be due to the difference in mechanism of oxidation caused by the two gases. The reaction for oxidation of carbon by a CO_2 atmosphere is as follows:

(1) C (in iron) + CO_2 = 2 CO.

Reaction (1) is an endothermic process. Furnace gas analyses show CO to be the major constituent immediately formed when CO_2 is introduced as the furnace atmosphere.

When air is introduced as the atmosphere, possible reactions are the following:

(2) C (in iron) + O_2 = CO_2

(3) C (in iron) + O_2 = 2 CO;

(4) Fe + O_2 = 2 FeO;

(5) xFeO + Si (in iron) = $zFeO{\cdot}ySiO_2$ + Fe.

When air is used to oxidize the iron the furnace gas analyses always show a high CO_2 content so that re-

action (2) rather than (3) prevails. The former is an exothermic reaction. At lower carbon contents, silicon loss occurs and a silicate slag forms, evidence that reactions (4) and (5) are occurring. The latter reactions are also exothermic. Thus a basic difference in the type of oxidation, exothermic or endothermic, may exist, even with the same carbon oxidation rate. Therefore, carbon loss cannot be used as the only measure of the severity of oxidizing conditions in melting. *Net changes in silicon content make a better distinction of the degree of oxidizing or reducing conditions* which have prevailed during the period that the metal bath is above 2600 to 2700 F.

Thermodynamics of Chemical Reactions

Thermodynamic calculations may be employed to show agreement between the theory of chemical composition changes and the experimental data obtained in this research on reactions between the melting environment (gases and refractory) and the metal. The calculations are not included in this report. However, it is worthwhile to note that in this research work the composition changes occurring during melting follow well defined chemical laws and are therefore known to be under control.

Conclusions

(Composition Changes Caused by Furnace Gases)

Considering the principles of chemical reactions between furnace gases and molten iron as observed in laboratory melting, certain generalizations may be advanced relative to commercial melting. These are given below:

1. No silicon losses will occur at elevated temperature unless free oxygen exists in the furnace gases (or FeO is introduced from some source). Carbon dioxide, CO, N, H_2O have negligible effects on the silicon or manganese in the iron.

2. Net changes in silicon content are the best measure of the severity of oxidizing or reducing conditions which have prevailed during an elevated temperature holding period in a furnace. A silicon loss or a pick-up of only a few points per hour in an acid-lined furnace means that the rate of silicon oxidation (due to gases or slag) is exceeding the rate of silica reduction. Normally, a silicon pick-up of 0.10-0.20 pct per hr is anticipated.

3. Raising the temperature during the refining stage of melting results in a higher silicon recovery; the increase amounting to 8 to 10 points as temperature is raised from 2700 to 2900 F.

4. Silicon losses caused by exothermic oxidation are accompanied by a small manganese loss, about 3 to 4 points of manganese to 10 points of silicon.

5. An indirect effect of oxidizing conditions which decarburize the melt endothermically is to make the iron more susceptible to exothermic oxidation (of silicon and manganese) if the furnace atmosphere should change in that direction by carrying free oxygen.

6. It is probably true that commercial furnace gases exert their greatest influence on composition of the iron by the effects of the gases, especially CO_2, during the melting down period. It is during melting down that iron oxide is most easily formed and this oxide can oxidize silicon and manganese from the iron at any temperature.

7. With respect to the *refining* or high temperature stage of melting practices, furnace atmospheres may be defined in the following way:

a. *Oxidizing.* Oxidizing conditions in the melting furnace atmosphere refer to furnace gases which cause exothermic oxidation of the metal (formation of silicon, manganese, and iron oxides). Such conditions are due to free oxygen in the furnace gases.

b. *Decarburizing and endothermically oxidizing.* All practical melting furnace atmospheres are decarburizing or endothermically oxidizing regardless of their composition since they contain a substantial percentage of CO_2.

3. *Reducing.* In a practical sense, reducing atmospheres are those which result in a minimum rate of carbon oxidation (endothermic). Truly reducing furnace atmospheres which cause the reduction of oxides do not exist. High temperatures, maximum CO percentage, and minimum percentage carbon dioxide in the furnace gases are the closest approach to reducing conditions.

The definitions above apply to the refining or high temperature stage of melting. Different conditions prevail during melting down than in the refining stage.

TABLE 14—DATA FOR HEATS MADE WITH COLD MELT IRON SPRUE

Heat No.	Holding & Pouring Temp., F	Gas Atmosphere	Metal Composition % C	% Si	Fluidity, in.	Hot tear Spring Deflection 0.0001 in.
CK-1-60	2700	CO	2.47		19.3	28
CK-2-60	2700	CO	2.72		23.2	40
CK-3-60	2700	CO	2.31	1.08	17.5	52
CS-1-60	2900	CO	2.35	1.11	30.0	59
CS-2-60	2900	CO	2.63		33.2	55
CP-1-60	2700	N_2	2.33	1.10	14.6	50
CP-2-60	2700	N_2	2.54		19.5	41
CV-1-60	2900	N_2	2.40	1.11	28.8	62
CV-2-60	2900	N_2	2.57		31.4	55
H-4	2700	Air open crucible	2.33	1.05		55
H-1	2700	"	2.53			51
H-7	2700	"	2.74			55
CX-1-0	2900	Air	3.42	1.10		
CX-1-60		Circulating	2.79	1.13	34	50
CX-2-0	2900	Air	(3.47)	1.09		
CX-2-60		Circulating	2.96	1.10	36.9	no break
CX-3-0	2900	Air Directed	3.26	1.02		
CX-3-60		to surface	3.06	.397	28.4	no break
CX-4-0	2900	Air Directed	(3.45)	.968		
CX-4-60		to surface	2.87	.397	30.2	no break
CX-5-0	2900	Air open	(3.28)	1.08		
CX-5-60		crucible	2.46	1.06	27.6	60
CX-6-0	2900	Air open	(3.48)	1.08		
CX-6-60		crucible	2.79	1.09	32.6	42

Furnace Atmospheres and Mottling Tendency

The influence of melting furnace gases on mottling tendency of the iron was studied by noting the fractured surface of the well of the fluidity casting. This is a 1¼-in. diam section. The carbon ranges for white, scattered mottled, highly mottled, and gray fractures are tabulated in Table 15. The table shows that in remelted duplex iron, at equivalent silicon

percentages and a holding temperature of 2900 F, scattered mottling begins at 2.40 C with a CO_2 furnace atmosphere; at 2.48 C for a CO atmosphere; at 2.60 C for N atmosphere; and at 2.78 C for an air atmosphere; and above 2.70 to 2.84 C for water vapor and hydrogen atmospheres. If the composition criterion is %C+% Si rather than %C alone, the figures in the foregoing cases are CO_2, 3.71; CO, 3.81; N, 3.92; and air, 4.01. Comparing the mottling tendency on this basis for molten iron temperatures of 2700 F and 2900 F and the various gases, the following conclusions may be advanced as applying to the remelted duplex iron:

1. Mottling occurs at minimum carbon percentages when holding the molten iron under CO or CO_2 atmospheres (at the same or comparable % Si).

2. CO and CO_2 are virtually equivalent to each other in their effect on mottling tendency. When a pure CO_2 atmosphere is introduced over the melt it quickly reacts to form a furnace atmosphere rich in CO (see data in Tables 1 and 12). Therefore, varying CO-CO_2 ratios in atmospheres would not alter mottling tendencies significantly from those caused by pure CO_2 or CO.

3. Melting under nitrogen or argon atmospheres markedly decreases mottling compared to CO-CO_2 atmospheres (shifts to higher carbon percentage). This may be due to an absence of CO rather than a direct carbide stabilizing effect since it is caused by both N and A.

Table 15—Effect of Holding Furnace Atmosphere on Mottling in Well of Fluidity Casting

		Carbon and Silicon Per cent and Extent of Mottling							
		Gray		Highly Mottled		Scattered Mottle		None, white Fracture	
Holding Gas Atm.		2700 F	2900 F	2700 F	2900 F	2700 F	2900 F	2700 F	2900 F
	C	2.80	2.83		2.63	2.53	2.48	2.45	2.37
CO	Si		1.33		1.29		1.35	1.26	1.29
	C					2.72			2.63
CO*	Si								
	C	2.77	2.76	2.60	2.60	2.50	2.40	2.46	2.30
CO_2	Si	1.22	1.26	1.21	1.31	1.21	1.31	1.22	1.31
	C			2.86	2.75	2.59	2.60	2.53	2.42
N_2	Si			1.28	1.36		1.32	1.28	1.29
	C							2.54	2.57
N_2*	Si					No Data			
	C					2.60	2.61	2.40	2.38
A	Si								
	C							2.70	2.70
H_2O	Si							1.21	1.33
	C							2.74	2.82
H_2	Si							1.25	1.31
	C				2.88		2.78		2.64
Dry AIR [2]	Si				1.23		1.23		1.21
	C		2.96		2.79				
Dry AIR*[1]	Si		1.10		1.13				
	C								3.06
Dry AIR*[2]	Si								0.40
	C								2.79
Dry AIR*[3]	Si								1.09

* Cold-melt Iron Remelt; all others Duplex Iron Remelt
[1] AIR at 6 liters per min. circulating over melt surface.
[2] AIR at 6 liters per min. directed at melt surface.
[3] AIR—normal circulation with open crucible.

4. Air, an atmosphere containing free oxygen, markedly increased the resistance to mottling. Oxidizing melting conditions stabilize the carbide during the freezing process.

5. Hydrogen and/or water vapor, as the furnace atmosphere, promotes maximum resistance to mottling.

6. There is little influence of temperature on mottling tendency since the carbon content for mottling is the same at both holding temperatures or, if anything, mottling occurs at lower carbon content at the higher temperature.

7. Superheating or increasing the temperature alone does not *directly* change the mottling tendency of the iron. Superheating acts indirectly through the effect of temperature on the reactions between the molten metal and the furnace atmosphere.

8. The minimum carbon content at which mottling occurred in the duplex iron was at 2.40 C (1.31 Si) after holding 1 hr under a CO_2 (forms CO) atmosphere. This borderline combination of carbon and silicon at which mottling occurred agrees closely with the information presented by Schneidewind and McElwee,[6] for a 1.25-in. diam section. The current work demonstrates that this carbon content for mottling may be raised to higher than 2.82 C (1.31 Si) depending on the gas atmosphere over the metal. The lowest carbon value probably represents a base level for the silicon content present in the duplex iron. Thus the furnace atmosphere is one of the very significant factors which determines the actual carbon content for mottling (at a given silicon level) and can raise it substantially above the base or minimum carbon content for mottling.

Furnace Atmospheres and Fluidity

Fluidity spiral castings were poured from each controlled atmosphere heat at temperatures of 2700 F and 2900 F. Fluidity in inches spiral length is given in Tables 1-13 for each heat. Individual graphs relating fluidity and carbon content for iron held at 2700 F under each gas of interest, CO, CO_2, N, A, H_2O vapor, H_2, and air, were presented in Reference 4. For a temperature of 2900 F, the fluidity graphs for each gas studied are presented in Figs. 4 and 5. These graphs show decreasing fluidity with decreasing carbon percentage in the iron and of course apply only to the remelted duplex iron with a silicon percentage of approximately 1.30 pct. By assuming an approximately straight line relationship of fluidity and carbon percentage, the idealized fluidity curves for each gas atmosphere may be compared as in Fig. 6. At 2700 F, iron held under a carbon monoxide furnace atmosphere has definitely increased fluidity compared with iron held under other furnace gases. However, at 2900 F this marked difference disappears. Higher temperature thus causes the fluidity property to lose sensitivity to the furnace atmosphere. Increased fluidity under CO at the 2700 F holding temperature appears to be a direct effect of this gas. Increased solubility of CO in the iron at the holding temperature of 2900 F may be the reason for the absence of any appreciable affect of the other furnace atmospheres at that temperature.

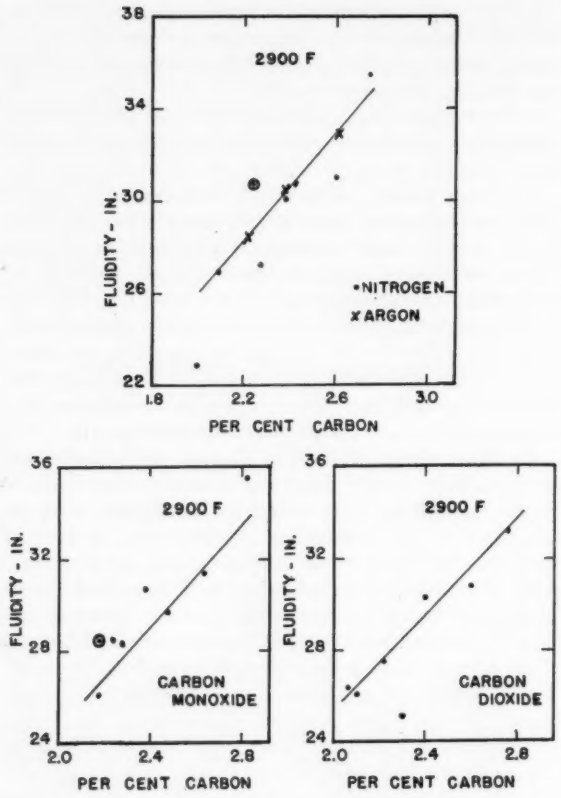


Fig. 4—Graphs showing fluidity of duplex iron remelt at various carbon levels after holding molten iron under gases indicated for 1 hr at 2900 F.

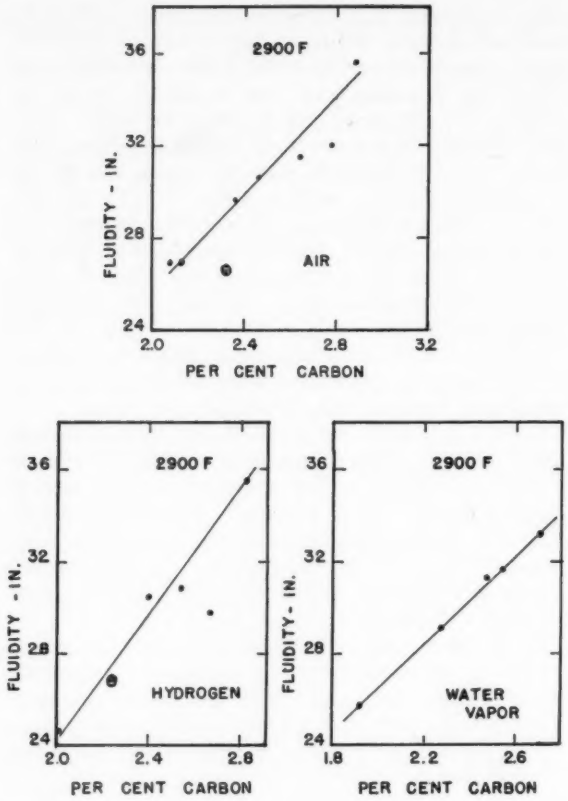


Fig. 5—Graphs showing fluidity of duplex iron remelt at various carbon levels after holding molten iron under gases indicated for 1 hr at 2900 F.

The idealized graphs, Fig. 6, showing the relationship between percentage carbon and fluidity when the duplex iron is held at 2700 F and 2900 F for 1 hr under CO, CO_2, H_2O, H_2, N, A and air atmospheres may be used for comparison purposes. A summary of the influence of furnace atmosphere, carbon, and temperature on change in length of the fluidity spiral is presented in Table 16.

TABLE 16—EFFECT OF CARBON CONTENT, FURNACE ATMOSPHERE, AND TEMPERATURE ON FLUIDITY

Gas Atmosphere	Aver. Change in Fluidity per 10 pts C at 2900 F, in.	Aver. Change in Fluidity per 10 pts C at 2700 F, in.	Aver. Change in Fluidity per 100 F, 2700-2900 F, in.
CO	1.1	1.1	3.9
CO_2	1.1	1.5	6.5
N_2	1.2	1.4	6.5
Air	1.0	1.2	4.6
H_2O	1.1	1.9	5.8
H_2	1.3	1.3	5.9

An over-all picture of the influence of all furnace atmospheres on fluidity is best obtained by plotting all data on one graph, as in Fig. 7. Comparison of fluidity at a common temperature is necessary and this can be done using the information in Table 16. For comparison, the fluidity for each heat at 2700 F was adjusted to 2900 F, using the temperature effect figures in Table 16. Then the fluidity of all heats at both temperatures are plotted on one graph as a function of carbon content, Fig. 7. Figure 7 shows that a carbon range exists, 2.36-2.60 pct C, wherein there is a minimum fluidity variation due to furnace atmosphere or change in carbon percentage. Above and below this carbon range fluidity is more influenced by carbon percentage and furnace atmosphere. Furthermore, Fig. 7 seems to prove that the assumption of a straight line fluidity relationship does not strictly hold in the carbon range of white cast irons. A definite jog in the fluidity-carbon content curve exists at 2.36-2.60 pct C. This effect is especially well illustrated if only the data for CO and CO_2 atmosphere heats are plotted as in Fig. 8.

The idealized curves for comparison purposes can now be redrawn as in Fig. 9, having a jog in the vicinity of 2.36-2.60 pct carbon. The relative positions of the curves in Fig. 9 show the following effects:

1. At high carbon contents, more than 2.60 pct, fluidity at 2900 F is slightly decreased by oxidizing conditions (air) and increased by reducing conditions (CO).

2. At low carbon contents, less than 2.30 pct, oxidizing conditions increase fluidity while reducing conditions decrease it slightly. The idealized curves in

Fig. 9 are presented for comparison with the idealized fluidity curves in Fig. 6 where a linear fluidity carbon content relationship was assumed for 2900 F. The assumption appears untenable.

The experiments with the influence of melting furnace atmospheres on fluidity of the duplex white iron permit the following conclusions to be advanced:

1. Temperature is the most important factor influencing fluidity. Higher temperatures cause the metal to be less sensitive to furnace atmospheres.

2. At the higher temperature, carbon monoxide-carbon dioxide ratios in furnace atmospheres have little direct effect on fluidity since it is virtually the same under either pure gas for iron of a given carbon content, see Fig. 8.

3. The critical carbon range of 2.35-2.60 pct carbon is probably the second most important factor influencing fluidity. The fluidity is least affected by the furnace atmosphere in this critical carbon range.

4. An indirect effect of oxidizing conditions is to cause the iron to reach the low carbon range below 2.35 pct where fluidity is lower and more sensitive to furnace atmospheres. Reducing conditions would indirectly favor the opposite effect. Indirect effects of furnace atmospheres are the result of composition changes of the iron.

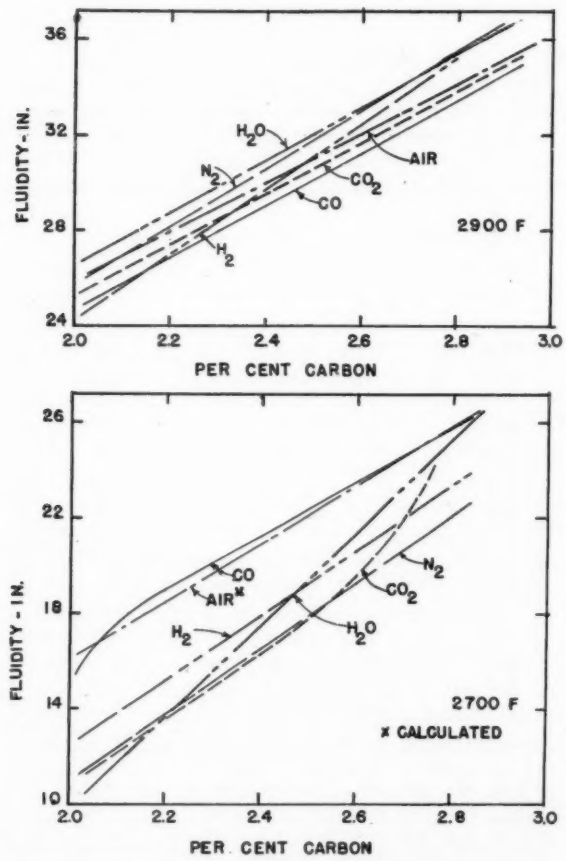


Fig. 6—Graphs showing effect of furnace atmosphere on fluidity of duplex iron remelt after holding molten iron under gases indicated for 1 hr. The straight lines are idealized representations of the data.

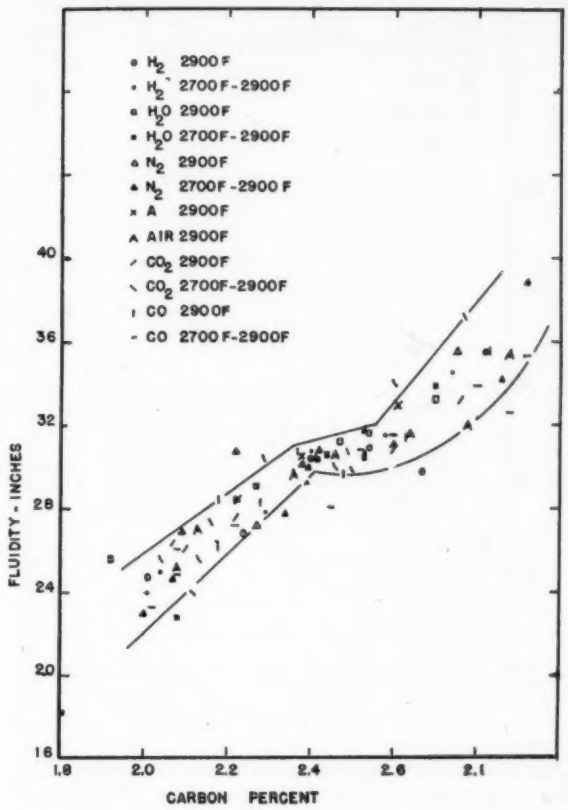


Fig. 7—Plot of fluidity data for duplex iron remelt after holding molten iron for 1 hr under gases indicated. Fluidity data for iron poured at 2700 F was adjusted to a pouring temperature of 2900 F with the aid of Table 16.

5. The effect of increasing temperatures between 2700 F to 2900 F on fluidity is least under a pure CO atmosphere.

Hot Tearing

Hot tear test castings were poured from each controlled atmosphere heat after 1 hr at 2700 F or 2900 F. The hot tear testing machine spring deflector, in ten thousandths of an inch, is equivalent to the pounds load necessary to cause tearing of the casting, Reference 3. The spring deflection values are given in Tables 1-13. Calculation of the strength or resistance to tearing is made by dividing the spring load by the test sample area.

Data for 2900 F—Individual graphs relating hot tearing tendency and carbon content for iron held 1 hr at 2700 F under each gas, were presented in Reference 4. The 2700 F temperature data will be discussed again later when all the data are considered. The new data for a holding temperature of 2900 F could also be presented on individual graphs of carbon content and hot tearing tendency. However, for simplification and summary purposes all the data for 2900 F were plotted on one graph, Fig. 10. This graph shows the relation between carbon percentage and hot tearing resistance under a variety of furnace atmosphere conditions. A study of this graph and the data permits the following conclusions relative

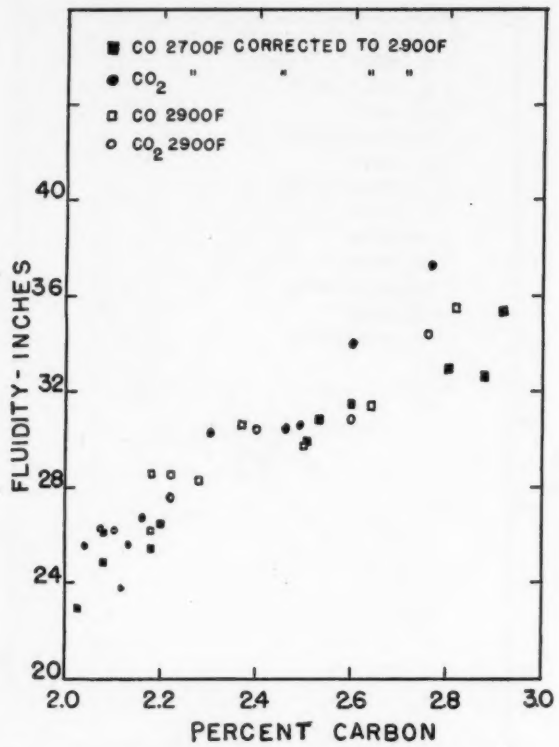


Fig. 8—Plot of fluidity data for duplex iron remelt held for 1 hr under a CO or CO_2 atmosphere. Fluidity data for iron poured at 2700 F was adjusted to a pouring temperature of 2900 F with the aid of Table 16.

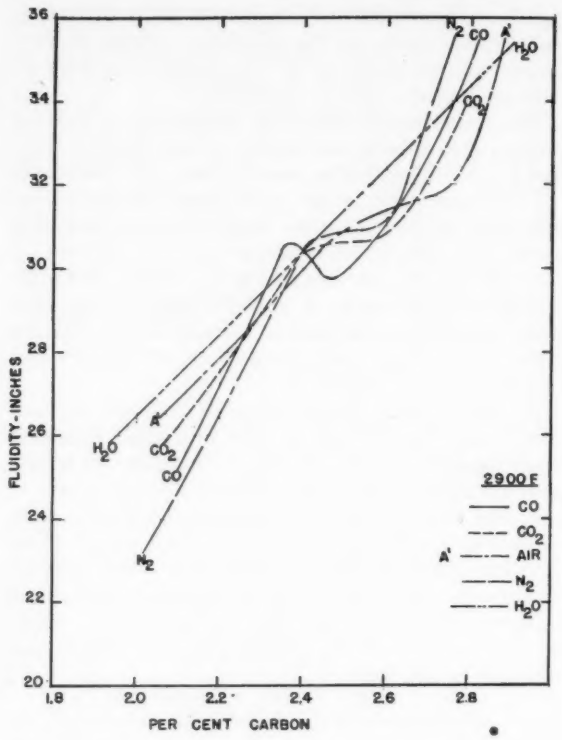


Fig. 9—Graphs showing effect of furnace atmosphere on fluidity of duplex iron after holding 1 hr at 2900 F. Curves are idealized representations of fluidity vs carbon content and show the jog discovered in Figs. 7 and 8.

to the holding and pouring temperature of 2900 F:

1. A carbon range of minimum hot tear resistance exists.

2. In the low carbon range, hot tear resistance is affected by furnace gases as follows:

a. Melting in air (oxidizing) decreases resistance to hot tearing.

b. Nitrogen and argon lower hot tear resistance; probably indirectly, since both are equally effective.

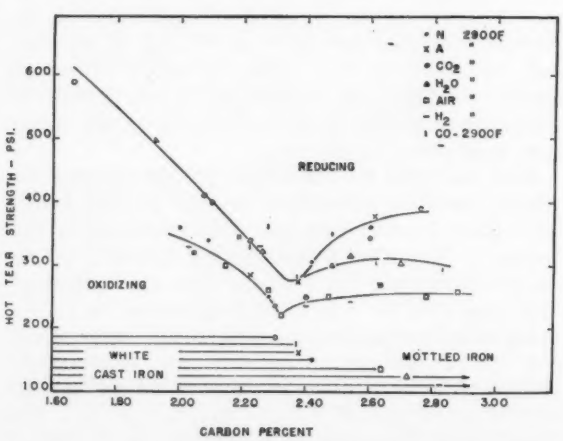


Fig. 10—Graph showing hot tear strength of duplex iron remelt at various carbon levels after holding molten iron for 1 hr at 2900 F under furnace gas atmosphere indicated.

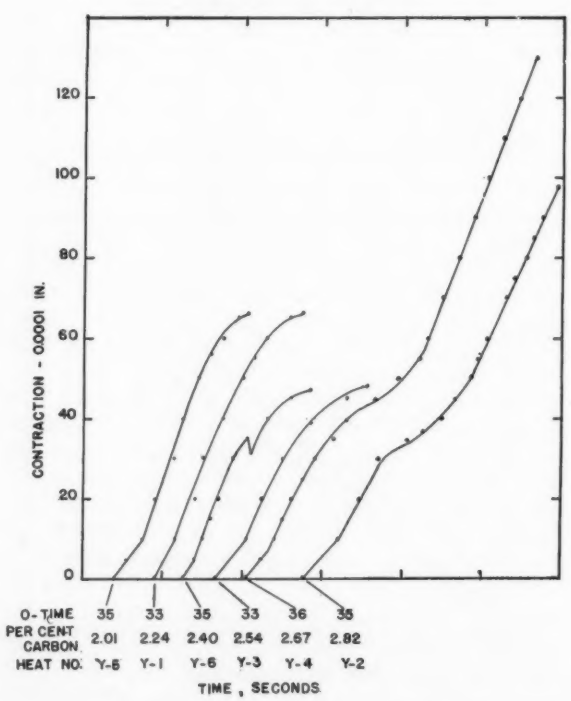


Fig. 11—Contraction vs time curves from hot tear test of duplex iron remelt after holding molten iron for 1 hr at 2900 F under a H_2 atmosphere.

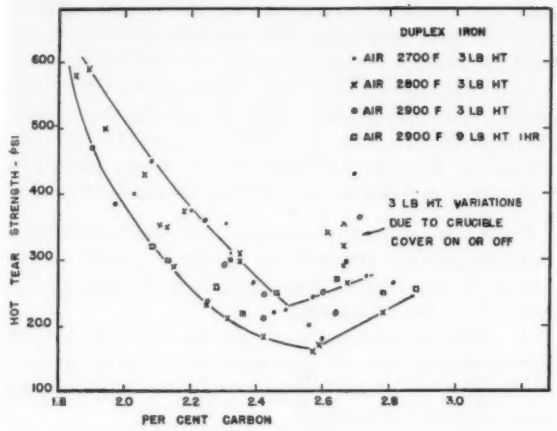


Fig. 12—Graph showing effect of temperature on hot tear strength of duplex iron remelt held under an air atmosphere for 1 hr.

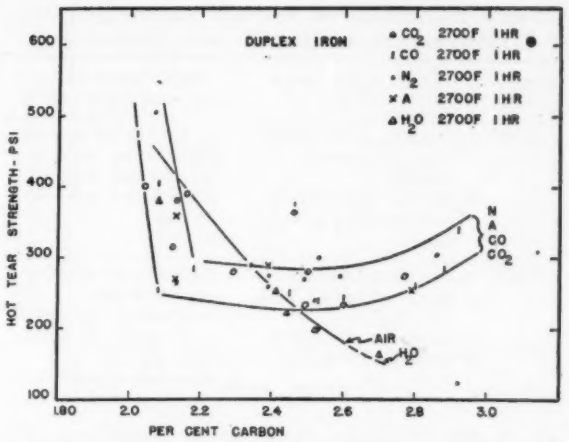


Fig. 13—Graph showing effect of CO, CO_2, N, A, H_2O holding furnace atmospheres on hot tear strength of duplex iron remelt held for 1 hr at 2700 F.

c. CO, CO_2, and H_2O furnace gases raise hot tear resistance.

3. In the high carbon range, hot tear resistance is affected by furnace gases as follows:

a. Melting under air (oxidizing) atmospheres lowers hot tear resistance.

b. Melting under CO, CO_2, N, A, and H_2 atmospheres raises hot tear resistance.

4. High carbon irons, above 2.40 pct, melted under reducing conditions show a greater hot tear resistance than low carbon irons, 2.10 to 2.40 pct carbon, melted under oxidizing conditions. This is a significant point because the lower carbon percentages are reached in melting under abnormal highly oxidizing conditions.

5. Melting under oxidizing conditions lowers hot tearing resistance while reducing conditions improve it at all carbon levels for a holding temperature of 2900 F.

The hot tearing properties of metal under hydrogen atmospheres after 1 hr at 2900 F were very erratic, possibly because of the rapid evolution of this gas during freezing of the iron. This is evidenced by the unique time-contraction curves shown in Fig. 11. The left hand curve in Fig. 11 is the usual time-contraction hot tearing curve. The discontinuities appearing in some of the contraction curves in Fig. 11 result from a breaking and rewelding during hot tearing. Thus, in high carbon irons, either gas evolution during freezing or graphite formation serve to raise resistance to hot tearing.

Review of Hot Tear Data for 2700 F and 2900 F—An over-all picture of the influence of the various atmospheres on hot tearing tendency can be obtained if all the data are plotted on three separate graphs as in Figs. 10, 12 and 13. Figure 10, as discussed earlier, shows the influence of the various furnace atmospheres on hot tearing when the metal is held at 2900 F for 1 hr. The effects of air atmospheres (exothermic oxidation) on hot tearing when the metal is exposed at 2700, 2800, and 2900 F is shown in Fig. 12. Figure 12 includes data obtained with 3-lb heats described in Reference 3 and 4 as well as the 9-lb heats of this paper. The influence of N, A, CO, CO_2, and H_2O atmospheres at 2700 F is depicted in Fig. 13. Hot tearing properties of metal melted under H_2 have not been shown in Figs. 12 and 13 because of the erratic effects caused by this gas.

Certain principles may be advanced if all the data and the aforementioned graphs are considered. A carbon percentage of minimum resistance to hot tearing exists for several of the atmospheres as given in Table 17.

TABLE 17—EFFECT OF TEMPERATURE AND FURNACE ATMOSPHERE ON CARBON PERCENTAGE AND MINIMUM HOT TEARING STRENGTH OF DUPLEX IRON

Gas Atmosphere	% C at Min.		Min. Hot Tearing Strength, psi	
	2700 F	2900 F	2700 F	2900 F
N, A	2.53	2.28	240	250
CO_2	2.54	2.40	245	250
CO	2.53	2.37	240	280
Air*	2.60	2.42	180	220
H_2O	2.70 plus	2.35-2.45	165	300
H_2			Erratic**	

* 2800 F, 2.48% C @ 160 psi.
** Rapid evolution during freezing suspected cause.

Table 17 shows that the effect of the temperatures under N, A, CO, CO_2 and air atmospheres is to shift the minimum point to lower carbon percentages. Strength at the minimum point is not as low at the higher temperature as it is at the lower temperature of holding. An exception noted is hydrogen. With carbon percentages above or below that at which hot tearing strength is at a minimum, the iron appears to be more sensitive to furnace atmospheres. Therefore, the effects of the furnace gases on hot tearing are considered at carbon percentages relative to the minimum range; i.e., high or low carbon irons. Relative to the minimum point the effects of furnace atmospheres can be summarized as in Table 18.

Since A and N have identical effects on the iron, it is believed that nitrogen itself has little direct effect on the iron with respect to hot tearing. It follows that the influence of air is a result of oxygen diluted with nitrogen.

TABLE 18—EFFECT OF FURNACE ATMOSPHERE AND TEMPERATURE ON HOT TEARING STRENGTH*

Temperature	Low Carbon Iron, 2.00 to 2.30% C.	High Carbon Iron, more than 2.45-2.50% C.
2700F	1. CO, CO_2, N, A produce lower hot tearing strength. 2. Air and H_2O cause increased strength, Fig. 13.	1. N, A, CO_2, CO produce high hot-tear strength. 2. Air and H_2O lower strength to a minimum, Fig. 13.
2900 F	1. Maximum strength is produced by CO, H_2O, CO_2, Fig. 10. 2. Minimum strength is produced by air, N, A, Fig. 10.	1. N, A, CO_2 favor maximum strength. 2. Intermediate strength results from CO and H_2O, Fig. 10. 3. Minimum strength is caused, favored by air, Fig. 12.

* Mottling is an additional factor in hot tearing of irons at high carbon contents.

Free oxygen can be an effective furnace gas causing lowered hot tearing resistance especially in intermediate and high carbon irons. On the other hand, carbon monoxide and carbon dioxide produce results closely paralleling each other. Both cause increased resistance to tearing as compared to an air atmosphere when the metal is held at the higher temperature, 2900 F. Thus it appears that two types of furnace atmospheres are effective in changing hot tearing tendency. One type may contain free oxygen. The other type contains carbon oxide gases but no free oxygen. Water vapor has effects like both types of atmospheres, since at lower temperatures it behaves like an air atmosphere and at higher temperatures it parallels the effects of a CO atmosphere.

In low carbon irons at the 2700 F temperature, air causes a higher hot tear resistance than the carbon oxide gases, and N, or A atmospheres. However, at the 2900 F holding temperature a lower resistance to tearing is caused by air than the carbon oxide gases. The cause of this reversal is not known.

The ideas discussed in the foregoing paragraph are presented graphically in Fig. 14 which was derived from the graphs in Figs. 10, 12, and 13. In Fig. 14, reducing melting conditions refer to a combination of atmosphere, carbon content, and temperature such

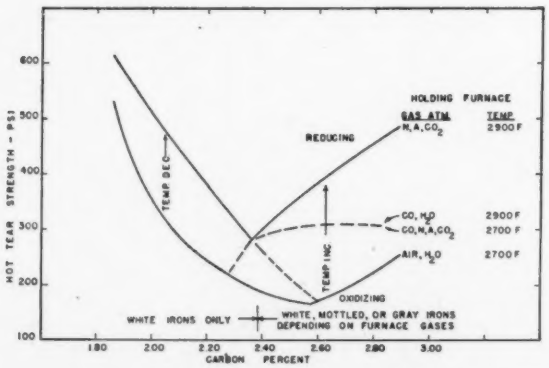


Fig. 14—Summary graph showing effect of holding furnace atmosphere and temperature on hot tear strength of duplex iron remelt at various carbon levels.

that the possibility of metal oxidation (exothermic type) is at a minimum and the CO content of the furnace atmosphere is high. On the other hand, oxidizing conditions refer to an atmosphere where free oxygen may be present and the possibility of exothermic oxidation of the metal is high. The two extremes of furnace atmospheres can cause the variations in resistance to hot tearing shown in Fig. 14.

Summary—The experiments with the influence of melting furnace atmosphere on the hot tearing tendency of the duplex iron permit the following conclusions to be advanced:

1. Melting furnace atmospheres can cause wide fluctuation in the strength developed during freezing of the iron at all carbon percentages (more than 100 pct variation in strength).

2. A carbon range of minimum hot tearing resistance exists for the duplex iron and in these tests falls between 2.28 and 2.60 pct C, depending on furnace atmosphere and holding and pouring temperatures.

3. Resistance to hot tearing in the high carbon range is increased by reducing melting conditions and higher temperatures.

4. Melting under oxidizing conditions (exothermic) at high temperature lowers hot tear resistance at all carbon percentages.

5. High carbon irons, above 2.40 pct, held under reducing conditions at high temperature show a greater resistance to hot tearing than low carbon irons, 2.10-2.40 pct C, held under oxidizing conditions. This is a significant point because the lower carbon percentages may be reached under oxidizing melting conditions.

6. Water vapor has a temperature dependent effect on the hot tearing tendency of the irons. At low temperatures it produces effects like those of air. At higher temperatures it seems to have no effects different from a reducing atmosphere, i.e., high temperature corrects for water vapor effects.

7. On the basis of the pure gas atmosphere tests, it appears that the possible effect of the *combustion gases* on hot tearing tendency could be grouped as follows:

   a. Oxidizing, at a maximum when free oxygen exists in the gas.
   b. Reducing, absence of free oxygen and generally accompanied by a high percentage of CO.
   c. Water vapor effect, functions like "a" above at low temperature and like "b" above at high temperature.

8. Hydrogen produces erratic results sometimes causing high resistance to hot tearing and other times strength in the normal range.

Contraction Rate in Hot Tearing—The rate of contraction of a casting during cooling may profoundly influence the tendency for hot tearing. The slope of the time-contraction curve obtained from the hot tear test may be used as a measure of the contraction rate, a procedure which has been described in Reference 3. The contraction rate value in 0.00010 in. per sec was obtained by determining the slope of the time-contraction curve during the straight line portion of the curve; a typical curve being the lefthand one in Fig.

Table 19—Initial Contraction Rates of the Hot Tear Casting Poured from Duplex Iron Remelt after Holding the Molten Iron for 1 Hr at Temperature under the Gas Atmosphere Indicated

Holding Furnace Conditions

Heat No.	Contraction Rate, 0.0001 in./sec.	Heat No.	Contraction Rate, 0.0001 in./sec.
Atm. CO_2; Temp 2700 F		Atm. CO; Temp. 2700 F	
Q- 1	5.0	K- 1	3.3
Q- 2	3.5	K- 2	3.2
Q- 3	3.5	K- 3	3.5
Q- 4	3.2	K- 4	3.3
Q- 5	4.6	K- 5	2.3
Q- 6	2.5	K- 6	2.7
Q- 7	3.5	K- 7	3.0
Q- 8	3.2	K- 8	3.4
Q- 9	3.3	K- 9	3.8
Q-10	5.0	K-10	3.0
Atm. N—2700 F		K-11	3.3
P- 1*	4.6	Atm. A—2700 F	
P- 2*	4.0	L- 1	3.7
P- 3*	3.4	L- 2	3.3
P- 4*	3.8	L- 3	3.8
P- 5	5.0	L- 4	4.3
P- 6*	2.3	L- 5	3.0
P- 7	2.7	L- 6	2.5
P- 8	5.0	L- 7	3.4
P- 9	3.6	L- 8	3.8
P-10	2.0	Atm. H_2—2700 F	
P-11	2.0	M- 1	2.7
P-12	3.4	M- 2	2.2
P-13	2.5	M- 3	2.4
P-14	5.0	M- 4	3.1
P-15	2.1	M- 5	2.0
P-16	2.8	M- 6	2.8
P-17	3.3	M- 7	3.4
* Held for ½ hr. under N_2 atm.		Atm. H_2O—2900 F	
Atm. H_2O—2700 F		Z- 1	3.5
R- 1	2.3	Z- 2	3.2
R- 2	3.0	Z- 3	2.6
R- 3	4.3	Z- 4	2.5
R- 4	4.0	Z- 5	2.7
R- 5	2.0	Atm. H_2; Temp. 2900 F	
R- 6	2.5	Y-1	2.9
Atm. CO_2; Temp. 2900 F		Y-2	2.2
U-1	2.7	Y-3	2.3
U-2	4.4	Y-4	2.3
U-3	3.0	Y-5	3.2
U-4	3.2	Y-6	2.5
U-5	2.9	Atm. N; Temp. 2900 F	
U-6	3.2	V-1	3.1
U-7	3.3	V-2	2.1
U-8	3.5	V-3	2.8
Atm. A; Temp. 2900 F		V-4	4.2
W-1	3.3	V-5	3.9
W-2	2.0	V-6	3.5
W-3	2.8	V-7	2.5
Atm. CO; Temp. 2900 F		V-8	2.5
S-1	3.4	Atm. Air; Temp. 2900 F	
S-2	2.7	X-1	3.1
S-3	3.3	X-2	2.6
S-4	3.2	X-3	1.7
S-5	3.8	X-4	2.1
S-6	3.6	X-5	3.0
S-7	3.6	X-6	3.4
S-8	3.6	X-7	3.2
S-9	2.2	X-8	2.8
		X-9	2.6

11. Contraction rates for the atmosphere heats at both temperatures are provided in Table 19. These rates are related to carbon content of the duplex iron as illustrated in Fig. 15 and 16. From the data and Figs. 15 and 16 a number of conclusions may be stated as follows:

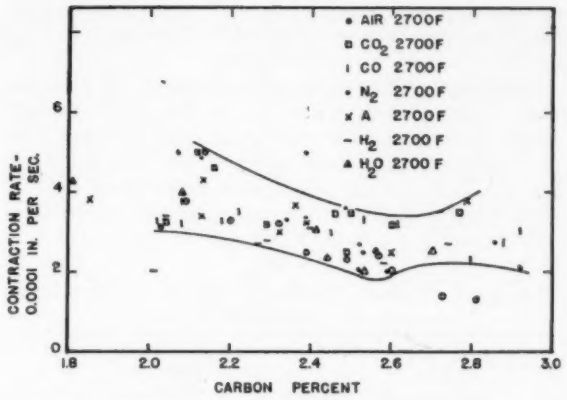


Fig. 15—Graph showing initial contraction rate of hot tear castings poured from duplex iron remelt held for 1 hr at 2700 F under gases indicated.

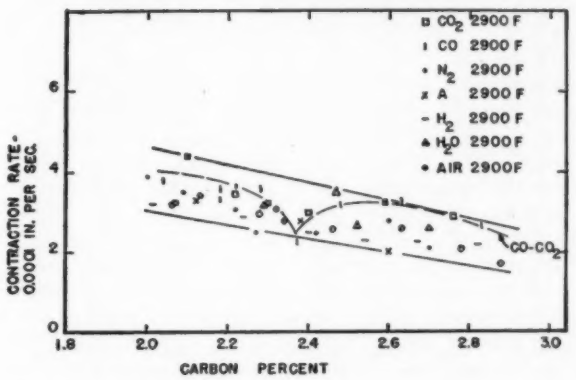


Fig. 16—Graph showing initial contraction rate of hot tear castings poured from duplex iron remelt held for 1 hr at 2900 F under gases indicated.

1. Under all atmospheres and at both holding temperatures contraction rate during freezing and tearing of the test casting decreases with increasing carbon percentage in the duplex iron.

2. A hydrogen furnace atmosphere produces minimum contraction rates at all carbon levels. The lower contraction rate is probably due to gas evolution during freezing.

3. For the iron held at 2900 F under the various atmospheres there appears to be a critical carbon range at about 2.30 to 2.40 C, having a minimum contraction rate. Under CO-CO_2 atmospheres the minimum occurs at 2.37 C, while under N atmosphere it occurs at 2.30 C. It is noteworthy that the properties of contraction rate, fluidity, and resistance to hot tearing all have critical carbon ranges of similar magnitude.

4. At a holding temperature of 2700 F, the point of inflection (carbon range for minimum contraction rate) is not so well defined but it does seem shifted to a high carbon range, approximately 2.50 to 2.60 C.

Annealability

The study of the nature of the response to malleabilizing heat treatment in white iron held for one hour at 2700 F and 2900 F under various furnace

atmospheres was begun with metal in castings remaining from the studies on fluidity, hot tearing, and mottling.

Experimental Procedure—Samples were cut from the ½-in. diam shank section of the hot tear castings. White irons at three carbon levels, 2.20, 2.40, and 2.60 pct, approximately, were selected for this study. A series of the castings were taken from heats held at 2700 F and 2900 F for 1 hr under each gas atmosphere. The heats are listed in Table 20.

TABLE 20—NODULE COUNT IN ANNEALED HOT TEAR CASTINGS OF DUPLEX IRON REMELT

Holding Temp. 2700 F				Holding Temp. 2900 F			
Heat No.	Holding Furnace Atm.	% C	Nodules per mm²	Heat No.	Holding Furnace Atm.	% C	Nodules per mm²
K- 9	CO	2.08	180	S- 6	CO	2.18	186
K-10	CO	2.45	209	S- 9	CO	2.37	193
K- 2	CO	2.60	217	S- 3	CO	2.63	245
Q- 5	CO_2	2.16	123	U- 2	CO_2	2.10	130
Q- 2	CO_2	2.46	150	U- 3	CO_2	2.40	182
Q- 4	CO_2	2.60	152	U- 4	CO_2	2.60	195
P- 8	N_2	2.13	123	V- 6	N_2	2.09	89
P-12	N_2	2.39	112	V- 8	N_2	2.42	100
P-10	N_2	2.59	134	V- 3	N_2	2.60	104
L- 7	A	2.13	134	W- 1	A	2.24	209
L- 2	A	2.39	158	W- 3	A	2.40	163
L- 6	A	2.60	159	W- 2	A	2.67	213
M- 7	H_2	2.04	140	Y- 1	H_2	2.27	209
M- 4	H_2	2.40	179	Y- 6	H_2	2.40	155
M- 3	H_2	2.74	218	Y- 4	H_2	2.67	204
R- 4	H_2O	2.08	182	Z- 2	H_2O	2.27	131
R- 2	H_2O	2.41	176	Z- 1	H_2O	2.47	123
R- 6	H_2O	2.70	194	Z- 3	H_2O	2.70	112
G-81	Air	2.08	120	X- 6	Air	2.13	92
G-62	Air	2.39	106	X- 8	Air	2.36	119
G-61	Air	2.60	110	X- 2	Air	2.64	118

The shank of the hot tear casting was sectioned as shown in the diagram of Fig. 17. The sections were given the heat treatment shown by the graph in Fig. 17 and described in Table 21.

TABLE 21

Section	Treatment*
A	No heat treatment.
4	Removed from furnace after half of first stage graphitization period; 15 hr at 1700 F.
3	Removed from furnace after the complete first stage graphitization period; 30 hr at 1700 F.
2	Removed from furnace after half of second stage graphitization period; 12 hr at 1320 F.
1	Full anneal, removed from the furnace at the end of the heat treatment; 12 hr at 1320 F plus 12 hr at 1300 F.

* See Fig. 17.

The ½-in. diam x 1-in. long samples were enclosed in 1½-in. x 8-in. pipe nipples sealed with pipe caps. The samples from the heats melted under hydrogen and water vapor atmospheres were placed in separate containers. The tubes containing the samples were placed in a refractory muffle which was heated by an electric furnace. Temperature control was maintained at ± 3 F. After receiving the heat treatments outlined in Fig. 17, the containers were removed from the furnace and allowed to cool in air. When room temperature was reached the

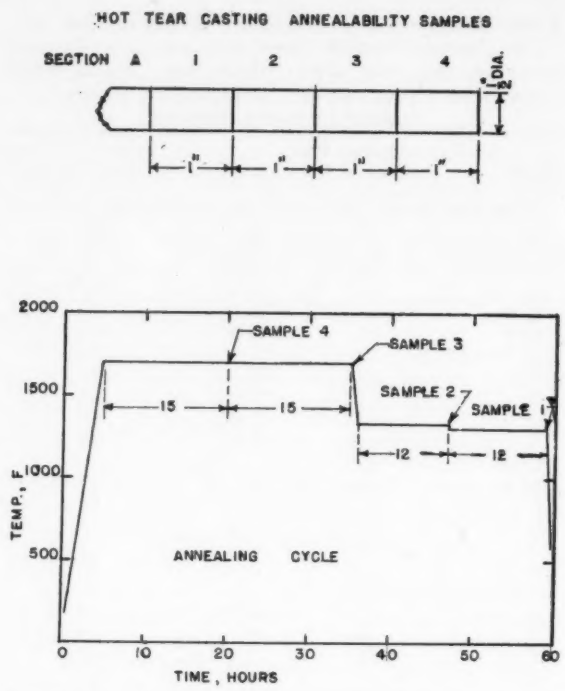


Fig. 17—Diagram showing how the hot tear casting was sectioned for annealability samples. The annealing cycle for the samples poured from duplex iron remelt is graphically illustrated.

samples were removed from the pipes and sectioned again for microscopic examination. The fluidity well castings for heats made under N, CO, and air atmosphere at a carbon level of approximately 2.40 pct were also annealed to observe graphitization in a heavier section, 1.25-in. diam. Data on the annealed fluidity well castings is given in Table 22. In this case, first stage graphitization was extended to 45 hr at 1700 F, while second stage graphitization time was extended to 41 hr as compared with the cycle shown in Fig. 17 and Table 21.

TABLE 22—NODULE COUNTS IN ANNEALED FLUIDITY WELL CASTINGS OF DUPLEX IRON REMELT

Holding Furnace Conditions				
Heat No.	Atm.	Temp., F	% C	Nodules per mm²
K-10	CO	2700	2.45	125
S-9	CO	2900	2.37	91
P-12	N_2	2700	2.39	96
V-8	N_2	2900	2.42	85
X-8	Air	2900	2.36	86

Results of Malleabilizing Heat Treatment

The annealed samples were examined metallographically for structure at four points in the annealing temperature-time cycle. These points were the half time of the period for first stage graphitization, the end of the period allowed for first stage graphitization, half time of second stage graphitization, and the end of the complete heat treatment. The samples revealed that the annealing cycle was adequate for complete graphitization of some of the test pieces but

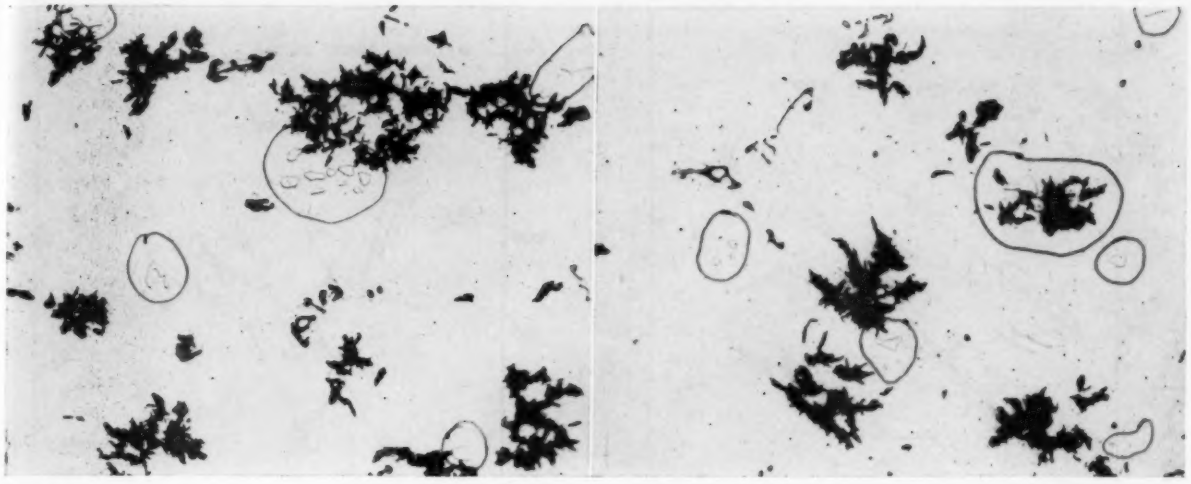


Fig. 18—Stable carbides in fluidity well castings of iron held 1 hr at 2900 F under a CO melting furnace atmosphere, Heat S-9, 2.37 C. Note intimate association of carbides and graphite. Nital etched. Mag. 250x.

not others. It was evident that the type of gas atmosphere was a more important factor in determining the completeness of graphitization than was the carbon content of the iron, or the temperature of the holding period during melting (2700 or 2900 F). The effects of the furnace gases on graphitization can be grouped as follows:

1. Nitrogen, argon, and air melting furnace atmospheres allowed complete graphitization during the annealing cycle.
2. Carbon monoxide, carbon dioxide, and water vapor melting furnace atmospheres caused resistance to complete graphitization especially during the first stage of graphitization. Stable massive carbides persisted.
3. Hydrogen atmospheres permitted graphitization to proceed almost as readily as N, A, and air but a few carbides remained at the end of the first stage graphitization.

Observations expanding on the effects grouped above are presented in the following paragraphs.

Stability of Carbides—Melting furnace atmospheres containing carbon monoxide caused the massive carbides to resist graphitization during the first stage of the anneal. Furthermore, the massive carbides present after the first half of the first stage anneal appeared to be relatively stable because the second half of the first stage anneal did not markedly decrease their number or size. This was noted in samples from heats held 1 hr under CO, CO_2, and H_2O atmospheres at 2700 F and 2900 F. On the other hand, first stage graphitization of the iron held 1 hr under N, and A, and air was complete during the first half of the first stage graphitization holding period. The same effect of the gases on stability of the carbides was noted in heavier section samples. Fluidity well castings from heats held under N and air showed completed first stage graphitization in 45 hr at 1700 F. However, after 45 hr at 1700 F a substantial amount of carbides remained in iron held under a carbon monoxide melting furnace atmosphere. The increased section size of the fluidity well casting augmented the furnace atmosphere effects obtained in the samples taken from the hot tear test. The type of carbide remaining from incomplete first stage graphitization is illustrated in Fig. 18. These carbides appear to be relatively stable since they are often intimately associated with graphite. Such stable carbides are one effect on graphitization of a carbon monoxide-rich atmosphere over the melt during the high temperature stage of melting. Data showing how the stability of the carbides is related to the carbon monoxide content of the effluent furnace gases is presented in Table 23.

Table 23—Stable Carbides Compared with CO Content of Holding Furnace Atmosphere

Furnace Atmosphere Introduced	CO Content of Effluent Furnace Atmosphere	Stable Carbides as in Fig. 18
Purified CO	98% +	Yes
" CO_2	60% +	Yes
" H_2O	40 - 47% CO	Yes
" H_2	40 - 50% CO	Borderline
" A	2%	No
" N	2% CO	No
" Air	low usually below 2%	No

Hydrogen—A hydrogen furnace atmosphere was not observed to cause the same resistance to graphitization caused by CO. Graphitization was almost as complete for hydrogen treated iron as it was with N, A, or dry air treated iron. The resistance to graphitization produced by the CO bearing melting furnace atmosphere did not develop in the hydrogen treated irons. Of course, it should be remembered that the annealing conditions were such as to permit the hydrogen to escape during heat treatment. Thus, hydrogen provides a good example of a gas promoting resistance to graphitization during freezing, since it decreases mottling tendency, but does not cause carbide stability during annealing under proper conditions where hydrogen may diffuse out of the metal during annealing.

Graphite Characteristics—The size, shape, and number of graphite nodules present in the malleabilized

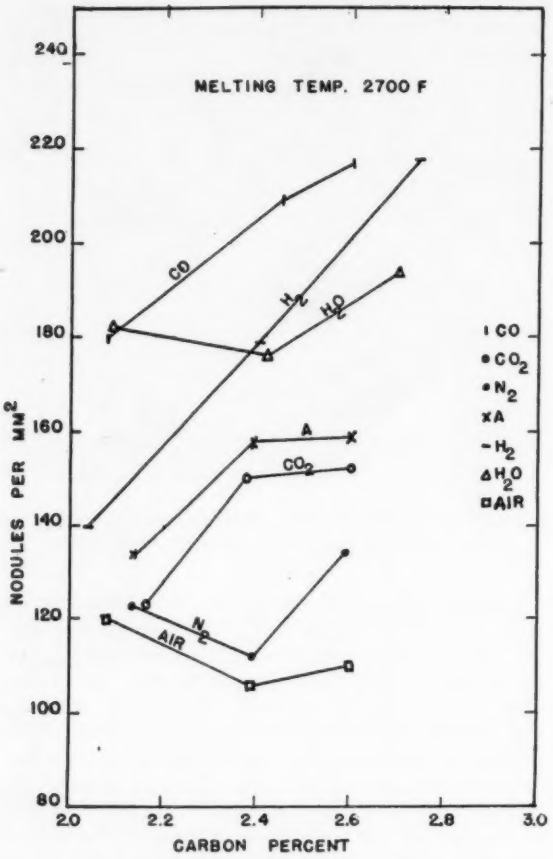


Fig. 19—Graph showing effect of holding furnace atmosphere on nodule count in annealed malleable iron. Data obtained from hot tear casting of duplex iron remelt held for 1 hr at 2700 F under the gas atmosphere indicated.

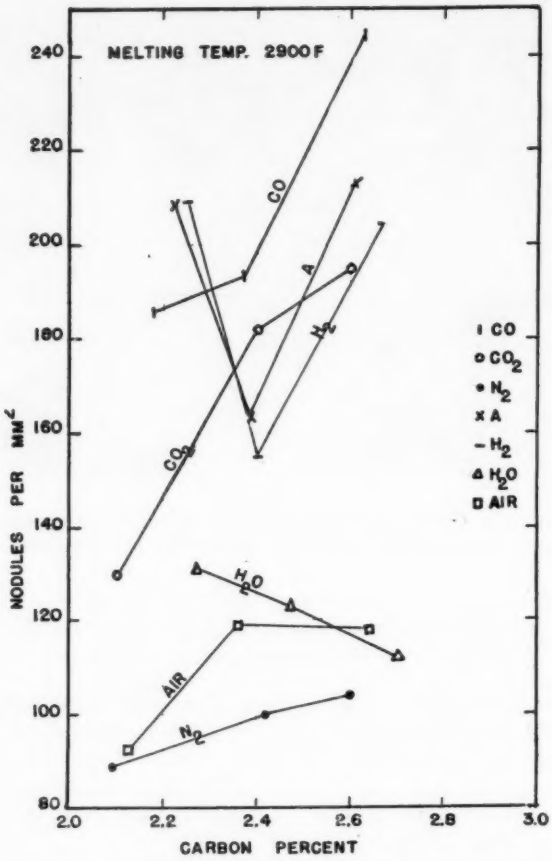


Fig. 20—Graph showing effect of holding furnace atmosphere on nodule count in annealed malleable iron. Data obtained from hot tear casting of duplex iron remelt held for 1 hr at 2900 F under the gas atmosphere indicated.

samples were investigated. The number of nodules per square millimeter of sample surface was determined by a counting procedure suggested by the Malleable Research Committee. The procedure is outlined below:

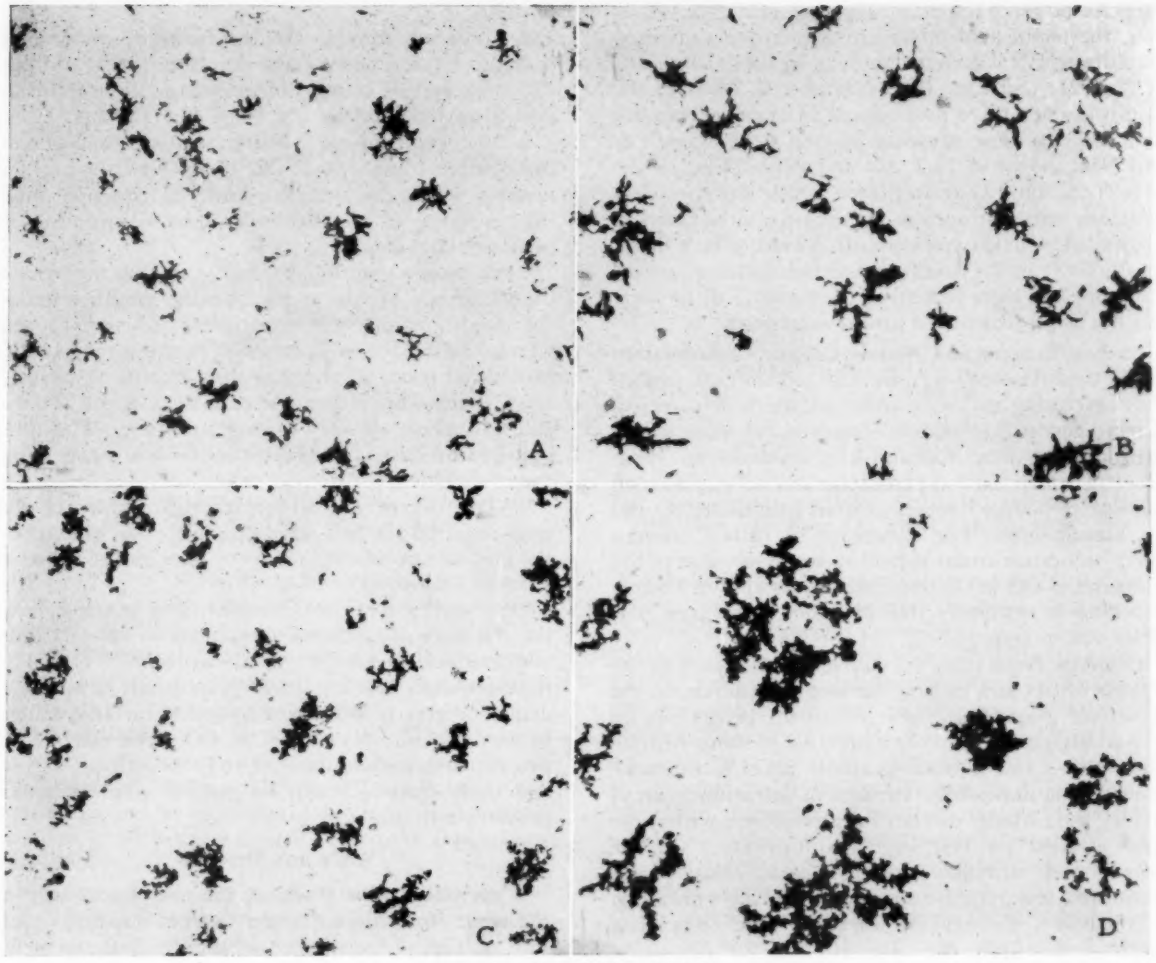
1. Polish the specimen;
2. Etch sample lightly with 2 pct Nital;
3. Project image on ground glass plate at 200X using 8X, 0.20 N.A. objective and increase magnification with a 10X eyepiece.
4. Count graphite particles in image area of 100 sq cm; an actual sample area 0.25 sq mm, at four different locations on the specimen.
5. Add the four counts to give the nodule count for 1 mm of specimen area. Repeat steps 4 and 5 until an average nodule count is obtained.

The magnification of 200X was used instead of 100X because of the many fine graphite particles in some of the samples. The 200X magnification also aided in distinguishing separate particles from those which were inter-connected as large groupings. The counting procedure yielded the nodule counts listed in Tables 20 and 22.

Nodule counts showed that the furnace gases had a marked effect on the number of graphite particles present in the annealed samples. A comparison of nodule counts existing in irons of three carbon levels and the influence of the furnace gases on the metal after holding 1 hr at 2700 and 2900 F may be obtained from the graphs, Figs. 19 and 20. These graphs are meant primarily to be used for comparison of the effects of the atmospheres and are not intended to show curves relating carbon content to nodule count under a particular atmosphere. Both graphs reveal that samples from irons held under CO, CO_2, H_2 and A have substantially higher nodule counts than samples from iron melted under N_2 and air. The aforementioned effect is more pronounced at the higher holding temperature of 2900 F. The nodule number is not decreased by the higher temperature in the case of the first-named gases but is actually somewhat increased. However, some decrease of nodule number at the higher holding temperature occurs with N, air, and water vapor furnace atmospheres. This may be due to direct effects of N and O on the iron. The extremes of graphite are illustrated in Fig. 21a, b, c and d, the low count occurring in an air type atmosphere treated iron and the high count in a CO atmosphere iron.

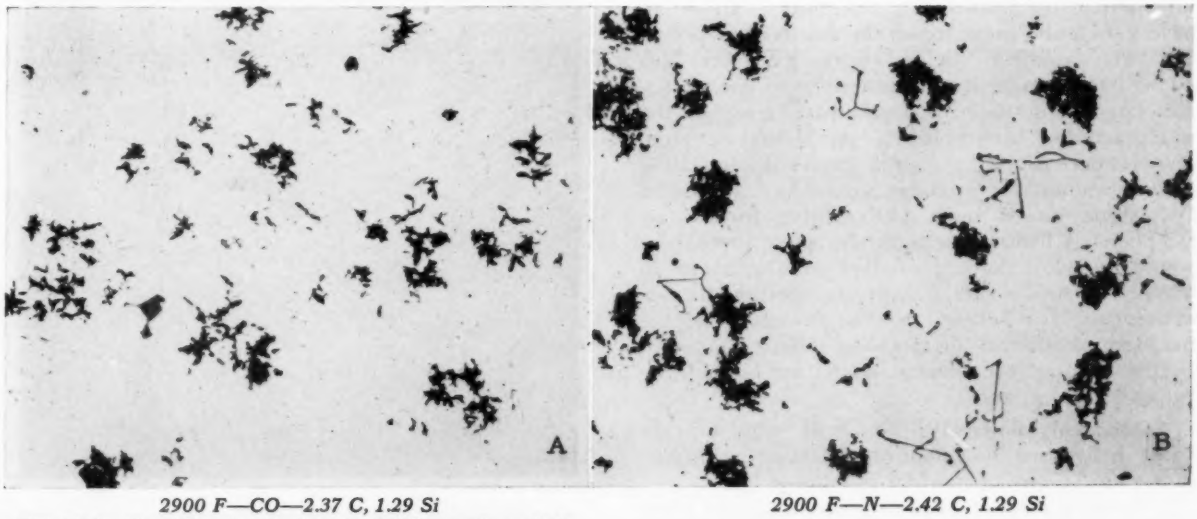
Nodule counting was found to be quite difficult because of the type of graphite shown in Fig. 21a and b. The iron held under CO atmosphere or under at-

2900 F—CO—2.63 C, 1.29 Si



2900 F—Air—2.64 C, 1.21 Si

Fig. 21A—Graphite distribution in hot tear casting samples of duplex iron remelt held 1 hr under CO melting furnace atmosphere. Heat S-3. Mag.—150x. Fig. 21 B—Same as Fig. 21A except Mag. 250x. Fig. 21C—Graphite distribution in metal held 1 hr under air atmosphere. Heat X-2. Mag.—150x. Fig. 21D—Same as Fig. 21C. 250x.



2900 F—CO—2.37 C, 1.29 Si

Fig. 22A—Graphite distribution in fluidity well castings of metal held 1 hr at 2900 F under a CO melting furnace atmosphere, Heat S-9. Mag.—150x.

2900 F—N—2.42 C, 1.29 Si

Fig. 22B—Graphite distribution in metal held 1 hr under N atmosphere, Heat V-8. Mag.—150x .

mospheres high in CO contained many small flake-like particles of graphite, as in Figs. 21a and 21b. However, the metal held under air or nitrogen contained graphite which was more nodular in shape as shown in Figs. 21c and 21d. The difference in effect of the gases was even more pronounced in annealed fluidity well castings. The graphite in iron held under CO and N is shown in Figs. 22c and 22b. Thus, in the heavier section, CO again caused a loose flaky graphite structure while nitrogen or air caused a more compact nodule. The flaky type graphite is held to be a direct result of CO in the iron from CO-rich melting furnace atmospheres. Note that silicon percentage of the iron was not a factor involved in this conclusion.

Carbon Content and Nodule Count—Consideration of the nodule count graphs, Figs. 19 and 20, reveals that increasing carbon content in the iron caused a marked increase in nodule count (many more flakes) in irons held under CO and CO_2 atmospheres. However, increasing carbon percentage did not cause much increase in nodule number in irons held under N_2 and air atmospheres. The difference in carbon content effect on nodule count is held to be largely due to the influence of CO in causing flake type graphite. Nodule counting is extremely difficult when flake type particles are present.

Graphite Nucleation vs. Carbide Stability—The influence of CO as a melting furnace atmosphere on the results of graphitizing heat treatment brings out an important point. Carbon monoxide melting furnace atmospheres caused many graphite flakes to be nucleated during annealing. In spite of the abundance of nuclei, the carbides did not fully graphitize within the time allotted for first stage graphitization. On the other hand, nitrogen melting furnace atmospheres permitted less graphite nucleation during annealing. Nevertheless, the massive carbides were completely eliminated within the time allotted for first stage graphitization. Thus, carbide stability and graphite nucleation tendency may vary independently of each other, being influenced by the melting furnace gases.

Annealing Furnace Atmosphere—The atmosphere surrounding the iron during annealing is known to have a profound influence on the results of graphitizing heat treatment. In this work, the metal was allowed to generate its own atmosphere. An artificially imposed atmosphere would have modified the results reported herein and this fact should be recognized. For example, a carbon monoxide annealing furnace atmosphere probably would have magnified the effects caused by a CO melting furnace atmosphere. A hydrogen annealing furnace atmosphere would probably have prevented graphitization in white iron held under a hydrogen melting furnace atmosphere. It is believed that the procedure adopted has been satisfactory in revealing differences caused by the melting furnace atmosphere, the latter being the object of the study.

Annealability Summary—The work with annealability has served to demonstrate several principles. These are briefly summarized as follows:

1. Melting furnace gases can have a greater effect on nucleation of graphite, nodule count, and carbide stability than does carbon percentage in the iron or temperature of the iron during high temperature stage of melting.

2. Carbon monoxide melting furnace atmospheres promote nucleation of flaky graphite during annealing, high nodule counts, and resistance to graphitization of carbides in the first stage of annealing.

3. Nitrogen and air melting furnace atmospheres cause more compact nodules to develop during annealing, lower the nodule count, and do not cause the resistance to graphitization noted as an effect of carbon monoxide.

4. Hydrogen melting furnace gases do not cause the white iron to present an annealing problem under the conditions of these experiments when hydrogen which might have been retained in the iron may diffuse out of it during the annealing treatment.

5. Nodule count does not directly indicate the degree of carbide stability during annealing. The time required for first stage graphitization is a better measure of carbide stability.

6. The degree of carbide stability influences the time required for first stage graphitization more than the graphite nucleating tendency does on the range of analyses studied.

Of course, a great deal remains to be learned about the influence of furnace atmospheres on annealability and mechanical properties of malleable iron. The time required for first stage graphitization will evidently be greatly influenced by stable carbides caused by furnace atmospheres rich in CO. The mechanical properties should be sensitive to graphite type. These and other questions will be studied as the research project continues.

White Iron Structure

A metallographic study of the as-cast structure of the white iron in the hot tear castings was made. Section A, Fig. 17, provided the sample of as-cast struc-

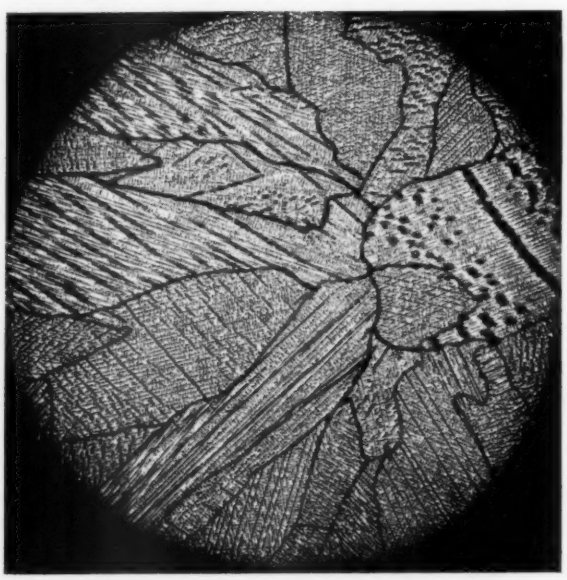


Fig. 23—Grain size developed during freezing of hot tear casting, Heat V-3. Grain outlines are drawn on basis of dendrite orientation. Nital etched. Mag.—Approx. 8x.

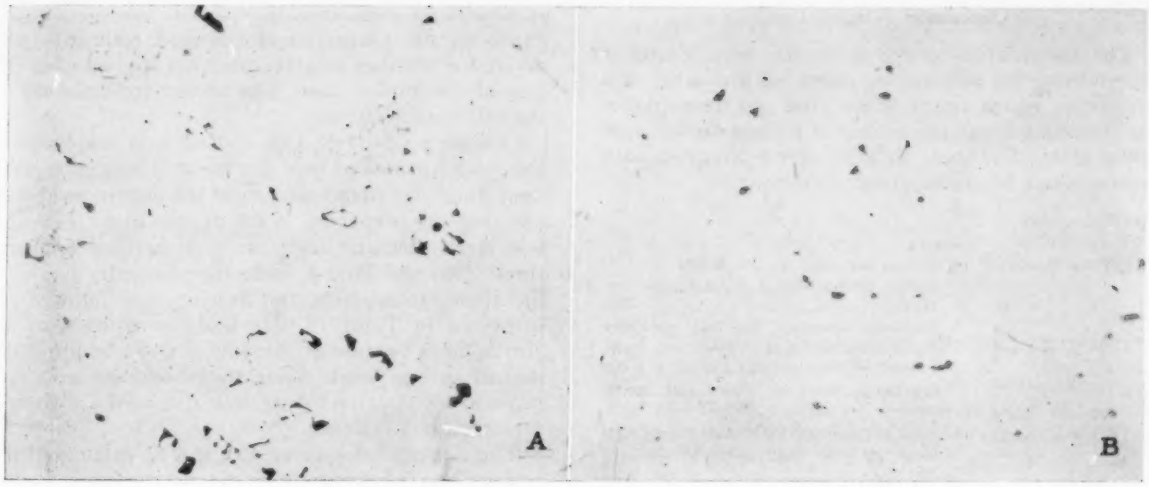


Fig. 24A—Skeletal groupings of sulfides in white irons from Heat S-6, 2.18 C. Mag.—500x.

Fig. 24B—Small, randomly distributed sulfides in white iron from Heat V-3, 2.60 C. Mag.—500x.

ture in the case of those hot tear castings which were given annealability tests. Structural features studied were the following:

1. Microstructure; carbide network, cell size, pearlite and eutectic appearance. 2. Macrostructure, grain counts. 3. Non-metallic inclusions.

Microstructure—No clearly defined microstructural changes in the massive carbides, cell size, pearlite, or appearance of the eutectic could be related to the melting furnace gases. Certain differences did seem to exist but these were so elusive as to make conclusions unjustifiable.

Macrostructure—The as-cast structure was studied at low magnification for possible effects of gases on the grain size developed during solidification. An example of this technique is illustrated in Fig. 23. No correlation could be established between the number of grains per cross-section of the hot tear casting as illustrated in Fig. 23, and melting furnace atmosphere or any other factor.

Inclusions—The non-metallic inclusions, principally sulfides, were observed to vary in shape with carbon content. At low carbon percentages in the iron below about 2.20 C, the sulfides adopted a distribution of skeletal groupings as illustrated in Fig. 24a. The skeletal groupings often were accompanied by film type sulfides. At high carbon contents of about 2.60 C, the sulfides were found to be rather randomly distributed, smaller, and fairly uniform in size as shown in Fig. 24b. The change from small, randomly distributed sulfides to skeletal groupings occurred over a carbon range of about 2.40 to 2.20 pct carbon although the change is gradual.

One significant effect of melting furnace atmospheres on sulfide inclusions was observed. Carbon monoxide atmospheres promoted a larger, blocky type of sulfide although the skeletal groupings persisted in low carbon irons. Sulfides in irons held under N and CO at 2900 F may be compared in Figs. 24b and 25. This effect is also evident in Figs. 22a and 22b.

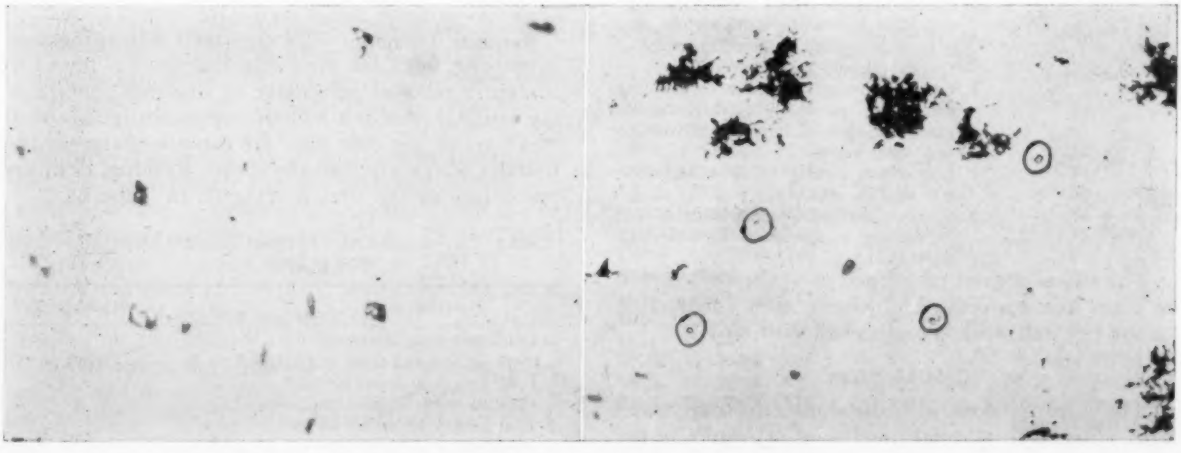


Fig. 25—Blocky sulfides, in white iron held at 2900 F under CO atmosphere for 1 hr, Heat S-3, 2.63 C. Compare with Fig. 24B. Mag.—500x.

Fig. 26—Titanium carbide inclusions in malleabilized iron from remelted cold melt iron held under a N atmosphere 1 hr, 2900 F, Heat CV-B, Unetched. Mag.—250x.

Conclusion *(Duplex Iron)*

The research up to this point has been aimed at determining the outstanding effects on malleable iron properties which result when pure gas atmospheres are introduced over the surface of molten duplex type white iron. The most striking effects obtained with each gas can be summarized as follows:

Gas Atmosphere Introduced	Effects
A. Carbon Monoxide	1. Causes mottling at the lowest level of carbon content for a given silicon percentage. 2. Promotes increased hot tear resistance in intermediate and high carbon irons. 3. Promotes increased fluidity at a holding temperature of 2700 F but not at 2900 F. 4. Results in increased resistance of carbides to first stage graphitization. 5. Modifies graphite nodule shape to a flaky or spider form and increases the nodule count.
B. Carbon Dioxide	1. Causes effects very much like that of carbon monoxide.
C. Nitrogen	1. The carbon level for mottling is increased above that characteristic of the carbon-oxide gases. 2. Promotes increased resistance to tearing in intermediate and high carbon irons at the 2900 F holding temperature. 3. Negates the effects of carbon-oxide gases in causing carbide stability during annealing. 4. Modifies the graphite nodule shape to a more closely contained particle and decreases the number of nodules.
D. Hydrogen	1. Markedly increases resistance to mottling. 2. Does not increase resistance to annealing under the test conditions of this research. 3. Causes erratic hot tearing properties with the deviation occurring in the direction of higher resistance to tearing. 4. Causes pinhole type surface defects.
E. Water Vapor	1. Markedly increases resistance to mottling. 2. Has effects on graphitization like those of CO. 3. Causes lowered resistance to tearing at the 2700 F holding temperature but not at the 2900 F temperature. 4. Causes pinhole type defects.
E. Air	1. Causes increased resistance to mottling compared to N and carbon-oxide gases, but not as much as hydrogen and water vapor. 2. Has effects similar to those of nitrogen during annealing. 3. Causes a marked lowering of resistance to tearing in intermediate and high carbon irons.

The effects of gases on properties of the duplex iron as listed above are those which are most outstanding in the research work accomplished thus far.

Cold Melt Iron

The research work with the duplex iron appeared to establish a substantial number of principles of the effects of furnace atmosphere on malleable iron properties. With these principles as a guide, it was decided to make a limited number of controlled atmosphere heats using the cold melt iron sprue as melting stock. These tests were intended to serve as a spot check on the properties of the cold melt iron and determine whether similar principles applied as in the case of the duplex iron. The results are discussed in the following sections.

Chemistry—Only N, CO, and air were employed as the gases introduced over the melt. These gases were used since they were found to be the ones most greatly affecting the properties of the duplex iron. Two carbon levels, low and high, and two holding temperatures, 2700 and 2900 F, were investigated. The data for chemical analyses, hot tearing and fluidity are presented in Table 14. Six heats were made in air atmospheres because of the difficulty of obtaining the desired carbon level. Some data obtained with 3-lb heats in an air atmosphere was also used and it was presented in Reference 3.

The non-oxidizing gases, CO and N, influenced the composition of the irons in exactly the same way as described earlier for the duplex iron.

However, the lower silicon content of the cold melt sprue made the iron more susceptible to exothermic oxidation by air. The same rate of air supply, 6050 ml per min, which caused a 2 point silicon loss and a 75 point carbon loss in the duplex iron over a 1 hr period at 2900 F caused a 60 point silicon loss and a 20 to 60 point carbon loss in the cold melt iron at the same temperature. It was necessary to change the manner of oxidation by air in order to maintain the silicon at about 1.10 pct and achieve the desired carbon content. This was done in heats CX-5 and CX-6 by exposing the molten iron at 2900 F to normal air atmosphere in an uncovered melting crucible. From this experience it is evident that the lower silicon cold melt iron is more sensitive to exothermic oxidation by free oxygen. Except for this fact, the chemical principles developed for the duplex iron apply to the cold melt iron. That a lower silicon content iron can have its silicon more easily oxidized by air has been experienced previously.[2] It should be recognized that the melting in these experiments was performed in a magnesia crucible. In a silica crucible the silicon loss would have been in part balanced by a pick-up from the refractory. Then the net silicon loss would have been less or there might even have been a gain.

Residual Titanium—The cold melt iron sprue used as melting stock for these experiments was found to contain a residual percentage of titanium. Source of the residual titanium was an imported pig iron used as a part of the cold melt air furnace charge in the foundry which supplied the sprue. Residual titanium percentage in the iron is reported in Tabe 24.

TABLE 24—RESIDUAL TITANIUM PERCENTAGE IN THE COLD MELT IRON

Material	Residual % Ti
1. Cold-melt iron sprue melting stock as received from suppliers—Lab B.	0.020
Cold melt iron sprue melting stock as received from suppliers—Lab A.	0.015
2. Iron held 1 hr under air atmosphere at 2900 F, heat CX-5.—Lab A.	0.020
3. Iron held 1 hr under CO atmosphere at 2900 F, heat CS-1 —Lab	0.020
4. Iron held 1 hr under N atmosphere at 2900 F, heat CV-1 —Lab A.	0.015

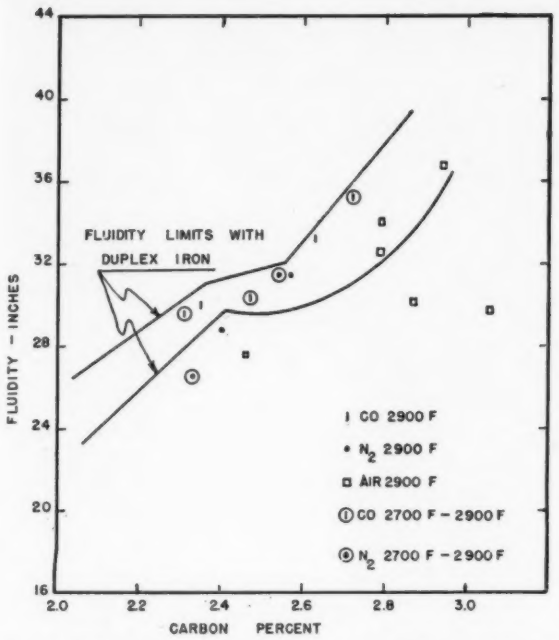


Fig. 27—Graph comparing fluidity of remelted cold melt iron with limits of fluidity for remelted duplex iron, Fig. 7. The molten iron was held for 1 hr at temperature under gas atmosphere indicated. Fluidity of irons poured at 2700 F was adjusted to 2900 F by adding 12 in. of spiral length.

Evidently a titanium residual may readily carry through remelting operations and be retained during a holding period even under as oxidizing an atmosphere as air.

The presence of the residual titanium percentage in the iron was first noted in a metallographic study of non-metallic inclusions in the iron. Titanium carbide and nitride particles such as those shown in Fig. 26 were observed in malleabilized castings made from this melting stock. Chemical analyses were then made with the results shown in Table 24. Similar metallographic studies on the remelted duplex iron discussed earlier revealed no titanium carbide or nitride inclusions. This difference in the two materials is an important one which can influence the experimental results especially in the case of annealability tests. This subject will be considered again later in the paper.

Mottling Tendency—As in the case of the duplex iron, a CO atmosphere over the molten cold melt iron caused mottling to occur at a minimum carbon content. Scattered mottling occurred at 2.72 pct C and 1.10 pct Si under a CO atmosphere at 2700 F, while the iron was white at 2.63 pct C under the same gas at 2900 F. Air caused the mottling tendency to vary depending on the severity of the oxidizing conditions. With the severe oxidizing conditions when the metal was exposed to air in an uncovered crucible at a temperature of 2900 F, the iron was white at 2.79 pct C and 1.09 pct Si.

In view of the limited number of heats, irrefutable conclusions are difficult, but it was found that CO atmospheres could cause mottling at the minimum

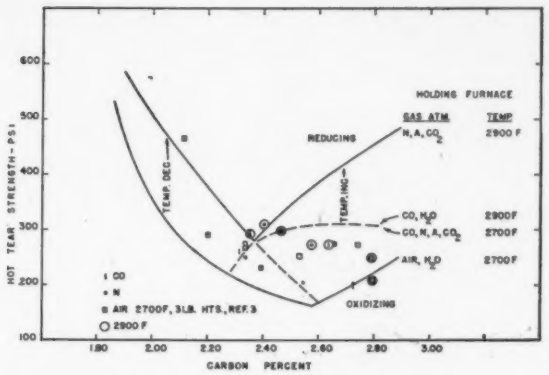


Fig. 28—Graph comparing hot tear strength of remelted cold melt iron with hot tear strength of duplex iron remelt, Fig. 14.

carbon content as in the case of the duplex iron.

Fluidity—Fluidity, in inches spiral length, of the cold melt iron when pouring from 2700 F and 2900 F after holding 1 hr at temperature under CO, N, and air atmospheres is plotted in Fig. 27. The curves defining the fluidity limits of the duplex iron at a common temperature of 2900 pct, which were presented in Fig. 7, are reproduced in Fig. 27 for comparison with the cold melt iron. Fluidity in general is in the same range for both irons as revealed in Fig. 27. However, at lower carbon content there is a definite tendency for fluidity to be lower in the cold melt iron than in the duplex iron. This may be due to the lower silicon content of the cold melt iron, 0.20 pct Si lower, and a possible shift of the jog in the fluidity curve to a higher carbon content. Two points of data shown at high carbon content in Fig. 27 are low because of a large loss in silicon due to air oxidation. There is a trend for the fluidity of air oxidized iron to be low at a given carbon percentage. The influence of CO on raising fluidity of the duplex iron at 2700 F was less pronounced in the cold melt iron. No

TABLE 25—NODULE COUNT IN ANNEALED CASTINGS OF COLD MELT IRON

Heat No.	Carbon, %	Nodule Count in Hot Tear Casting, No. per mm²	Nodule Count in Fluidity Well, No. per mm²
CK-1, CO 2700 F	2.47	70	44
CK-2, CO 2700 F	2.72	107	43
CK-3, CO 2700 F	2.31	67	36
CS-1, CO 2900 F	2.35	115	32
CS-2, CO 2900 F	2.63	122	32
CP-1, N 2700 F	2.33	34	28
CP-2, N 2700 F	2.54	57	35
CV-1, N 2900 F	2.40	71	67
CV-2, N 2900 F	2.57	56	72
H-4, Air 2700 F	2.33	83	—
H-1, Air 2700 F	2.53	80	—
H-7, Air 2700 F	2.74	68	—
CX-1, Air 2900 F	2.79	95	Primary Flakes
CX-2, Air 2900 F	2.96	76	Primary Flakes
CX-3, Air 2900 F	3.06	31	6—Incomplete Anneal, about 0.40% Si
CX-4, Air 2900 F	2.87	24	6—Incomplete Anneal, about 0.40% Si
CX-5, Air 2900 F	2.46	74	35
CX-6, Air 2900 F	2.79	117	38

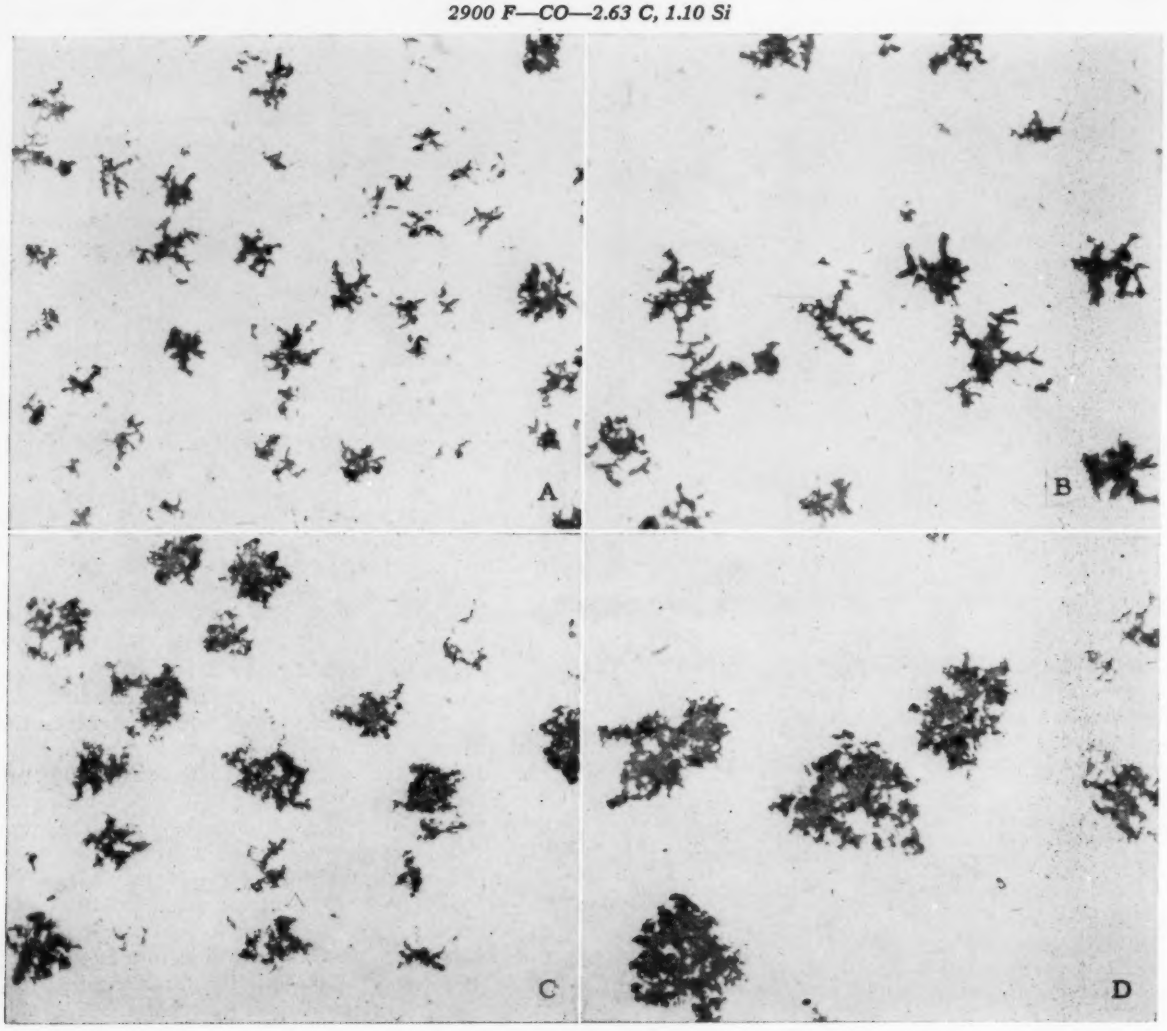


Fig. 29A—Graphite distribution in hot tear casting sample of remelted cold melt iron held 1 hr under CO melting furnace atmosphere, Heat CS-1. Unetched. Mag.—150x.

Fig. 29B—Same as Fig. 29A except magnification is 250x.

Fig. 29C—Graphite distribution in metal held 1 hr under air atmosphere, Heat CX-5. Unetched. Mag.—150x.

Fig. 29D—Same as Fig. 29C except magnification is 250x.

marked differences were found to exist between the duplex and cold melt iron in regard to fluidity and the effect of gases on fluidity.

Hot Tearing—Because of the limited number of heats it was not possible to construct curves showing the influence of furnace gases on the relationship of resistance to hot tearing and carbon content of the cold melt iron. However, all the data obtained with the cold melt iron were plotted in Fig. 28 for comparison with the generalized hot tearing property graph developed for the duplex iron, Fig. 14. The points for the most part fall within the limits developed for the duplex iron. From the graph, Fig. 28, it appears that the low carbon mold melt iron behaves much like the duplex iron. In the high carbon range, the cold melt iron has lower tear resistance than the duplex iron at a holding temperature of 2700 F, but behaves like the duplex iron at the 2900 F temperature. It was not possible to make any further conclusion with the amount of data available, but in general the principles involved in the two irons are similar.

Annealability—Annealability tests were conducted on the remelted cold melt iron by the same procedure used on the duplex iron. Samples for annealing were taken from the hot tear casting as indicated in Fig. 17. Fluidity well castings were also annealed. The annealing cycle was slightly modified by extending the time allowed for first stage graphitization at 1700 F from 30 hr for the duplex iron to 45 hr for the cold melt iron. This was done to compensate for the lower silicon content of the latter iron. The same second stage graphitization period was followed for both irons.

Stability of Carbides—The microstructure of the fully annealed samples was examined for the presence of carbides. No carbides of the type reported in the duplex iron were noted in the cold melt iron. Samples

from the cold melt irons held under CO atmosphere were examined at the mid-point of the first stage graphitizing period. Massive carbides were completely absent at that point. The absence of stable carbides in metal held under CO atmosphere was a distinctly different feature of the cold melt iron as compared with the behavior reported for the duplex iron. It is here that the residual titanium content of the cold melt iron becomes effective. The effects of the addition of small percentages of titanium to the iron in increasing the rate of elimination of carbides during first stage graphitization has been reported a number of times. The residual titanium apparently is effective in preventing the CO atmosphere from developing the stable carbides in the cold melt iron which were caused by CO in the duplex iron.

Graphite Characteristics—Nodule counts were made on the fully annealed cold melt iron samples. The nodule count data for all heats made under atmospheres at holding temperatures of 2700 F and 2900 F are listed in Table 25. The relative effects of the gas atmospheres on nodule count in the cold melt iron appeared to be similar to the effects obtained with the duplex iron. Carbon monoxide caused a high nodule count as compared with N and air, especially at the 2900 F holding temperature. The differences in nodule count caused by CO as compared with N and air were less pronounced in the heavier section fluidity well casting than in the hot tear casting. Even samples with the lowest nodule count were fully graphitized except in the cases of heats CX-3 and CX-4, where a very low silicon percentage resulted in incomplete graphitization.

Carbon monoxide atmospheres were again observed to cause a flake type of graphite. The graphite in cold melt iron held 1 hr under a CO atmosphere at 2900 F is compared with that from an air atmosphere in Fig. 29. The graphite in the cold melt iron held under the CO atmosphere contains flakes and loose groupings of flakes while that held under air appears more compact. This is the same effect reported for the duplex iron.

A comparison of nodule counts shows that the cold melt iron is consistently lower in nodule count than the duplex iron. Such a comparison is made in Table 26. The annealed cold melt iron contains only about 30 to 75 pct as many nodules as the duplex iron.

TABLE 26—COMPARISON OF COLD MELT AND DUPLEX IRON NODULE COUNTS IN 1/2-IN. DIAM SECTION

Melting Conditions Atm.	Temp. F	% C	Nodules Per mm² Cold Melt	Nodules Per mm² Duplex	Ratio: CM Duplex %
CO	2700	2.31	67	198	34
CO	2700	2.47	70	210	33
CO	2700	2.72	107	224	48
CO	2900	2.35	115	192	60
CO	2900	2.63	122	244	50
N_2	2700	2.33	34	114	30
N_2	2700	2.54	57	108	53
N_2	2900	2.40	71	99	72
N_2	2900	2.57	56	103	54
Air	2700	2.33	83	108	77
Air	2700	2.53	80	108	74
Air	2700	2.74	68	113	60
Air	2900	2.46	74	119	62
Air	2900	2.79	117	118	99

From the limited data available it appears that the annealability of the cold melt iron is influenced by furnace atmospheres in very much the same way as the duplex iron. In the cases studied, the presence of the residual titanium is a complicating factor.

Acknowledgments

The authors wish to express their indebtedness to many persons for their assistance in carrying out the work reported in this paper. Among those offering their help were the following:

1. Members of the A.F.S. Malleable Iron Research Committee: C. F. Joseph, *Chairman;* W. D. McMillan, *Vice-Chairman;* H. Bornstein; W. A. Kennedy; R. Schneidewind; Milton Tilley; R. P. Schauss; J. H. Lansing; P. F. Ulmer; S. C. Massari, A.F.S. Technical Director. White iron sprue for melting stock was supplied by the group through W. D. McMillan, International Harvester Company, and the Pratt Manufacturing Company.

2. University of Wisconsin administrative leaders: Professor K. F. Wendt, Associate Director, the Engineering Experiment Station; and Professor E. R. Shorey, Chairman of the Department of Mining and Metallurgy.

3. Colleagues of the Department of Mining and Metallurgy: Professor G. J. Barker; Professor P. C. Rosenthal; Research Engineer L. F. Porter; and Mechanician Ray C. Wiggen, who constructed the hot tear testing apparatus.

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DISCUSSION

Chairman: C. O. SCHOPP, Link-Belt Co., Indianapolis, Ind.

Co-Chairman: N. N. ARMHEIN, Federal Malleable Co., West Allis, Wis.

H. A. SCHWARTZ *(Written Discussion):*[1] The authors have reported a considerable amount of information covering various factors within their field of investigation. There seems to be little reason to question the accuracy of their observational data.

This commentator is in some doubt as to whether all of the interpretations of the data are justified.

One of the questionable features in the mind of this commentator is the use of curves possessing inflections in interpret-

[1] Manager of Research, National Malleable & Steel Castings Co., Cleveland.

ing the results. For example in Fig. 7, unless some statistical approach such as the Chi Square Test indicates that the discontinuous lines of the figure are actually demonstrably superior to a straight line through the array of points, it seems doubtful whether such complex trend lines should have been attempted. This is especially true since there is no apparent reason why the effect of carbon on fluidity should be any different near 2.4 or 2.5 per cent than above or below that range.

In further reference to Fig. 7 it appears, if this commentator has read the matter correctly, that the actual observations at 2900 F are shown in Fig. 4; being there represented by straight lines, which in most cases seem a perfectly adequate representation of slightly scattering data.

In the case of air and hydrogen, if there is a flat area it is at a carbon content above 2.60 per cent. The writer would, however, interpret all of these figures, as did the authors, as being properly represented by straight lines.

Table 16 seems to have been obtained on this assumption and has now been used to convert lines in Fig. 6, having no jog at 2700 F, for comparison at 2900 F. By such a conversion no jog could be introduced. After plotting Fig. 7 the authors become convinced that there is a jog, which would appear to vitiate the translation from 2700 F to 2900 F, which was made on the assumption that there was no jog. The assumption that there is a jog at 2900 F, against the evidence of Fig. 4, is, to this commentator, surprising.

Figure 10 gives curves showing minima, apparently due to the fact that an attempt has been made to represent two universes—mottled iron and unmottled iron—in a single figure.

Other similar cases can be cited.

This commentator is also doubtful whether hot tear strength is a measure of the castings to be subjected to this fault. These castings probably fail by plastic deformation. There is not a simple relation of stress and strain under these conditions. The stress imposed by a given strain depends on the rate of application of that strain.

With the test specimens used by the authors it would be decidedly difficult to form an opinion as to the rate of strain to which the hot portion of the casting is subjected.

The treatment of mottling tendency seems to this commentator quite inadequate. The effect of silicon has been largely dismissed in this discussion although the Schneidewind-McElwee publication on this point has been referred to.

The number of specimens studied seems completely inadequate to the drawing of any conclusion, a matter which is elucidated in the paper on "Graphitization in the Malleable Iron Industry" by the commentator and some of his associates, in this volume.

Be it emphasized in conclusion that these comments are not written to minimize the value of the present contribution but rather to serve as a caution lest the data be over-interpreted.

R. W. HEINE *(Authors' Reply to Dr. Schwartz):* The discussion of Dr. Schwartz is gratefully received. The appearance of a discontinuous fluidity curve, Fig. 7 in the paper, is questioned by Dr. Schwartz. It may be that this discontinuity does not in fact exist, however, the data which we must interpret indicates that it is there. Since this is a continuing research project and additional data will be forthcoming, we will have ample opportunity to check this point. Further data will be presented in future papers.

The question of a minimum in Fig. 10 is well taken. It is true that mottled and unmottled iron are represented on the same graph. This is a perfectly legitimate means of representing differences in properties and is commonly done to emphasize abrupt changes in properties. In a cooling curve, temperature versus time is plotted for both the molten and solid states. A discontinuity represents a change in state. The hot tear test appears to be a sensitive one in detecting mottling. The hot tear test bar does not fail by plastic deformation. It is a brittle hot tear failure. Furthermore, the test specimen employed in the test and the testing procedure makes it very simple to determine the rate of strain to which the hot portion of the casting is subjected, contrary to Dr. Schwartz's statement. This point is made very definitely in an article in AFS TRANSACTIONS, vol. 60, entitled, "A Test for Hot Tearing Tendency," pp. 182-196 (1952).

The effect of silicon content on mottling is limited in this work to the silicon range of the irons supplied by the AFS Malleable Research Committee and the scope of the investigation. At this silicon content, it has been definitely proven in this investigation that the melting furnace atmosphere has a more profound effect on mottling tendency than any substantial variation in silicon content. The number of specimens is quite adequate for drawing this conclusion. Furthermore, the results are reproducible and can be duplicated at will.

RISERING OF GRAY IRON CASTINGS

Progress Report No. 4

By

W. A. Schmidt* and H. F. Taylor**

This is a report of the work being done at the Massachusetts Institute of Technology under sponsorship of the American Foundrymen's Society; the work is being done under the direction of the Gray Iron Research Committee of the Society. The purpose of the project is to develop information which can be used by practical foundrymen in their problems of risering gray iron. Such knowledge, based upon fundamental truths determined by experiment, should enable gray iron foundrymen to understand their overall problems in clearer detail, and should completely answer many routine questions. The important thing for the practical man to have in mind constantly in appraising this project is that the research problem is tedious and time consuming, as gray iron is a complex material and understanding of the mechanism of shrinkage and solidification is particularly cloudy. Thus before a simple interpretation can be developed, so understanding and use in practice will follow some fundamental data are needed. Eventually it is planned the mechanism of solidification of gray iron as determined by this research, will be reduced to a motion picture film.

This is a progress report of the work done to date, an analysis of the results, and a description of plans for future work. It does not pretend to answer any problems at this time. However, a general trend of thought is recognizable, and is part of the foundation upon which the final analysis will be based.

Actual experimental work on this project, "Risering of Gray Iron Castings," was initiated November 1950; the following describes work done from that date. A bibliography[1] was prepared under the original contract, and plans for research developed between the dates January 1 and November 1, 1950. As an aid in keeping research effort allied as closely as possible with practical experience and requirement, it was decided that foundries operated by members of the Committee would make a series of life size castings for careful study at M.I.T.

The Gating and Risering Committee furnished castings in the form of slabs (½ in., 1 in., 2 in., 3 in., and 4 in. thick), and X, L, and T sections (1 and 3 in. thick). These were cast in two grades of iron, 35M and 50M, risered and unrisered. Two and one-half complete sets of these castings were received for examination and testing.

All castings were sectioned to observe internal defects and structure. All risered castings were heavily risered and were essentially sound. Usually casting sections were bulged slightly in the lower regions. In the unrisered castings various shrinkage patterns were present. Careful examination showed that shrinkage defects fell into three general categories. These were described in a previous report,[2] and were (1) localized piping or diffuse internal shrink cavities, (2) draws at internal angles or on flat, upper surfaces, or (3) no localized shrinkage. A study of the properties of the different molding sands did not indicate that the sand could be the lone variable causing the different shrinkage habits. It was therefore thought that some of these variations could be attributed to solidification behavior.

Many of the sections were etched, heat tinted, or traced with sulfur prints to permit a study of the macrostructure. Sulfur printing was found suitable only for the heavier sections. From these prints of the slab type castings, it was determined that the castings showing no localized shrinkage had a thinner zone of columnar dendrites than those which piped. These observations again indicated that solidification behavior might have some influence on shrinkage habits. At any rate, it was obvious the problem could not be attacked in a systematic research manner by making shop type castings and cutting them up; a much more fundamental approach was indicated. It is felt our present approach, after three year's effort, is sound in every respect. The number of variables still haunting the program, however, is still rather awesome.

I. Current Practices and Theories

Composition is an important variable in any consideration of gray iron behavior. There was a time

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This is a Progress Report on a Research Project sponsored by the Research Committee of the Gray Iron Division of A.F.S. Members of this Committee are J. S. Vanick, *Chairman*, H. N. Bogart, J. W. Bolton, Alfred Boyles and T. E. Eagan.

when composition varied only because of melting practice and the raw materials available. Today, because of the need for higher strength irons and for irons possessing superior corrosion or heat resistance, alloying has become standard practice. The effects of such alloying on risering requirements is not specific. If it changes or modifies graphite precipitation, or influences some other solidification phenomena, risering requires modifications.

Common Elements in Gray Iron

The most common elements in gray iron are carbon, silicon, manganese, sulfur, and phosphorus. The alloying elements are nickel, chromium, molybdenum, and several others. The general effects of these various elements are discussed below as they bear on solidification phenomena:

1. Carbon in iron precipitates as graphite as the iron freezes. However, elements called graphitizers (i.e., silicon, nickel, titanium, etc.) must be present to promote this action; otherwise the metal will freeze as a white iron. Other elements called carbide stabilizers (chromium, sulfur, manganese, etc.) tend to retard graphite formation by stabilizing the iron carbide; that is, by rendering the iron carbide more difficult to decompose into carbon and ferrite. The degree of carbide stabilizing ranges from pearlite formation through massive carbides to an entirely white structure. The amount of graphitizers and stabilizers are carefully balanced in practice to give desirable characteristics of strength, machinability, etc.

Since the density of graphite (2.22) is so much less than that of the base metal (7.25), it is possible to counteract all solidification shrinkage by the formation of a sufficient amount of graphite. Honda, *et al*[3] have shown that 1.75 per cent graphite formation during solidification in a eutectic iron is sufficient to equal the solidification shrinkage. A compilation of data from shrinkage experiments by others confirms this figure closely.[1] Many of today's commercial irons do not contain this amount of free graphite at the eutectic temperature and must therefore shrink on solidification in one form or another.

2. Silicon is used as a graphitizer in most commercial irons; however, in very high silicon irons the addition of silicon promotes corrosion resistance. Interest in typical gray iron is chiefly centered around its graphitizing effect. Besides aiding indirectly in the formation of flake graphite, silicon actually reduces the solubility of carbon in austenite. This permits a greater amount of the carbon in the alloy to separate on solidification. The solubility of carbon in austenite at the eutectic is roughly 2%—.1 $\times$ % Si.

3. Manganese is a carbide stabilizer. Its main function is to control the amount of pearlite in iron. High strength irons thus require more manganese than those of lower strength. Probably more important is the ability of manganese to combine with sulfur as a manganese sulfide, and so render the sulfur less harmful to mechanical properties; it also prevents the sulfur from stabilizing the carbides. For manganese to act as a stabilizer, it must therefore be present in amounts above that required to combine with the sulfur present. The amount needed to combine with the sulfur is usually 1.7 times the sulfur present plus .30 per cent or more.

4. Sulfur is an element common to all irons. Some irons require that special means be employed to reduce sulfur to a low level. As indicated above, when sulfur is present it is necessary to tie it up with manganese to prevent its carbide stabilizing effect on the metal.

5. Phosphorus forms a low melting compound (eutectic) called steadite; this is the last bit of molten constituent to freeze in an iron. When present in sufficient quantities, it is recognizable as a network pattern in the iron. Usually this eutectic does not create shrinkage problems because it expands on solidification. Recently, Hamaker, et al[4] have shown that phosphorus in conjunction with some of the more common alloying elements used in high strength irons will cause interdendritic shrinkage. This is because the phosphide eutectic has a high volumetric shrinkage in their presence. Since this type of shrinkage is impossible to feed directly, they recommend certain levels of phosphorus that reduce the effect.

Gases in Gray Iron Practice

Gases are of considerable interest in gray iron practices. This is readily appreciated since the metal is melted without a continuous slag covering and in contact with large volumes of gases, and is melted at a much higher superheat (600 to 800 F) than most other metals.

Oxygen and hydrogen are recognized as carbide stabilizers, and as such have an effect on the structure of the iron. Many investigations have been made on the effect of gases on the expansion of gray and white irons. These show that hydrogen, nitrogen, and carbon monoxide are largely responsible for this expansion. Reports have been made that superheating increases the expansion and remelting, or melting in a vacuum, reduces it. Normally it is the practice to avoid water in the form of wet ladles, this being a source of hydrogen which seems to cause more unsoundness than the other gases.

Molding is confined largely to green sand. It is generally agreed that dry or core sand practices produce better castings with regard to shrinkage characteristics, surface conditions, reproduction of the pattern, and just generally give more dependable results; however, cost prohibits their use unless special requirements of casting shape or dimensional tolerance require their use.

In green sand systems, either natural or synthetic sands are used. Their control is awkward and requires close attention. Abuse of the sand is recognized as a source of casting defects, with the result that many plants consider sand control an essential part of their operations, and one that pays for itself in the long run.

Efforts are continually being made to improve the properties of the sand to meet special requirements. Probably the most successful has been the use of seacoal as an additive. The purpose of the seacoal is to create a reducing atmosphere in the mold. This

results in better casting surface and less burned-on sand by removing the oxygen. Oil added to the sand produces similar results. Recently work published by Sanders and Sigerfoos[5] showed that the addition of seacoal and wood flour, within limits, reduces the piping tendency of iron. They also reported that southern bentonite is better than western bentonite or fireclay in reducing piping. Bohm[6] has also reported of the advantages of seacoal and southern bentonite.

Allowance for shrinkage known as "pattern makers shrinkage" is variable between about 1/4 to 1/8 in. per ft. Section size is an important factor in calculating this shrinkage. Larger sections need less allowance than thin sections. Sand conditions may cause this variation, or metallurgical factors such as graphite precipitation or gas evolution may also be significant.

Function of Riser

Risering as it is practiced today presents many conflicting and confusing aspects. Often on the same, or closely similar jobs, good castings result when small, seemingly inadequate, risers are used, and again only when large, heavy risers are employed; sometimes good castings result only when no risers are used at all. Methods have been developed through individual observation and experience without an understanding of basic principles of solidification. Several factors governing the application of these risers are evident; it is the problem of this particular research to determine why and how these variables influence solidification behavior, and to predict their effects in a way to be of use to the pratical men in the shop. Risering today does one of two things:

1. It removes the thermal center or hot spot, where most interdendritic shrinkage occurs, from the casting. In this instance, the riser is not used as a source of feed metal primarily, but as a source of heat. It is a mechanism to control the solidification process, just as chills are used to control solidification in other metals. The disadvantage of this type control is that the riser must be slightly larger than the casting section, with attendant low yield of good castings per ton of metal poured.

2. It acts as a source of feed metal similar to risers in other metals. The difference in gray iron is that the riser needs to feed only liquid shrinkage, and in some instances, a little solidification shrinkage. This permits the use of the small risers, commonly called shrink bobs.

Since the type riser used is dependent on the solidification behavior of the iron, it is impossible to broadly predict risering success at the present time. Generalizations of risering theory at present closely follow those of steel. If this system is adhered to in gray iron practice, many castings would be overrisered with the possibility of doing more harm than good.

The gating of iron is far removed from the standard practices used for other metals in several important details. For hypereutectic irons, it has been developed through the years that gating through thin sections into heavier sections reduces shrinkage trouble. This means in effect that preheating the mold near thin sections, and thus equalizing the temperature distribution through the metal and mold, does one, or possibly two, things.

1. Shrinkage is uniformly distributed and not apparent.

2. Enough superheat is lost to reduce liquid shrinkage after the mold is full.

Another deviation from standard procedure is to utilize the gate and runner system as a riser mechanism. This is practical only when small amounts of feed metal are required and the gates can be maintained at a reasonable size.

A fundamental of casting design in iron is to avoid sharp changes in size of section, and to keep sections as nearly uniform as possible. This is done primarily to reduce weak internal angles, and to keep cooling rates as uniform as possible. These rules are designed primarily to cover those instances when shrinkage trouble does occur, since many successful castings are made which abrogate them entirely. It is good insurance to adhere to these rules whenever possible to avoid trouble.

II. Experimental Procedures

A. *Measuring Shrinkage by Use of Cast Spheres* — Many investigations have been made, and varied techniques used, to determine shrinkage occurring in irons. The index of shrinkage has usually been a density measurement. However, seemingly identical castings made in accordance with these findings may shrink badly one day and be sound another—or worse yet, some castings from a single ladle may be solid and others badly shrunken. Somewhere in the process of taking results from the laboratory and applying them to a specific casting, conditions vary enough to cause this erratic behavior. The cause might be gases due to melting procedure or to the melting stock; the effect of mass, and solidification rates, may be involved. Composition variables, particularly trace elements not commonly analyzed, might throw conditions out of balance; so the problem of rationalizing cause and effect is difficult.

To be able to study the effects of any factor affecting shrinkage behavior, a method for measuring the shrinkage accurately is necessary. Initially small cylinders of iron were cast and the sinks filled with wax, water, or lead. None of these methods were actually successful, as a complete materials balance of metal and void volume was never obtained. The one possible source of error seemed to be from the gating system. Elimination of the gate was indicated.

Subsequent castings were made in the form of 4-in. diam spheres. The gates were short and direct, and were stoppered with form fitting plugs as soon as the casting was poured. This practice eliminated the gate almost as soon as pouring was complete. Once the mold was poured and plugged, it was rotated on an axis through the center of the sphere. The reason for rotating the castings during solidification was to isolate all the shrinkage within the casting for easier measurement. These castings were made in both green and dry sand molds. Figure 23 is a picture of the apparatus used.

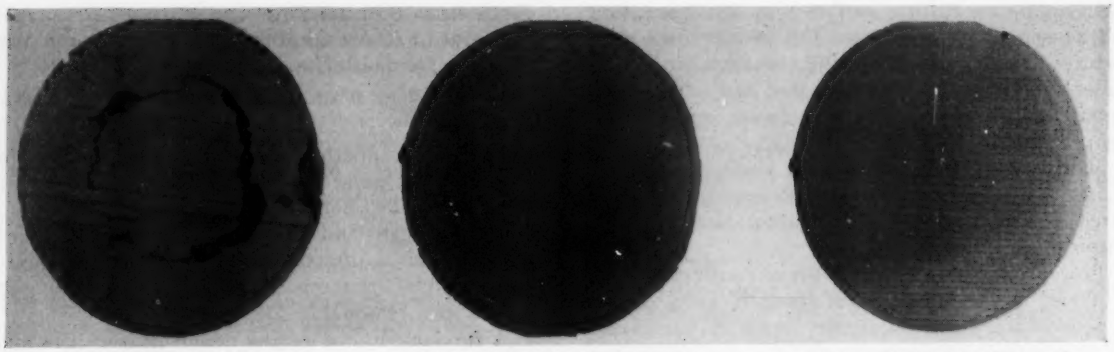


Fig. 1—Rotated in green sand mold.

Fig. 2—Rotated in dry sand mold.

Fig. 3—Untreated dry sand casting.

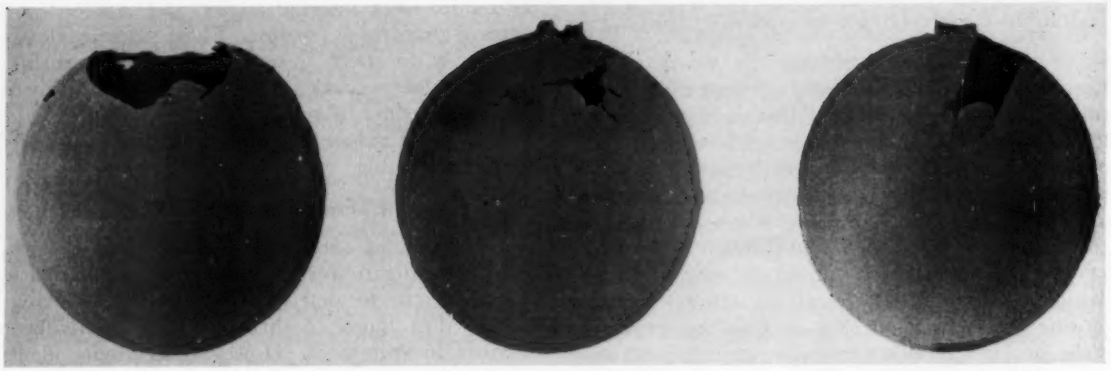


Fig. 4—Untreated green sand casting.

Fig. 5—Dry sand casting treated with H_2.

Fig. 6—Dry sand casting treated with H_2.

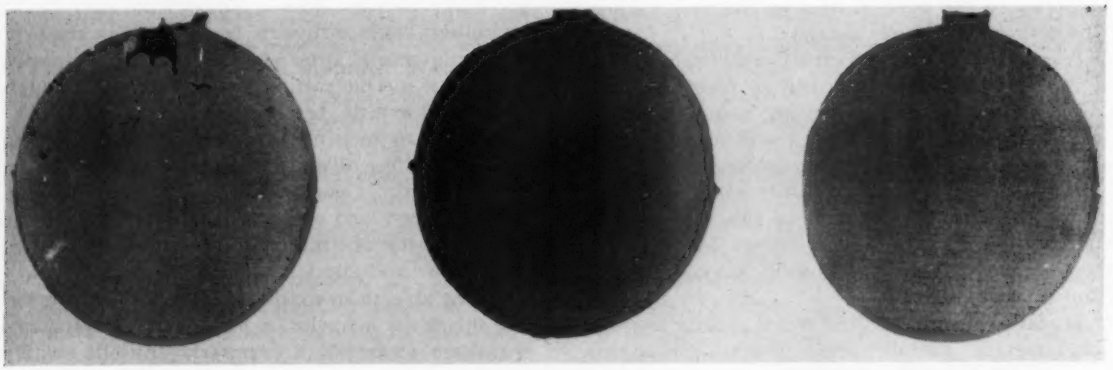


Fig. 7—Dry sand casting treated with O_2.

Fig. 8—Dry sand casting treated with N_2.

Fig. 9—Dry sand casting treated with CO_2.

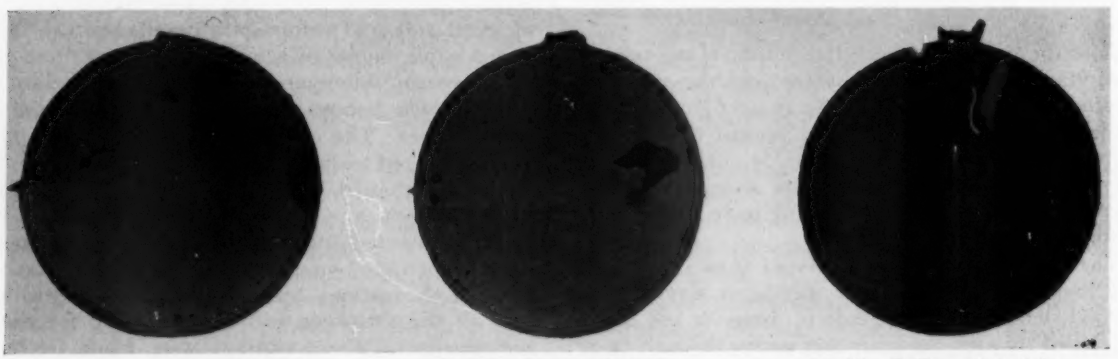
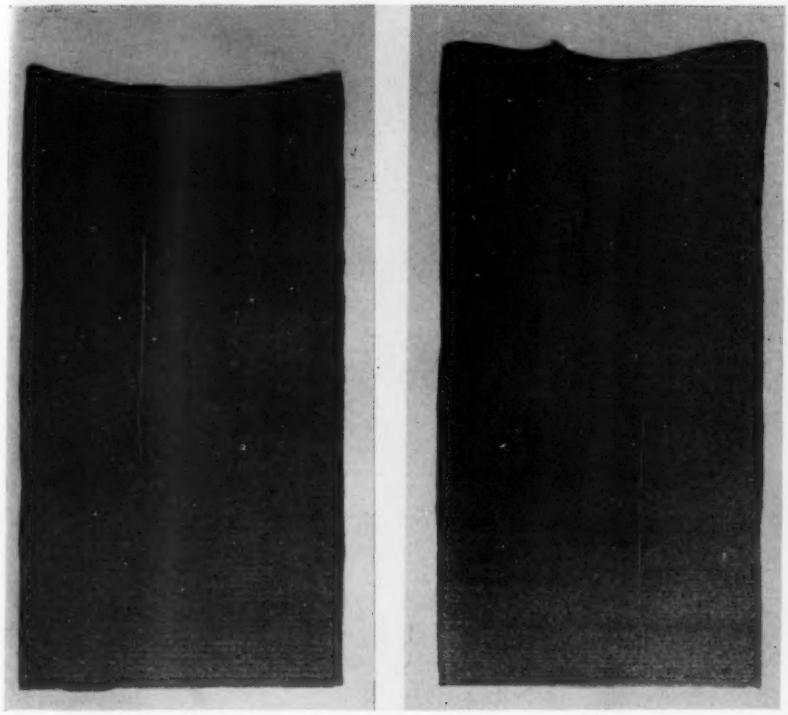


Fig. 10—Green sand casting treated with H_2O.

Fig. 11—Dry sand casting treated with H_2O.

Fig. 12—Sulfur print of dry sand casting.



A B

Fig. 13—Green sand castings (a) top covered with rice hulls (b) top open.



Fig. 14 (Right)—Green sand casting with insulated riser.

Rotation produced a completely isolated central void in metals such as aluminum and copper. In gray iron the void was distributed between an outer wall of metal ½ to ¾ in. thick and a central core. This distribution occurred only with green sand molds. The dry sand castings contained no internal voids. Another feature of the iron castings with shrink cavities was a passage (sometimes tiny and sometimes large) to the surface through the outer wall. These penetrated the wall near the parting line. External measurements of the spheres indicated that the green sand castings were larger in diameter than the dry sand.

It was concluded that the green sand mold cavity enlarged after the casting was poured. This accounted for the larger casting size and the large shrink cavity. Shrinkage measurements under such circumstances would not indicate true values. Figures 1 and 2 illustrate typical dry and green sand castings, rotated during solidification.

B. *Gases in Gray Iron*—The effect of gases on the shrinkage habits of iron was also investigated using spherical castings. It is known that hydrogen and oxygen are carbide stabilizers and that sufficient quantities might cause gasiness. These tests were expected to indicate whether shrinkage could be aggravated by carbide stabilization or alleviated by gasiness.

The metal was treated by bubbling various gases through it before pouring. These gases included hydrogen, oxygen, nitrogen, carbon dioxide, and water vapor. The metal was then cast in both green and dry sand molds, as described above. It was observed that hydrogen and oxygen caused shrinkage in castings made in dry sand molds which are usually solid. Water vapor alone created considerable gasiness. The other gases had no effect (Figs. 3 through 11).

Of particular interest are the castings in Figs. 6 and 7. The pencil-shaped cavity formations which are partially refilled with exuded metal are quite different in shape from the usual "V" type cavity. The indications are that this type cavity is formed after a partially solidified wall of metal has frozen. A section of the wall is displaced and sucked into the interior. Normal reaction would be that such a movement of metal would have to be quite sudden to keep the sides of the cavity as parallel as they are. This might very well be the case. It is hard to conceive of any solidification reaction which would occur at such a high rate. Another possible method of formation, equally open to conjecture, is that the movement starts and proceeds to completion during an interval of comparative thermal arrest. Such a favorable period would be during the eutectic arrest. Dunphy and Pellini[7] have reported extensive work on the solidification of gray iron with a comprehensive coverage of the thermal conditions involved. If this type cavity is formed in the described manner, the theory can be evolved that the gases hydrogen and oxygen (and possibly water vapor) stabilize the carbide during the earlier portion of the eutectic hold. This early retardation of graphite formation causes

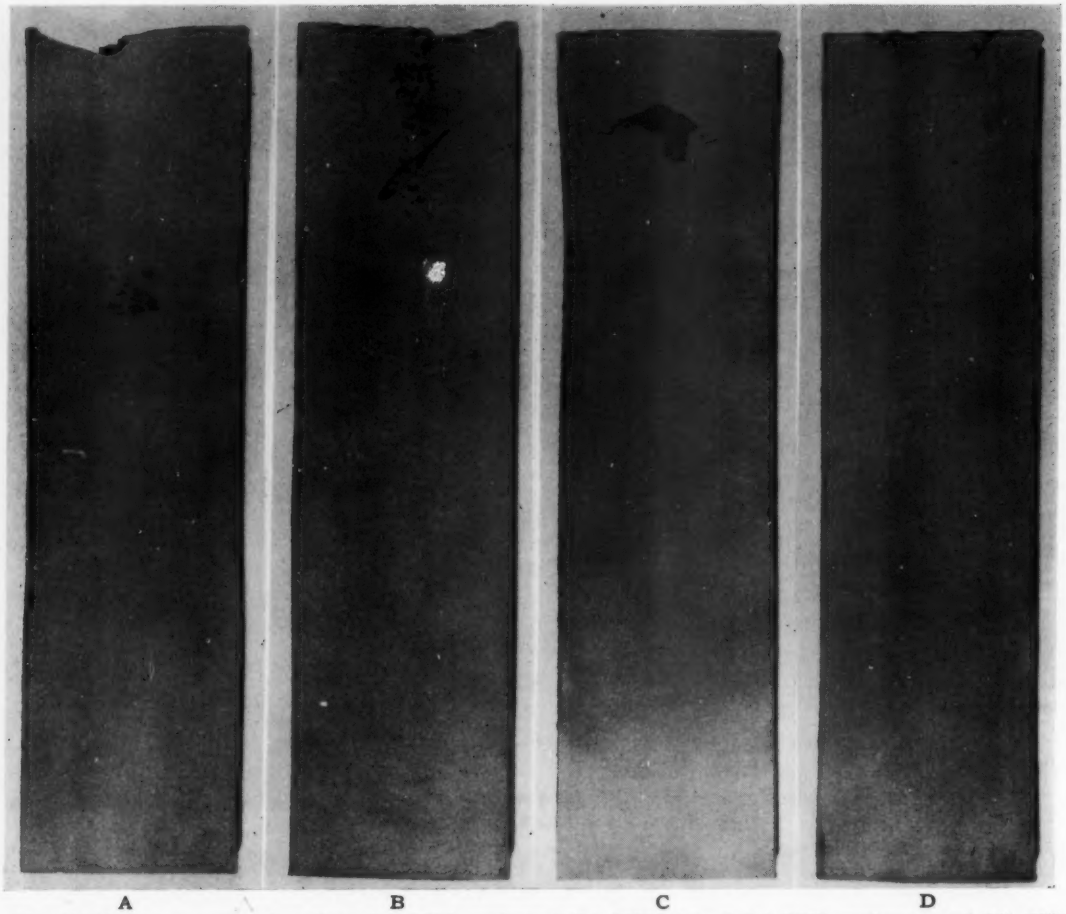


Fig. 15—12x10x3-in. commerial castings (a) 35M, unrisered (b) 35M, risered (c) 35M, unrisered and (d) 50M, risered.

a normally shrink-free iron to suddenly shrink. Later, as evidenced by the exuded metal, the graphite does form. The cavity can be partially refilled with eutectic metal, or the graphite formation may swell the whole section leaving the shrink cavity intact. In this manner, microscopic examination of the metal structure would not necessarily be indicative of how, or exactly in what sequence, the graphite formed. Figure 12 is a sulfur print of a dry sand casting with a similar type cavity which has been complete refilled with exuded metal. This particular casting received no unusual treatment.

C. *Study of Mold and Riser Behavior—Measurement of Dilation During Solidification*—An experimental procedure was developed to assess the significance of mold dilation (expansion) to risering requirements. Dilation was measured by connecting an Ames dial to a thin steel plate molded at the mold-metal interface. A cylindrical casting 2 in. in diameter and 5 in. high was used for these experiments. Castings were made in both green and dry sand molds. Figures 21 and 25 show the apparatus in detail and as it was set up. Typical of the results are the curves shown in Fig. 16. It can be seen that there is no appreciable dilation in dry sand molds. Green sand shows dilation almost from the instant of pouring through the eutectic hold. The shapes of the two green sand curves indicate that dilation may proceed at different rates but still reach the same end point. The reason for this may be connected with pouring temperature and/or sand characteristics, but is not known at present. The effects of such characteristics are obviously important in risering efficiency.

Curve No. 1 shows rapid dilation while the metal is all liquid, and a hold during austenite formation. Small shrink bobs should be able to feed castings with this type behavior efficiently. Curve No. 2 indicates slower but continuous dilation during austenite formation. Under such conditions, an ordinary shrink bob may freeze before the eutectic is reached. When this happens, pinholing or draws may form near its base. Both curves show continued dilation when the eutectic is reached and graphite subsequently formed. Curve No. 3 from dry sand indicates that the mold is strong enough to resist both initial dilation and that caused by graphite precipitation.

Dunphy and Pellini[8] in their paper reported dilation occurring only when the solidifying system was enclosed in solidified walls of iron. Then the dilation was only due to graphite precipitation. Their reasoning is sound, but since their apparatus was designed to measure metal movement only their find-

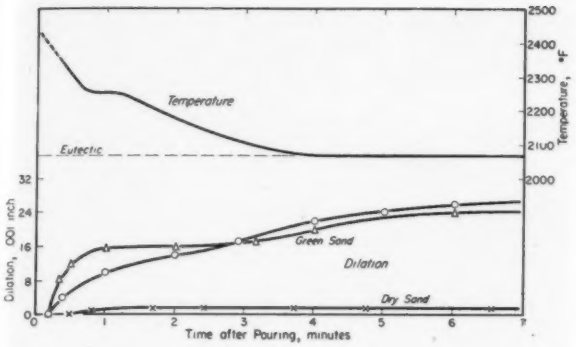


Fig. 16—Dilation of green and dry sand castings.

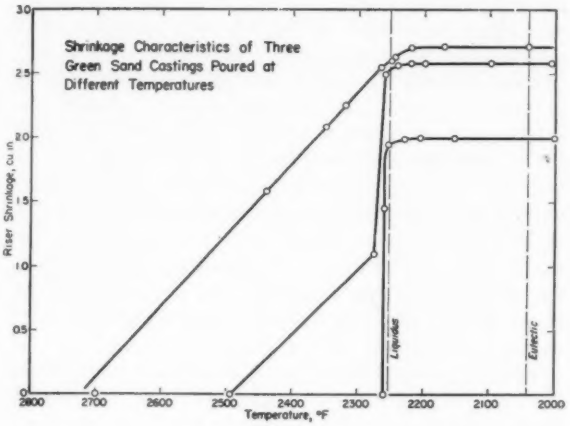


Fig. 17—Shrinkage characteristics of three green sand castings poured at different temperatures.

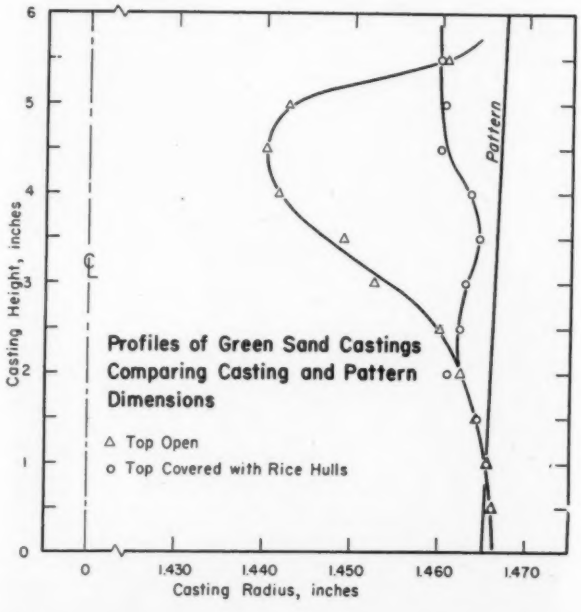


Fig. 18—Profiles of green sand castings comparing castings and pattern dimensions.

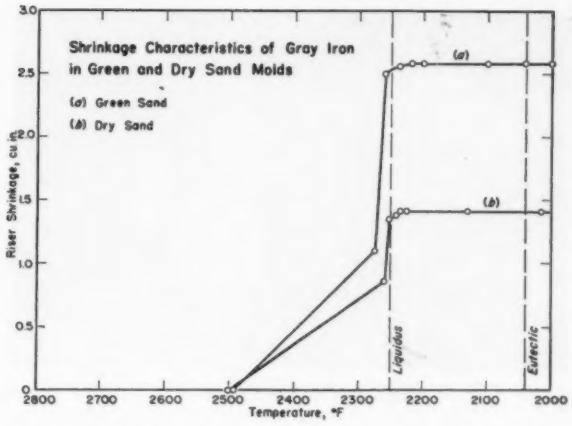


Fig. 19—Shrinkage characteristics of gray iron in green and dry sand molds.

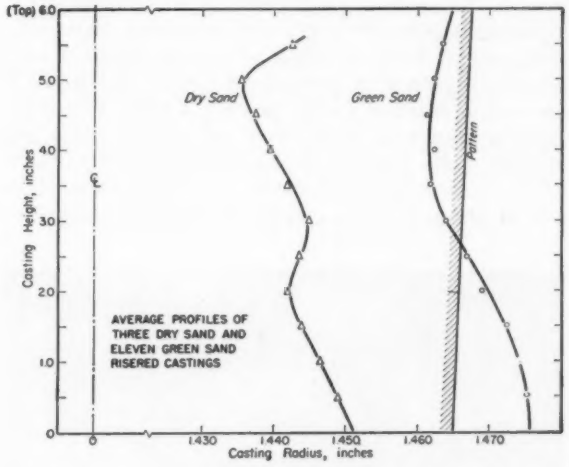


Fig. 20—Average profiles of three dry sand and 11 green sand risered castings.

ings do not explain their observations. Our investigation considered both mold wall and metal movement. With superior sand and good molding, dilation can be overcome. (Evidence of this is in the quantity of good industrial castings produced.) With their closed system in green sand, they observed dilation caused by graphite precipitation. In our closed system in dry sand, no dilation was observed on graphite precipation. The conclusion could be that the two sets of results overlap. One, utilizing a strong green sand, reports dilation only when the system can expand no other way. The other, possibly with weaker green sand, reports dilation always, and with dry sand that is stronger than the best green sand reports no dilation at all.

D. *Direct Measurement of Shrinkage*—An insulated riser technique was evolved for studying shrinkage. The riser was 1½ in. in diameter and 4 in. high. It fed a 3 by 6-in. cylindrical casting. The riser cavity was formed by a shell of "C" process sand approximately 1/16 in. thick. The shell was then packed with 1½ in. of vermiculite. The top of the riser was in-

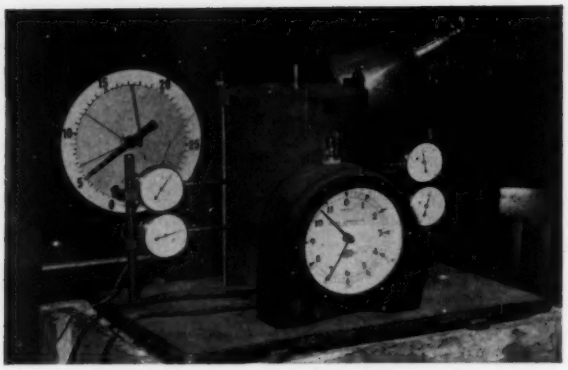


Fig. 21—Dilation apparatus.



Fig. 23—Rotating apparatus.

sulated with rice hulls on completion of pouring. The fall of the liquid level in the riser was followed with a metal probe thrust through the rice hull covering. These measurements were then converted into volumes. Figures 22 and 26 illustrate this apparatus.

Figure 17 shows the effect of pouring temperatures on riser demand in green sand molds, and Fig. 19 shows the effect of green and dry sands. The most notable feature of these curves is that in almost all instances, the risers cease feeding before the center of the casting reaches the eutectic temperature. The curves are plotted as volume of feed metal against the temperature of the casting. Since there is a thermal block developed at the liquidus resulting in a period of constant temperature for 40 to 50 sec, any volume changes occurring during this period appear as vertical lines. Actually the volume change develops over an interval of time. This accounts for the sharp vertical slope of some of the curves at the liquidus. It can be seen that higher pouring temperatures involve greater shrinkage effects. This is to be expected since liquid contraction must be fed as well as solidification contraction. The comparison of riser feeding in green and dry sand molds, given in Fig. 19, illustrates that castings made in dry sand molds require less risering.

The data from these experiments are too scarce, particularly in the liquid region, to expect any more than qualitative results. Also, the riser did not func-



Fig. 22—Apparatus for measuring shrinkage.

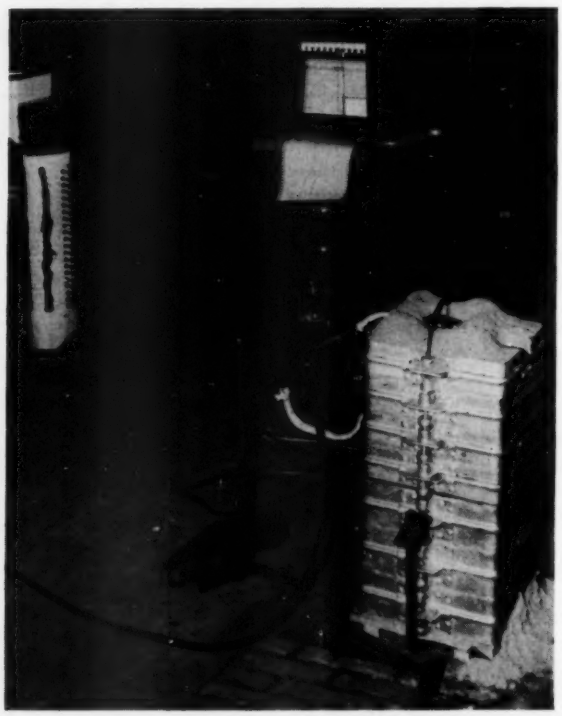


Fig. 24—New feed measuring apparatus.

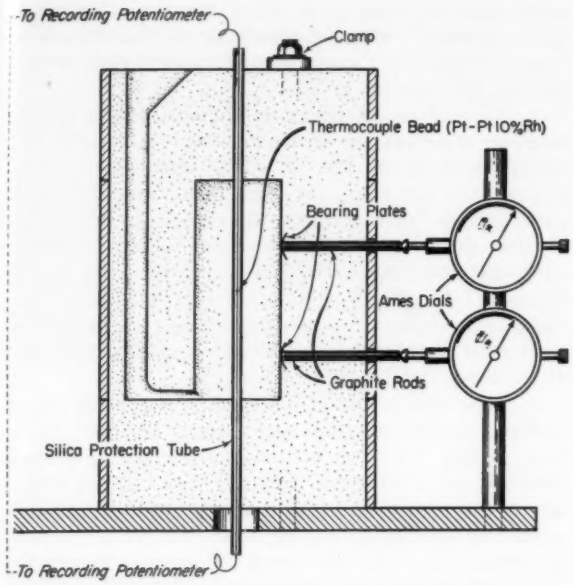


Fig. 25—Dilation measurement apparatus.

tion as a relief valve for the system when the graphite precipitated. It had been hoped that the riser would remain liquid long enough so that when the graphite precipitated, instead of enlarging the casting, it would force the still liquid eutectic metal back into the riser. The riser fed efficiently as evidenced by their relatively flat tops, Fig. 14. The heat flow of the system is such that mere insulation is not enough. To correct the heat flow pattern a very large riser would be necessary, and measurements would be more difficult than ever since large changes in height over a very small cross-section are more accurate than small changes over a large cross-section. To circumvent this difficulty, the method now in use was evolved; a 1-in. diam riser 10 in. high is enclosed in a resistance furnace, which pumps heat into the system and keeps the riser molten as long as necessary. Figure 24 is a picture of this newer piece of equipment.

Castings made by this method and many similar cylinders risered without the insulating sleeve, were carefully measured with micrometers. It was found, without exception, that the green sand castings had larger dimensions than the dry sand castings. The dry sand castings adhered closely to the dimensions expected from allowance for solid contraction. The green sand castings showed dimensions almost the same as the pattern from which they were made. Figure 20 illustrates this point. There are average configurations of castings made from the same pattern. It is interesting to note how both types of castings are dished near the center. This is a form of external feeding which promotes soundness. Many castings of irons with low carbon equivalents could not be made sound without elaborate or expensive risering systems if this did not occur. The castings shown in Fig. 13 are examples of how dishing will promote soundness and how some types of porosity may occur. These two castings are similar in size to

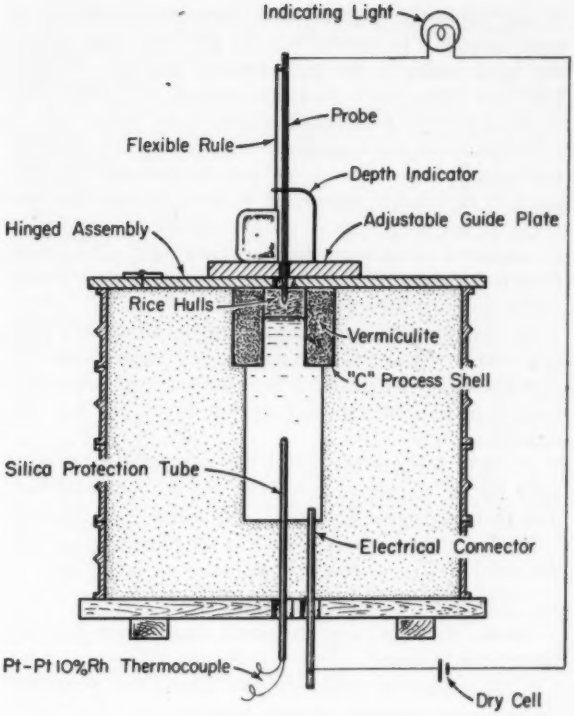


Fig. 26—Assembly for measuring rate of shrinkage.

those previously mentioned. However, they were poured in open top molds. One casting was poured and left to solidify. The other was covered with rice hulls immediately after pouring. The top of the open casting quickly froze. When the eutectic solidified the shrinkage demand (small as it is) was met by the movement of the weak walls. There appears to be a limit beyond which the walls cannot move to satisfy such shrinkage. Particularly larger castings evidence this. An inertia effect created by the larger masses involved and possible mechanical bridging prevent too much movement. (Note Fig. 15.) The casting which was insulated on top did not freeze prematurely. When the eutectic solidification demanded feed metal, the atmosphere was able to penetrate the mushy mass at the top and work downward between the dendrites. It can be seen that this porosity extends almost a third of the way down the casting. With the system thus relieved by atmospheric pressure, the walls did not have to move. Figure 18 illustrates the configuration of these two castings as measured with micrometers.

This simple experiment develops the theory that it is easiest for the atmosphere to satisfy reduced pressures within the casting by attacking the system at its weakest point. If for instance it is easier to penetrate a hot spot formed by a knock-off core than move the mushy metal in the riser, it will do so. This results in porosity directly below the riser, where it is least expected. Internal angles are also weak points.

III. Conclusions

A. Sand is an important variable in controlling gross shrinkage defects. Such defects, called sinks,

draws, pipes, or shrinks, can often be remedied by sand control. It should not be felt that the use of dry sand molds is the only remedy for such defects. Dry sand is probably the surest method of control, but is expensive for production work.

B. Graphite precipitation will account for some of the expansion in castings. These dimensional changes are not necessarily connected with shrinkage defects.

C. If for any reason normal graphite formation is inhibited or delayed, normal risering practice may function erratically. Hydrogen, oxygen, and water vapor are harmful in this respect.

D. The graphite formed during solidification does not necessarily need to compensate for all solidification shrinkage to produce internally sound iron. External dishing of the casting can make up the difference.

E. The feeding action of the riser may stop after only 20 to 40 per cent of the metal has solidified, due to bridging of dendrites across the feed channel.

F. Gray iron risers feed liquid shrinkage, mold dilation, and very little solidification shrinkage.

IV. Summary

Briefly summarizing, the work done since January 1950 is as follows:

1. A complete bibliography has been prepared covering the risering of gray iron.[1]

2. Castings submitted by the Gating and Risering Committee were sectioned and examined.[2] The results of this work showed that those castings with no localized shrinkage had shorter zones of columnar dendrites than the castings exhibiting local shrinks. It was concluded that solidification behavior may thus affect the type of shrinkage observed.

3. A method of measuring shrinkage was sought. Preliminary attempts by direct measurement of the void formation were not considered adequate or accurate.

4. Rotation of spherical castings, 4 in. in diam, during the solidification process was tried; the purpose was to completely isolate the shrinkage within the casting for easier measurements. These castings were poured in green and dry sand molds under various test conditions. The results indicated that the shrinkage observed in castings from green sand molds was not indicative of true solidification shrinkage since these castings dilated while freezing. This fact was observed while comparing the size of these castings with that of the sound castings made in dry sand molds. Measurement of the solidification shrinkage of the dry sand castings was impossible because these castings seemed to shrink by external deformation.

5. The effect of gases such as those encountered in melting the metal or in the mold was investigated, still using the spherical castings. Hydrogen, oxygen, and water vapor appear to promote shrinkage in normally sound, dry sand castings. Water vapor produced gasiness in both the dry and green sand castings. Nitrogen and carbon dioxide had no effect whatever.

6. Dilation of a cylindrical casting during solidification was measured using Ames dials. The results show that in green sand molds dilation starts soon after pouring. When the eutectic is reached, dilation continues at a lower rate. Dry sand castings indicated no dilation at all.

7. Rate of riser feeding was determined in a pycnometer type casting by measuring the fall of the liquid level in a riser of small diameter. It was shown that the risers for both green and dry sand castings stop feeding before the center of the casting reaches the eutectic. An indication of the quantity of metal required to feed dilation was also determined. Limitations of the accuracy of the results using an insulated riser in these experiments prompted development of better apparatus to continue these and future experiments.

8. New equipment which maintains the riser in a molten condition while the casting solidifies, was constructed. This consists mainly of a tall, thin riser enclosed in a resistance furnace. Such a procedure keeps the riser molten so that any volume changes occurring in casting during solidification will cause a fluctuation of the riser level. This is similar to the action of an ordinary thermometer. The liquid level in the riser is followed as shrinkage occurs, with a metal probe which lights an electric bulb when contact is made with the surface of the metal.

V. Plans for Future Work

The pycnometer type apparatus for measuring shrinkage has only recently been completed; about four test runs have been made to date. Modifications are being made on the basis of current tests, and it is expected all necessary changes will be made and test work resumed on January 5. Results have been encouraging. This apparatus combines the features found necessary by past experience.

Using the molten-metal pycnometer, the following will be done:

1. Measurements will be made of the rate of shrinkage of gray iron as effected by (a) mold, (b) composition, (c) pouring temperature, and (d) gases.

2. The volume of shrinkage will be determined as it is effected by the above variables.

3. The influence of solidification on shrinkage characteristics will be assessed; that is, any unique changes in shrinkage behavior will be explored to see if there are attendant changes in micro- or macrostructure.

4. The effects of mass, cooling rate, pouring rate, pouring temperature, and superheat will be studied in detail if there are indications these variables influence shrinkage characteristics.

5. At conclusion of work a simple, easily understandable report will be prepared designed specifically for interpretation and use of the practical men in the shop. Such a simple interpretation cannot be made until a more clear understanding of the problem has evolved.

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DISCUSSION

Chairman: J. S. VANICK, International Nickel Co., Inc., New York.

Co-Chairman: F. J. DOST, Sterling Foundry Co., Wellington, Ohio.

Recorder: C. A. JOHNSON, Armour Research Foundation, Chicago.

L. G. MILLER *(Written Discussion)*:[1] The relation between piping tendency and the sea coal content of molding sand was established by experiments conducted at the Engineering Experiment Station of Michigan State College in 1939 and 1940. Results of this work were published in the TRANSACTIONS of the American Foundrymen's Association, vol. 48 (1940) under the title "Influence of the Mold on Shrinkage in Ferrous Castings." This investigation was part of a program of foundry research proposed by Professor H. L. Womochel, and was carried out under his supervision. This paper was the original paper of the several on this important subject, and still remains the most complete.

The subject of the relative effect of various types of clay bond on shrinkage was discussed in Michigan Engineering Experiment Station Bulletin No. 92, July, 1941, under the title "Effect of Green Compressive Strength of Molding Sand on Shrinkage defects in Gray Iron Castings" by H. L. Womochel and C. C. Sigerfoos.

C. C. SIGERFOOS *(Written Discussion)*:[2] Authors Schmidt and Taylor as well as the Research Committee of the Gray Iron Division are to be highly commended for the progress being made in basic foundry research as reported in "Risering of Gray Iron Castings." The authors have recognized the vast complexity of the problem of risering gray iron and they have attacked the problem by making castings using methods that appear to give control over the molten metal and the mold materials. The ingeniously devised experimental equipment has yielded data that the practical foundryman can understand and appreciate. The closely controlled practical approach to risering problems described in this paper is in considerable contrast to some of the highly theoretical heat transfer solutions being offered. The mathematical treatment has a certain degree of merit, but it must be recognized as difficult to apply even when it is well understood.

It is encouraging to note that this AFS-sponsored research is in general agreement with the previous work of Womochel and Sigerfoos, AFS TRANSACTIONS, 1940[1] and Sanders and Sigerfoos, AMERICAN FOUNDRYMEN, Jan., 1951[2]. In these papers it was clearly pointed out that the effect of the mold materials on apparent shrinkage is very large. It is sincerely hoped that the evidence presented in these earlier papers together with this report by Schmidt and Taylor will go far in correcting the serious mistake that was made by the authors of the "Analysis of Castings Defects" book that very bluntly states that mold or core sands do not cause shrink cavities or depressions.

The plans for future work as outlined in this paper are very broad and cover the principal phases of shrinkage. Certainly no factor in this important study should be neglected. However, it appears that in the past more work has been done on the metallurgical aspects of true shrinkage (change in molten metal density) than has been done on apparent shrinkage (due to mold dilation). This leads to the suggestion that the most practical and fruitful research in the near future would be on the study of mold materials. For example, answers to the following questions would seem very important: Can a correlation be found between the physical properties of molding sand and the mold dilation (mold wall movement) that occurs during and after pouring? What laboratory instrument can be developed to measure a sand's tendency to dilate? What new materials can be found to inhibit dilation? Answers to these questions would obviously greatly assist in providing practical solutions to many gray iron risering problems.

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1. H. L. Womochel and C. C. Sigerfoos, "Influence of the Mold on Shrinkage in Ferrous Castings", AFS TRANSACTIONS, vol. 48 (1940).

2. C. A. Sanders and C. C. Sigerfoos, "Gray Iron Shrinkage Related to Molding Sand Conditions", AMERICAN FOUNDRYMEN, Jan., (1951).

3. *Analysis of Casting Defects*, American Foundrymen's Society, Chicago, 1947.

MR. SCHMIDT: It is recognized that mold dilation has profound effects on the observed shrinkage of gray irons. Because of these effects, this problem will have to be recognized and a solution gained before the entire shrinkage problem is solved.

However, there are certain metallurgical factors involved with equally profound effects which must be acknowledged and carefully studied before a coherent program of risering practice can be presented to the industry.

R. W. KRAFT:[3] Are the shrinkage characteristics due to the strength of the dry sand or the moisture content therein?

MR. SCHMIDT: The mositure content of the molding material appears to have little effect on the shrinkage characteristics of the iron in a metallurgical sense.

It seems that the moisture in green sand effects its strength at elevated temperatures in such a manner that metals which freeze without a solid supporting skin can dilate.

C. R. ISLEIB:[4] Do you draw any conclusions from the graph, Fig. 2, and does this connect with the second point of your summary?

MR. SCHMIDT: At the present time, the work in progress has not been extended to cover this point.

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[1] Dean of Engineering, Michigan State College, East Lansing, Mich.
[2] Assoc. Prof. of Mechanical Engineering, Michigan State College, East Lansing, Mich.
[3] Metallurgist, American Brake Shoe Co., Mahwah, N. J.
[4] Metallurgist, International Nickel Co., Inc., Bayonne, N. J.

SOLIDIFICATION OF CYLINDERS

By

V. Paschkis*

I. Introduction

All projects carried out so far in the cooperative endeavor between the A.F.S and Columbia University deal with greatly simplified conditions in which heat flow occurs in one direction only. Slabs, for example, were thought of being sufficiently long and wide so as to permit disregarding all end effects; heat flow and solidification would proceed only in the direction of thickness, perpendicular to the surface of the slab.

Spheres are, mathematically speaking, one-dimensional problems.

Cylinders, as far as they have been treated to date, were considered to have been so long that heat flow through the ends could be disregarded and solidification and heat flow were thought of occurring in radial direction only.

Actual castings always do have end effects and therefore work was undertaken to show the increased rate of solidification in bodies exposed to cooling in two directions. Specifically, a cylinder of finite length was selected.

Previous investigations of this laboratory had established for cylinders only times to reach the liquidus; therefore complete solidification of a cylinder without end effects was now studied, to serve as basis for a comparison with two dimensional freezing (cylinder with end effects).

II. Properties

The thermal properties for all investigations were the same as those formerly used. A solidification range from 2700 to 2600 F was assumed although it is now recognized that such range would be somewhat unusual for the carbon steels under consideration.

The error incurred by assuming this solidification range can be estimated from the report of the Heat Transfer Committee "Influence of Properties on Solidification of Metals," TRANSACTIONS, A. F. S., vol. 55, p. 54 to 61 (1947). The value was chosen because investigations of solidification of large slabs and of spheres were carried out using this range.

* Technical Director, Heat and Mass Flow Analyzer Laboratory, Columbia University, New York.

This is a Progress Report on an A.F.S.-sponsored Research Project on Heat Transfer.

The values used were as follows:

Thermal Conductivity:	
Liquid steel	9.2 Btu/ft, hr, F
Solid steel	18.4 Btu/ft, hr, F
Sand	0.9 Btu/ft, hr, F
Specific heat:	
Liquid steel	0.2 But/lb, F
Solid steel	0.165 Btu/lb, F
Sand	0.28 Btu/lb, F
Density:	
Liquid steel	450 lb/cu ft
Solid steel	468 lb/cu ft
Sand	93.6 lb/cu ft
Heat of fusion:	126 Btu/lb
Pouring temperature:	2845 F
Diameter of the cylinder:	2 in.
Length of the cylinder:	
Case A	infinite
Case B	2 in.

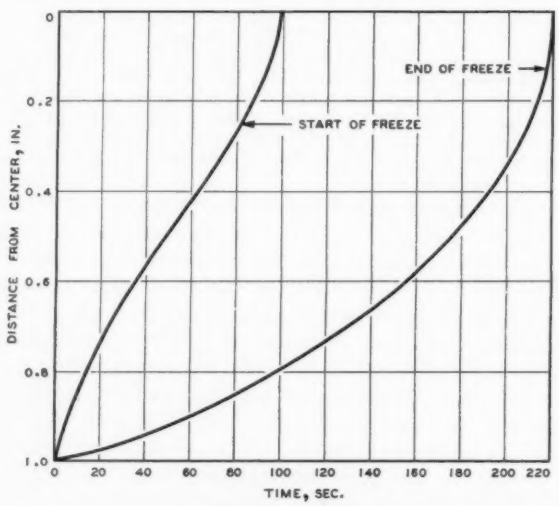


Fig. 1—Progress of solidification. Long cylinder, 2-in. diam. Pouring temperature, 2845 F. Sand at 70 F.

Thickness of sand: "Sufficient," that is, selected so that at end of solidification no appreciable temperature rise occurs at the outer surface of the sand.

III. Long Cylinder

1. **Progress of solidification.** The solidified thickness (radius) is plotted in Fig. 1 against time after pouring. In the investigation it was assumed that the mold is filled in so short a time that this filling time may be considered zero.

It will be noted that the time to reach the solidus at the center is 220 sec.

2. **Necessary sand thickness.** It was found that sand at a distance of 2.2 in. measured from the axis of the cylinder did not show any appreciable temperature increase.

3. **Consideration of volume/area ratio.** Chvorinoff[1] has stated that castings of various sizes and shapes solidify according to the ratio volume/area hereafter called shape factor, *S*, and that the solidification time is proportional to the square of this ratio. The ratio has the dimension of length.

Former investigations seemed to disprove this claim but were based on the old, and by now discarded, concept of solidification being completed upon reaching the liquidus.

Bodies of equal shape but different size, such as spheres of various diameters or cylinders of great length and varying diameters solidify in times proportional to the square of their critical dimension.

For the three simple shapes (sphere, long cylinder and slab with no end effects) the shape factor, *S*, can be expressed in terms of a critical dimension, *D*, as follows:

Sphere $S_s = D/6$ (critical dimension: diameter)
Cylinder $S_c = D/4$ (critical dimension: diameter)
Slab $S_p = D/2$ (critical dimension: thickness)

Former experiments have shown that the time to reach the solidus, that is complete solidification time, is a function of the degree of superheat. Table 1 shows the times for complete solidification for slab and sphere, both having a shape factor $S_s = S_p = 2$ in. In addition, in Table 1 the ratio of the times is shown and it will be seen that for high degrees of superheat the ratio is nearly 1. For low degrees of superheat however the ratio is somewhat smaller and becomes quite low near the liquidus, that is with very small degrees of superheat.

It is interesting that the cylinder apparently fits in well with Chvorinoff's concept (See Tables 2 and 3).

Based on former experiments Table 2 has been set up which shows solidification times for the three different bodies for different sizes. Table 3 shows the solidification times reduced to the same critical dimension, *D*, and the ratios of these times. Table 2 contains moreover the ratios of shape factors as well as the square of these ratios.

It may be noted that the ratio of square of the shape factor ratios cylinder/sphere is 0.25 and slab/sphere is 0.111. The corresponding ratios of times use 0.251 and 0.108 respectively.

Obviously the check is very good.

Values of solidification time for the cylinder poured at low temperatures are not available.

The three shapes mentioned before are, mathematically speaking, one-dimensional. The finite cylinder discussed in Section IV is the first two-dimensional problem within the cooperative research program.

IV. Short Cylinder

1. **Experimental difficulties.** It was planned originally to investigate a cylinder of a length seven times the diameter. Extreme difficulties in building the circuit were the reason for changing the procedure for starting with a cylinder of length equal to the diameter. It is planned in future years to increase that ratio up to the point where radial solidification at mid height equals the radial solidification in a cylinder of infinite length.

Even with the cylinder of 1:1 ratio difficulties were encountered which are described in Appendix 1. These difficulties made a change of procedure necessary. In the computational tests which were carried out only the steel casting was investigated, not the sand. In order to explain that, it should be remembered that during solidification heat flows from the steel into the sand. It is known that during solidification the interface temperature between casting and sand remains almost constant. Under certain simplifying assumptions the interface temperature is entirely constant and is called the "Riemann temperature," because Riemann has shown how to compute this temperature (see Appendix 2).

In the computation experiments, it was assumed that the surface of the casting remains permanently at the Riemann temperature; then additional computations were made in which after a specified length of time the interface temperature between casting

TABLE 1—RELATIVE INFLUENCE OF POURING TEMPERATURE ON SOLIDIFICATION RATES ($S_p = S_s = 2$)
(Time to Reach Solidus)

Pouring Temp. F	Solidification Time, Sec Slab (D = 4 in.)	Sphere (D = 12 in.)	Ratio Slab/Sphere
3010	4600	4320	1.06
2900	3900	3920	0.99
2820	3280	3520	0.93
2800	3100	3400	0.91
2740	2375	3280	0.73

TABLE 2—COMPARISON OF SOLIDIFICATION TIMES

Shape	Size (D) in.	Times, sec to reach Liquidus	Solidus	Pouring Temp. F
Sphere	6	480	880	2820
Cylinder	2	98	220	2845
Slab	4	510	3500	2845

TABLE 3—SOLIDIFICATION TIMES OF TABLE 2 REDUCED TO CRITICAL DIMENSION D = 2 IN.

Shape	Size, in.	Times, sec to reach Liquidus	Solidus	Shape factor S, in.	S^2	Ratio of Time to reach Solidus (Slab = 1)
Sphere	2	53.3	94	⅓	0.111	0.108
Cylinder	2	98	220	½	0.25	0.251
Slab	2	127	875	1	1	1

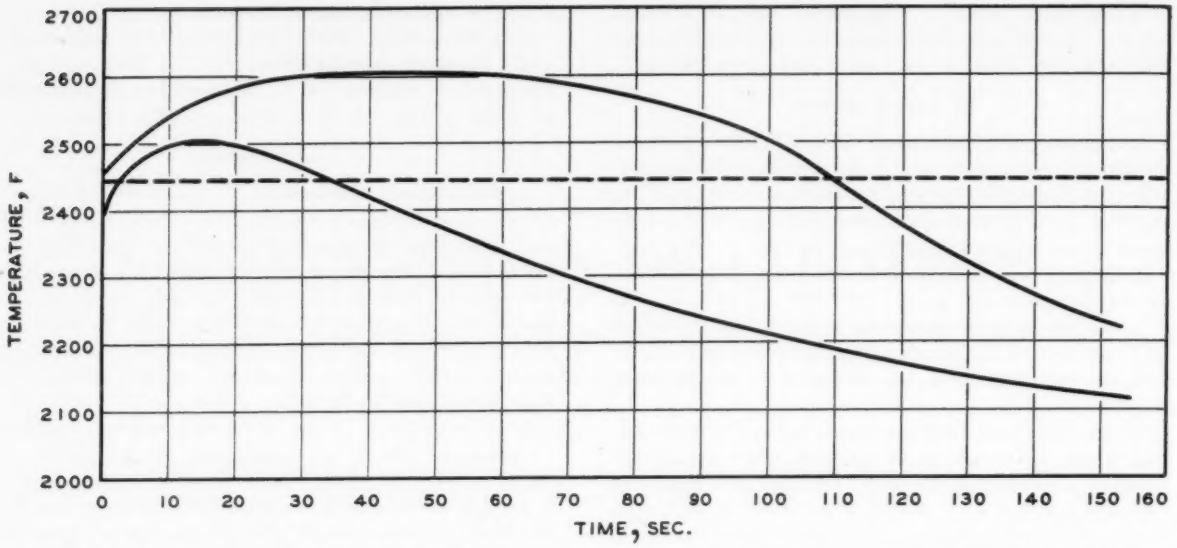


Fig. 2—Time-temperature curves for interface. Cylinder, 2-in. diam, 2 in. long. Pouring temperature, 2845 F. Interface held at Riemann temperature (2444 F). For meaning of location 1 and 25 see Fig. 3.

and mold was dropped to a value 10 pct below the Riemann temperature. No tests were run with the interface at a temperature higher than the Riemann value.

In order to estimate the time at which the temperature dropped, a preliminary experiment was run including steel and sand. This experiment covered only the very early parts of solidification and even for these values must be considered only approximate. The surface temperatures were observed and are shown in Fig. 2. It will be seen that the properties mentioned in Part II, the temperature at the interface at the start of solidification actually drops to within roughly 2 pct of the Riemann temperature, but at present after 20 to 40 sec drops to a value roughly 10 pct below. It was not practical to lower the interface temperature gradually and therefore the decrease of the interface temperature was made in one step optionally at two different times after pouring. Appendix 3 comprises a discussion of the validity of the approach by comparing one dimensional solidification in sand with that obtained by assuming "Riemann temperature" at the interface.

2. **Method of presentation.** In two-dimensional solidification there is a choice of different ways of presenting the progress of solidification. It is either possible to draw several sets of curves similar to Fig. 1, each set holding for a specified distance from the flat surface of the cylinder. Such a presentation would not be very clear with regard to the axial distribution of solidification.

Instead a dual method of presentation is selected. Figure 3 shows the pattern of measured points in the cylinder. Tables are presented to show the times at which each point reached the liquidus and solidus respectively at each of these points. In addition figures are shown (for example Fig. 4) in which isochrones are drawn. Isochrones are lines which connect all points in a casting which reach a specified temperature at the same time. Each isochrone therefore carries as notation a time. Such isochrones are shown for the liquidus and the solidus temperature and for the different assumptions regarding the interface temperature.

3. **Results.** Results are shown as follows: Table 4 holds for the liquidus and Table 5 for the solidus. In each table the time to reach liquidus or solidus is noted for each of the 28 points in the circuit. Each table contains information for three experiments: the interface being held at the Reimann temperature (2444 F) during the entire solidification; the interface being held for 40 sec at the Reimann temperature, then dropped to 2200 F; and the interface being held at the Riemann temperature for 20 sec, and then dropped to 2200 F. For each experiment, three col-

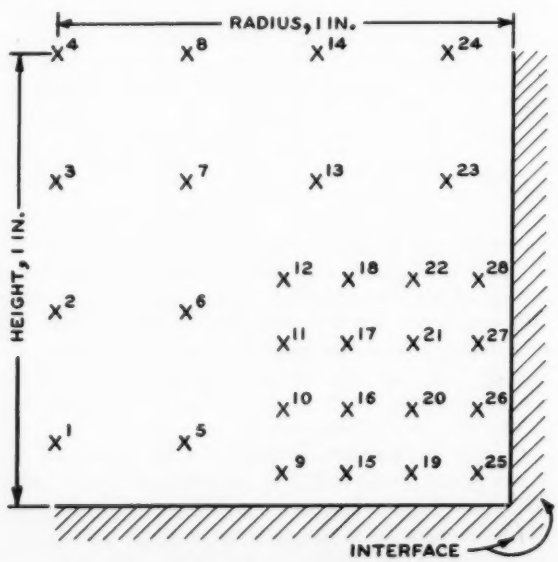


Fig. 3—Distribution of observation points.

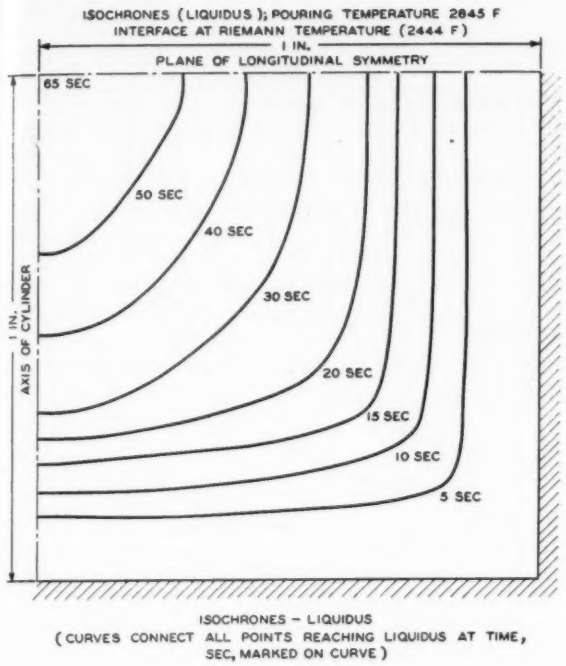


Fig. 4

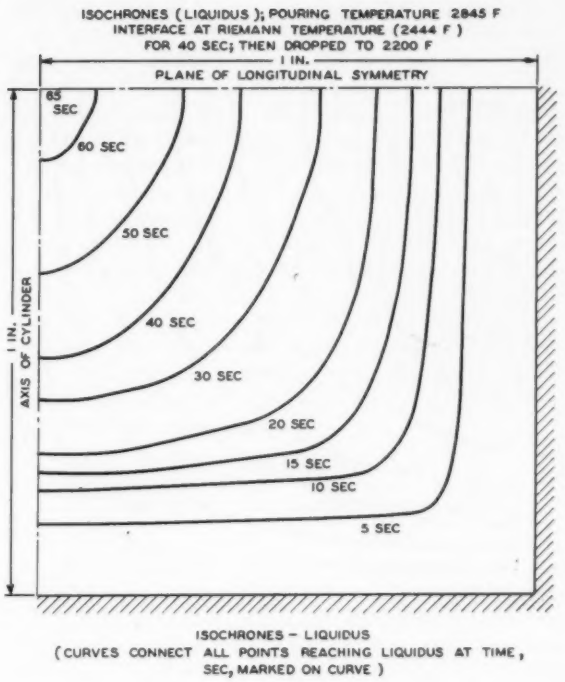


Fig. 5

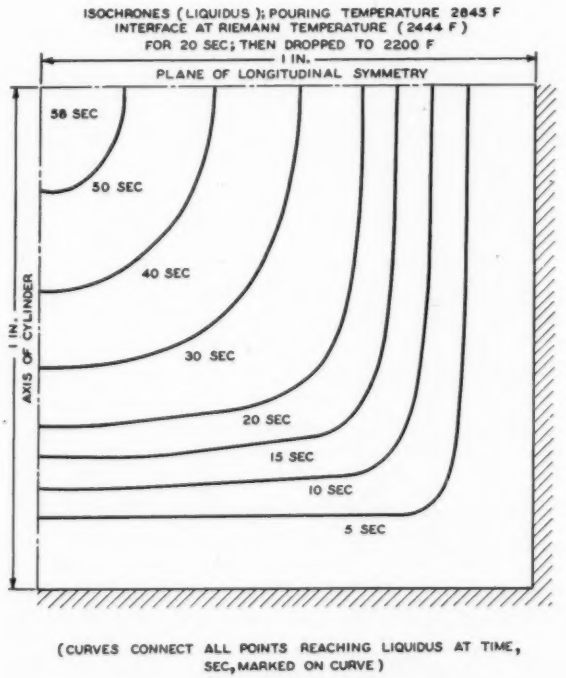


Fig. 6

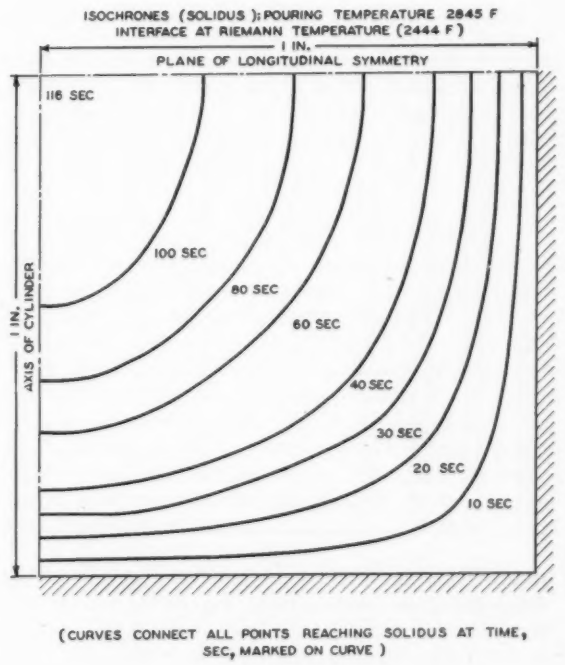
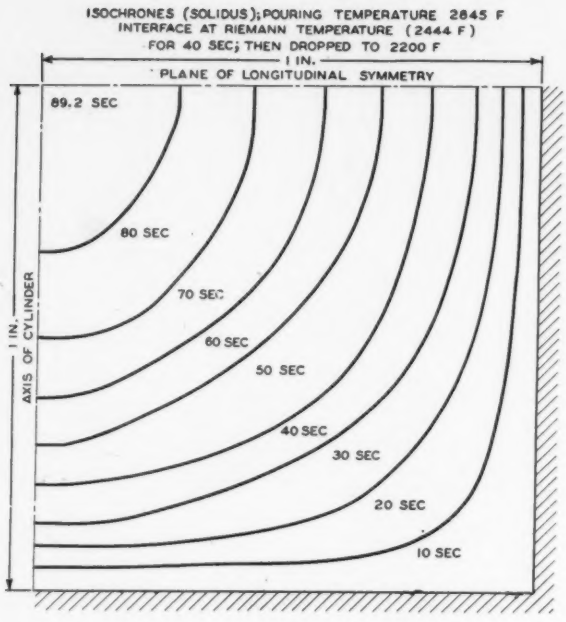


Fig. 7

umns are shown with the minimum and the maximum time observed and probable time. Any point reaching recorded change before 40 and 20 sec respectively undergoes the changes in all three experiments at the same time. Therefore, minimum and maximum values are not repeated beyond the "Riemann Temperature test."

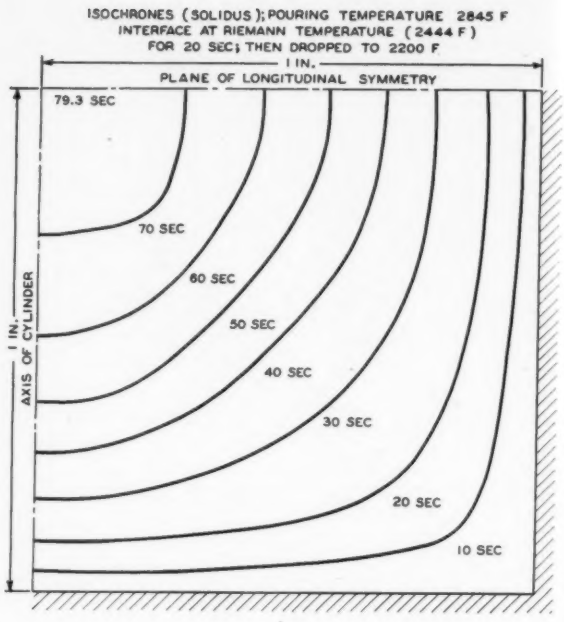
The probable value of time is selected on the basis of notations made during the tests. For example, if in a run it was noted that the change at a certain point was made too late (e.g. Table 2, Point 7) then the late time was disregarded in selecting the probable value although under "maximum" the late time is listed.

Figures 4 to 6 comprise the isochrones for reaching the liquidus and Figs. 7 to 9 those for reaching the



(CURVES CONNECT ALL POINTS REACHING SOLIDUS AT TIME, SEC, MARKED ON CURVE)

Fig. 8



(CURVES CONNECT ALL POINTS REACHING SOLIDUS AT TIME, SEC, MARKED ON CURVE)

Fig. 9

TABLE 4—VALUES OF TIME TO REACH THE LIQUIDUS, SEC

	Interface at Riemann Temperature			Interface at Riemann Temperature for 40 sec			Interface at Riemann Temperature for 20 sec		
Point	Min	Max	Probable	Min	Max	Probable	Min	Max	Probable
1	6.1	6.7	6.3			6.3			6.3
2	36.0	42.2	38.3			38.3	30.0	32.0	31.0
3	53.7	60.1	54.5	54.2	54.3	54.3	47.0	50.1	48.5
4	57.6	78.6	65.0	65.0	?	65.0	56.6	61.3	58.0
5	4.7	5.0	4.8			4.8			4.8
6	27.9	35.2	28.0			28.0	27.0	27.2	27.1
7	42.5	44.9	42.8	41.8		41.8	38.1	39.6	38.9
8	51.7	59.2	52.7	49.1	53.0	49.5	41.9	44.1	43.0
9	1.3	1.6	1.3			1.3			1.3
10	8.9	9.8	9.4			9.4			9.4
11	19.3	22.5	19.4			19.4			19.4
12	23.7	28.6	24.4			24.4	27.8	22.0	22.0
13	25.7	30.4	26.4			26.4	?	?	
14	27.3	32.0	28.0			28.0	25.0	26.4	26.0
15	1.5	2.2	1.6			1.6			1.6
16	8.7	9.6	9.4			9.4			9.4
17	15.3	19.2	17.0			17.0			17.0
18	18.6	21.2	18.8			18.8			18.8
19	1.3	1.6	1.3			1.3			1.3
20	6.5	8.3	6.6			6.6			6.6
21	8.1	10.7	9.0			9.0			9.0
22	8.3	10.0	9.6			9.6			9.6
23	4.8	5.0	4.8			4.8			4.8
24	4.8	5.0	4.8			4.8			4.8
25	0.7	1.0	0.7			0.7			0.7
26	1.3	1.6	1.3			1.3			1.3
27	1.3	1.6	1.3			1.3			1.3
28	1.6	2.0	1.6			1.8			1.8

TABLE 5—VALUES OF TIME TO REACH THE SOLIDUS, SEC

	Interface at Riemann Temperature			Interface at Riemann Temperature for 40 sec			Interface at Riemann Temperature for 20 sec		
Point	Min	Max	Probable	Min	Max	Probable	Min	Max	Probable
1	33.9	38.2	34.2			34.2	26.2	28.2	27.2
2	88.0	100.2	90.0	66.3	66.5	66.4	54.0	59.5	56.7
3	106.2	112.9	108.9	81.9	85.1	83.5	70.3	75.6	72.8
4	112.7	120.8	116.2	88.1	90.4	89.2	76.7	82.0	79.3
5	27.9	32.5	28.0			28.0	23.5	24.5	24.0
6	66.5	74.6	69.8	56.2	56.3	56.3	45.7	49.3	47.5
7	92.5	100.6	96.0	73.7		73.7	62.3	67.9	65.1
8	100.9	109.8	104.6	79.9	80.9	80.4	68.2	74.0	71.1
9	11.0	11.9	11.4			11.4			11.4
10	25.3	27.0	26.0			26.0	22.6	24.2	23.9
11	45.1	47.4	46.0	42.5	42.8	42.7	31.4	35.1	33.2
12	48.7	53.7	50.5	44.8	45.5	45.2	34.0	40.0	37.0
13	64.2	69.4	65.1	55.4		55.4	44.1	47.2	45.6
14	71.0	77.2	72.0	59.5	60.3	59.9	48.0	55.3	51.6
15	9.2	12.3	11.0			11.0			11.0
16	23.1	25.6	23.1			23.1	22.3	23.6	22.3
17	32.8	42.8	34.0			34.0	26.4	31.0	28.5
18	42.3	48.2	44.4	40.3	41.5	40.9	30.0	35.9	32.9
19	7.3	8.1	7.8			7.8			7.8
20	16.1	20.0	17.0			17.0			17.0
21	22.3	26.2	23.0			23.0			21.0
22	23.9	25.1	24.5			24.5	22.0		22.0
23	26.9	30.0	27.6			27.6	22.6	24.1	23.3
24	29.5	32.6	31.6			31.6			24.2
25	4.7	5.7	4.8			4.8			4.8
26	8.3	10.8	8.3			8.3			8.3
27	9.1	10.3	9.6			9.6			9.6
28	10.9	13.1	10.9			10.9			10.9

solidus. Figures 4 and 7 are based on the case of constant interface temperature; Figures 5 and 8 cover the case of a temperature drop after 40 sec and Figs. 6 and 9 for a temperature drop after 20 sec.

4. **Conclusions.** (a) The probable total solidification time of the casting is between 83 and 116 sec; total solidification time being the time for the center of the body to reach the solidus.

(b) The time when the interface temperature is assumed to drop from the Riemann temperature to a value 10 pct below is of some influence on the total solidification time. However, the influence is more marked from "no change to any change" as compared to the specific time of change: from "no change" to 40 sec, the solidification time drops from 116 to 94 sec whereas the variation "40-second change" to "20-second change" is only from 94 to 83 sec.

5. **Consideration of volume/surface ratio.** (a) The shape factor of this finite cylinder with a height = the diameter = 2 in. is 1/3 in. (the volume is 2π, the surface area 6π). A sphere of 2-in. diam has a shape factor of 1/3 in. According to Table 2, a 6-in. sphere requires the solidification time of 850 sec; a 2-in. sphere therefore will require $850/9 = 94$ sec.

The solidification times, assuming constant surface temperatures (83 to 116 sec) straddle this value. It is too early to state definitely the actual solidification time.

V. Further Work

It is suggested to expand the work in two directions:

(a) Perfect the technique, so that solidification of the short cylinder in sand can be studied completely.

(b) Study the implications of the Riemann temperature. Should it be possible to establish relationships between volume of casting and mold on one side and interface *vs* the Riemann temperature on the other a great simplification of casting studies would be obtained. It cannot be stated at this time if work with the Riemann temperature will prove permissible, but if so, it will be possible to predict cooling rates from an observation of the interface temperature.

APPENDIX I

Description of Difficulties Encountered

1. **General Comments**—In the computation experiments on the Heat and Mass Flow Analyzer, the casting and mold are each represented by a resistance capacitance network. The two networks have to be computed in a certain relationship which depends on the thermal properties of steel and sand. Moreover the circuit elements depend on the time scale used: the longer the computing experiment should run, the higher the resistance and capacitance values become.

In representing two-dimensional problem a large number of units must be used. It seemed reasonable to have in the steel at least 28 points of observation.

It is necessary to carry out the experiments in not too short a time because of the difficulty of observing the 28 points in one. On the other hand if the times become too long the tests become unreliable because of leakage.

In order to represent a cylinder on the computer the body is thought of divided into a number of radial and axial layers of equal thickness. Proceeding radially from the center toward the outside the volume of each layer increases and the electric capacitances accordingly. This results in the use of very small capacitances in the center and larger ones on the outside. Small capacitances tend to accentuate the leakage difficulties.

2. **Cylinder with ratio 7:1**—For such a cylinder it was found that the capacitances and resistances necessary to represent the system for a reasonable time scale became excessive. Since not enough equipment was available it was decided to start with the cylinder with the ratio 1:1.

3. An initial experiment was run with a time ratio 1:1, that means that one second in the electric computing experiment represented one of actual solidification time. Since the total solidification time is in the order of magnitude of 90 to 100 sec, operators had to follow the temperature changes and manually control a total of 84 changes: there are 28 points of observation in the circuit and at each point three changes are to be made. The first which consists in introducing the heat of fusion; the second, in changing the thermal properties from liquid to solid values and the third in disconnecting the heat of fusion.

It was found that it was impossible, even with three operators to carry out 84 changes in roughly 100 sec. A longer time ratio appeared necessary. Unfortunately there were not enough circuit components available to introduce a longer time ratio. This was the reason to eliminate the sand circuit and introduce the concept of the constant steel to sand interface temperature; or of a temperature changing in one step only. This procedure eliminates the necessity of working with the sand circuit and resulted in the availability of additional circuit components for the steel. Moreover the resistances in the steel are relatively low compared to those in the sand and thus the increased time ratio with the resulting increased required resistance is easier to build for the steel than for the sand.

APPENDIX II

Computation of the Riemann Temperature

1. Two slabs contacting each other and extending to infinite thickness in a line perpendicular to the plane of contact, interchange heat if both are initially at different temperatures. Riemann has shown that under the conditions described the interface assumes a constant value from the moment of contact and holds this constant value permanently.

The constant value can be computed from the thermal conductivity k, the volumetric specific heat c′ and the temperatures t of the two bodies as follows:

In the equation subscripts 1 and 2 designate properties and temperatures of the two bodies respectively and subscript m the temperature of the interface.

$$t_m = \frac{t_1\sqrt{k_1c_1'} + t_2\sqrt{k_2c_2'}}{\sqrt{k_1c_1'} + \sqrt{k_2c_2'}}$$

Obviously the conditions described above do not hold in solidification problems. The properties of steel and sand change with temperature; in the steel heat of fusion is developing and neither body (casting and mold) is of infinite thickness. Moreover, in the present case, the interface is not a plane surface but of cylindrical shape. Yet in first approximation it may be expected that the interface would be at or near the Riemann temperature. With the properties mentioned in Section II the temperature can be computed as follows:

$$t_m = \frac{2845\sqrt{9.2\times450\times0.2}+70\sqrt{0.9\times93.6\times0.28}}{\sqrt{9.2\times450\times0.2}+\sqrt{0.9\times93.6\times0.28}}$$

$$= 2444 \text{ F}$$

APPENDIX III

Significance of Riemann Temperature

Two interface temperature curves are shown in Fig. 2: one point at the circumference of the basal plane (opposite Point 25 in Fig. 3) and one near the axis (opposite Point 1 in Fig. 3). The latter shows an increase beyond the Riemann temperature, whereas the former drops very soon to a lower value. The great difficulties in carrying out changes of properties

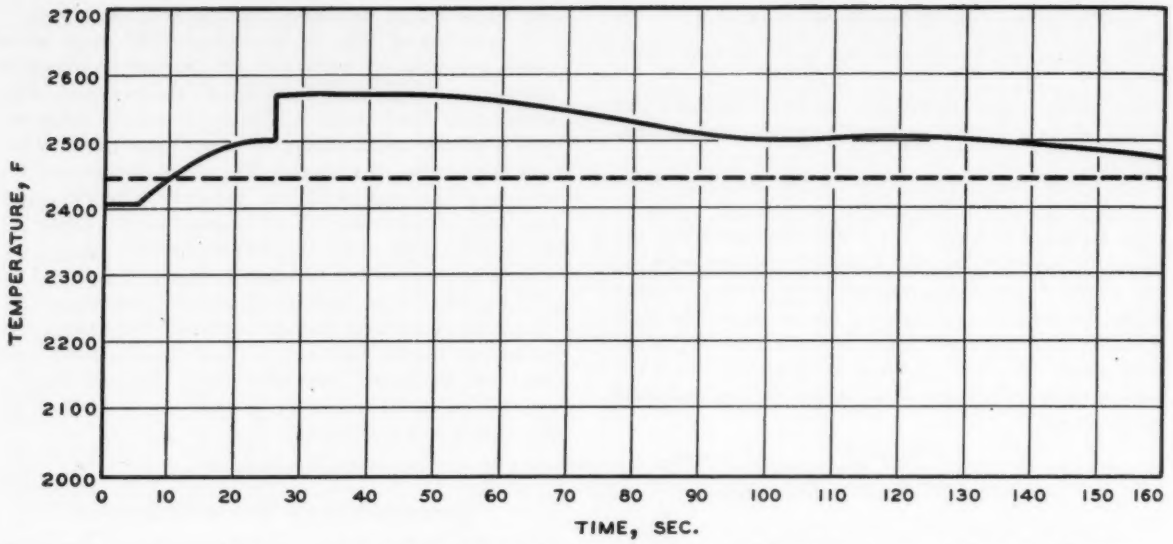


Fig. 10—Time-temperature curve of interface. Long cylin der, 2-in. diam. Pouring temperature 2845 F. Interface held at Riemann temperature (2444 F).

in the short run were the reason for not attempting to take such curves at other points of the surface. It was believed that the behavior of a point near the circumference of the basal plane (in the cross section: a corner; Point 25) was most characteristic, and based on this concept, the experiments of the short cylinder were run with the surface at the Riemann temperature or below.

Upon completion of the test for the short cylinder a test was run to compare the solidification of a long cylinder freezing in sand with the solidification behavior of a long cylinder the surface of which is held at the Riemann temperature.

The total solidification time with the surface at the Riemann temperature was 160 sec as compared with 220 sec, in the case of solidification in sand.

In order to examine the validity of this finding, the surface temperature was observed (Fig. 10). It started with approximately 2 pct of the Riemann temperature and then increased. This may be explained by the fact that as soon as the first section has dropped to the liquidus temperature, the heat of fusion has to be represented. Considering the latter as a temporary increase of specific heat, and introducing the liquidus temperature as that of the metal, a Riemann temperature of 2534 F would be obtained.

Based on this finding, the curves, Fig. 2, were reexamined. It appears possible that also in the short cylinder the interface temperature would increase beyond the Riemann value. Deviation of the interface temperature from the Riemann value depends on the ratio of heat of fusion/(specific heat $\times$ solidification range) and on the volumes of steel and sand.

If relationships between these factors and the effective interface temperature can be established, solidification studies could be greatly simplified.

DISCUSSION

Chairman: E. C. TROY, Foundry Engineer, Palmyra, N. J.

Co-Chairman: W. S. PELLINI, Naval Research Laboratories, Washington, D. C.

W. S. PELLINI *(Written Discussion):*[1] Dr. Paschkis has presented the results of another important study related to the fundamentals of the solidification of metals. Developing of scientific principles is a long and difficult process and the application of these principles to practice also requires time and hard work by the practical foundryman.

The proper application of Heat Transfer concepts to the problem of solidification is a highly specialized task and we are indeed fortunate to have the services of Dr. Paschkis in this undertaking. We are thankful also to Columbia University for this work. It may be "news" to many that funds which have been made available by the A.F.S. for this work have been matched dollar for dollar by the University.

It is of interest that the data of Fig. 1 which relate to the solidification of a 2-in. diam, long cylinder indicate a freezing time of 3.65 min. N.R.L. data for the freezing time of 8-in. diam, long cylinders of 0.30 per cent C steel indicate a freezing time of 55 min, these data were obtained by thermal analysis of castings in the foundry. By calculation based on Chvorinoff the freezing time of 2-in. cylinders should be in the ratio of the squares of the thickness dimension:

$$\frac{X}{55} = \frac{(2)^2}{(8)^2} \qquad X = \frac{(55)\ (4)}{64} = 3.44 \text{ min.}$$

This is a very good check and presents further evidence of the reliability of the Analogue approach. It also indicates that the choice of solidification range as 2700 to 2600 F while "unusual" (in that it represents a steel of higher carbon content that the 0.30 per cent C of usual interest) has not significantly affected the results. These are good reasons, all explained in previous reports, why this should be so.

In this attempt to handle two-dimensional problems, difficulties related to the need for more equipment have arisen. In the previous work heat flow was considered to occur only in a direction perpendicular to the casting face, thus there was no need to consider lateral heat flow, i.e., heat flow towards the casting end. In simple terms the length of the casting was considered to be great in comparison with the thickness dimension. With a short cylinder the problem is to evaluate the result of heat flow towards the casting surface and also towards the casting end. A great deal of effort and ingenuity was required to solve this problem. Within the limits of available funds it was not possible to purchase additional electrical units and build the elaborate "set up" required to handle heat flow in two directions in the casting and also in the sand. Thus, it was necessary to utilize the concept of a "calculated" interface temperature which could be used to represent the heat extraction effects of the sand. The electrical units required for the

[1] Head, Metal Processing Branch, Metallurgy Division, Naval Research Laboratory, Washington, D. C.

sand could now be used for the casting. Dr. Paschkis indicates that he is not entirely satisfied with the methods and that further trials would be required to assure that this procedure is completely reliable.

There is a great deal of practical value in this work for many investigations which are too complex for the previous methods could be handled simply and with less expense should this method prove reliable. This means that in the long run the usefulness of the Analogue to foundry research would be increased.

Most of the data which are presently available both from Analogue and thermal analysis studies are specific to the solidification rates and times of semi-infinite (long) shapes. If corrections could be applied for lengths less than semi-infinite (for example lengths less than approximately 6-7 times the diameter of a cylinder) it should be possible to estimate solidification times for a great variety of practical castings. This paper indicates that the time required for the solidification of a 2-in. cylinder of a length greater than approximately 7 times the diameter is decreased to .37 to .52 if the length is equal to the diameter. Should this ratio be applied to cylinders irrespective of diameter? For example, would the solidification time of an 8-in. diam cylinder 8 in. long also be .37 to .52 of the 55 min required for a long cylinder? Dr. Paschkis' comments would be appreciated.

DR. PASCHKIS *(Reply to Mr. Pellini):* I appreciate Mr. Pellini's kind words about the importance of our work.

The new proof for the good correlation between the analogue approach and that by thermal analysis is gratifying.

Regarding the query at the end of the discussion, I would like to answer as follows:

If in two geometrically similar systems all dimensions are changed in the same ratio, then times to achieve a certain temperature change with the square of this ratio. This law was applied by Mr. Pellini in reducing the observed time of 55 min (for an eight-inch cylinder) to 3.44 min (for a two-inch cylinder). The same procedure is permissible for short cylinders, and hence the question is to be answered by "yes": solidification of a cylinder 8 in. diam, 8 in. long would be 0.37 to 0.52 of the 55 min required for a long cylinder.

The only limitation holds for long and short bodies alike: the square law ratio is valid if all dimensions change simultaneously. This implies that the mold wall thickness (sand) for the 8-in. cylinder is four times as large as that for the 2-in. cylinder. In practice no such ratio prevails. But, fortunately, this is not significant if in either case the sand thickness is such that the temperature of the flask does not increase materially during solidification.

VEINING TENDENCIES OF CORES

A Progress Report by A.F.S. Sand Division

Committee 8-J

ABSTRACT

In addition to the previous conclusions the present work shows definite relationship between hot strength, hot deformation and veining tendencies of cores.

Introduction

This Progress Report covers further work by the Physical Properties of Iron Foundry Molding Materials at Elevated Temperatures Committee 8-J accomplished during 1952 on the occurrence of core veining in gray iron castings. The initial report on this project was presented at the 1952 Annual Meeting and is recorded in the A.F.S. TRANSACTIONS, vol. 60, pp. 218-222 (1952).

Test Cores and castings were made and inspected by members of the Committee at the University of Michigan, May 14-16, 1952. Laboratory tests were performed at the Harry W. Dietert Co., laboratory by the Committee and laboratory staff. The core sand mixtures selected for study are in Table 1.

Procedure

Test castings were made using the same practice as described in the 1952 Progress Report. Figure 1 shows the test casting with four cored holes. The holes are 1 in. square. Iron was poured at a temperature of 2650 F. Table 2 shows laboratory test results on the cores and also the appearance of the test castings.

Discussion of Results

Room Temperature Properties—The earlier report showed that veining tendencies increased sharply with green core hardness. No further relationship between room temperature properties and veining was discovered.

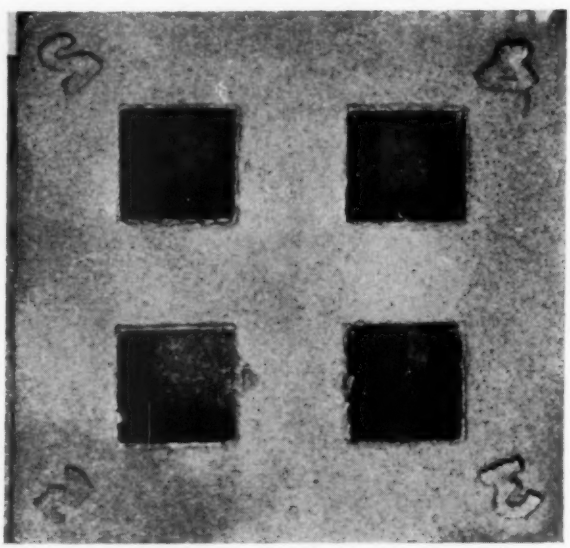


Fig. 1—Test Casting used in Veining Test Project.

TABLE 1—COMPOSITION OF CORE MIXTURES

Mix No.	5	7	9	10	13	16H	18.H	23.J	23.A	45.0	46.0	54.Q
Lake Sand, %						66.0	65.3	63.3	64.0	64.6	65.0	64.6
Bank Sand, %						33.0	32.7	31.7	32.0	32.4	32.5	32.4
100 Mesh Ottawa W&D, %	96.5	94.5	86.5	95.75								
No. 17 Ottawa W&D, %					49.0							
UofM Heap Sand, %					49.0							
Cereal Binder, %	2.0	2.0	2.0	2.0				1.0		0.5	0.5	1.0
Iron Oxide, %		2.0						2.0	2.0			
Silica Flour, %			10.0									
Western Bentonite, %				0.75								
Pitch, %					2.0							
Oilless Binder, %										2.5		
Resin, %											2.0	
A.F.S. Ref. Linseed Oil, %	1.5	1.5	1.5	1.5		1.0	2.0	2.0	2.0			2.0
Water, %	4.8	4.3	4.5	5.0	6.0	2.0	2.0	3.0	3.0	5.0	3.5	2.5

53-37

TABLE 2—BAKED AND HOT PROPERTIES OF TEST CORES

Table 2—Baked and Hot Properties of Test Cores

Mix. No.	5	7	9	10	13	16.H	18.H	23.J	23.A	45.O	46.O	54.Q
Baked Properties												
Wt. of Tensile Specimen, gm	108.0	108.0	107.5	107.5	106.0	100.9	101.5	104.0	103.0	104.3	101.2	103.3
Baked Tensile Strength, psi	405.0	387.0	437	261	31.5	45.9	113.8	276.4	160.0	55.5	42	283.5
Scratch Hardness, Top	93	93	93	84	33	42	78	89	82	48	29	94
Scratch Hardness, Bottom	93	93	93	87	31	49	82	90	80	55	50	92
Rotary Hardness, Top	92	91	91	82	26	35	77	86	78	47	5	88
Rotary Hardness, Bottom	92	91	92	83	12	40	81	88	78	53	47	87
Baked Permeability	20.8	21	18.7	22.4	149	88	89	82	90	83.5	99.5	83
Wt. of 2-in. x 2-in. Green Core, gm	173	173.5	173.5	173	170.5	160.9	162.0	168.0	165.5	167.0	162.0	164.6
Hot Properties												
Unaccommodated expansion @ 2000°F 5 psi load, oxidizing atmosphere 1 in. sq specimen, max esp in./in.	.0108	.0119	.0169	.0124	.0051	.0024	.0068	.012	.0103	.003	.002	.008
Collapsing Time @ 2000°F, 5 psi load Oxidizing atmosphere 1 in. sq specimen, min	5.45	∞	7.28	6.40	10.47	1.93	4.98	∞	8.75	8.52	0.82	5.83
Veining Factor = $\frac{\text{Time}}{\text{Max. Exp.}}$	505	∞	431	516	2052	804	732	∞	849	2840	410	728
Hot strength at 2500°F 3 min soak 1" sq ½" I.D. Hollow specimen own atmosphere, psi	76	20	151	81	53	2.25	32	6	7	67	9	88
Total Hot Deformation @ 2500°F 3 min soak 1 in. sq, ½ in. I.D. hollow specimen own atmosphere, in./in.	.0098	.0076	.0110	.0114	.0135	.029	.0124	.0209	.0194	.0150	.0048	.0099
Hot Toughness Hot str x hot def x 1000	745	152	1,662	9.24	7.16	65.3	397	125.4	135.8	1,005	43.2	871
Rate of Hot Deformation $\frac{\text{Hot Deformation}}{\text{Hot Strength}}$	1.29×10^{-4}	3.8×10^{-4}	0.73×10^{-4}	1.4×10^{-4}	2.55×10^{-4}	128.9×10^{-4}	3.87×10^{-4}	38.4×10^{-4}	27.7×10^{-4}	2.2×10^{-4}	5.3×10^{-4}	1.11×10^{-4}
Modulus of Rupture $\frac{\text{Hot Strength}}{\text{Total Hot Def.}}$	7.76×10^{3}	2.63×10^{3}	13.74×10^{3}	7.11×10^{3}	3.93×10^{3}	0.078×10^{3}	2.58×10^{3}	$.29\times10^{3}$	0.36×10^{3}	4.47×10^{3}	1.88×10^{3}	8.89×10^{3}
Spalling Appearance of first cracks, sec	20	10	12	14	35	50	17	30	15	23	25	20
Appearance at 2 min.	Vertical & Horiz. Cracks on 4 Sides	Vertical & Horiz. Cracks on 4 Faces	Ver. & Horiz. Cracks on 4 Faces	Vert. & Hor. Cracks on 4 Faces	Vert. & Hor. Cracks on 2 Faces	Center Collapsed Leaving ⅛ Shell	Vert. & Hor. Cracks on 3 Faces	Vert. & Hor. Cracks on 4 Faces	Moderate Vert. & Hor. Cracks	Vertical Cracks on 4 Faces	Vertical Cracks on 3 Faces	Vertical & Horizontal Cracks on 4 Faces
Veining Condition	Severe	None	Severe	Severe	Moderate	None	None	None	None	Moderate	Slight Not True Vein	Moderate & None

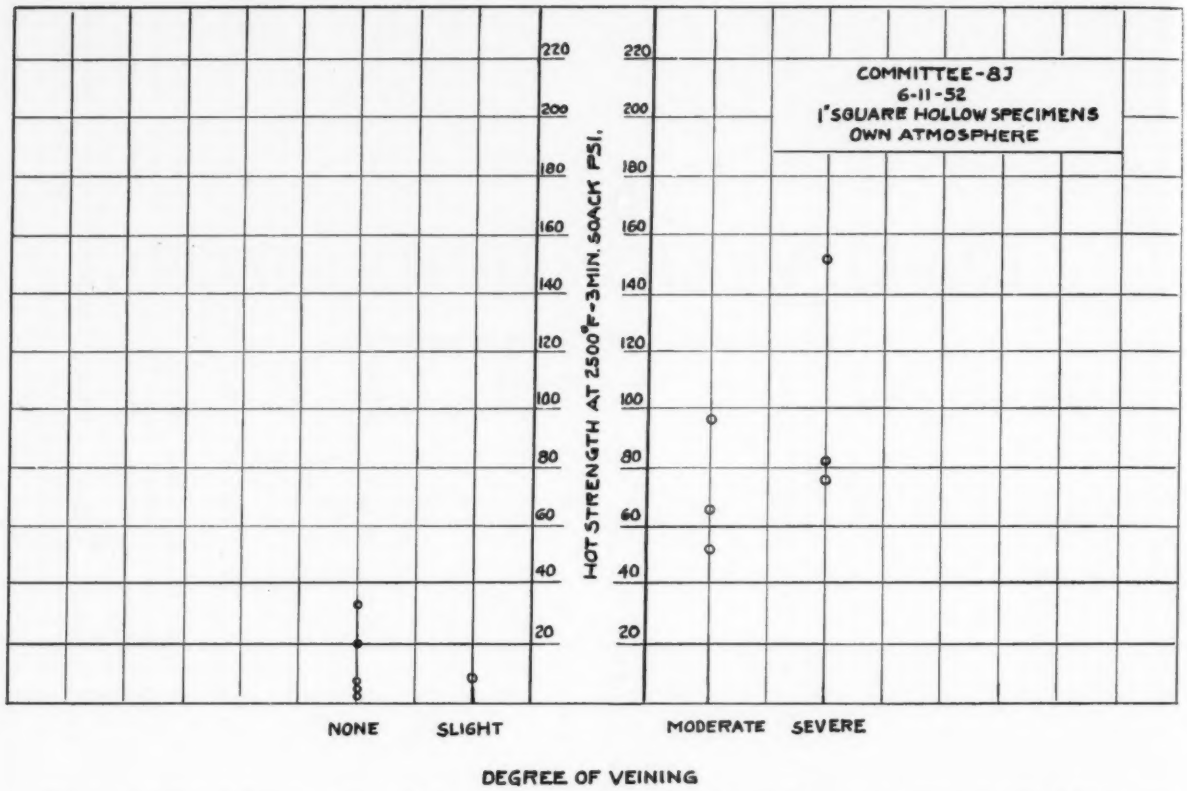


Fig. 2—Relation between Degree of Veining and Hot Strength at 2500 F in Own Atmosphere with 1 in. Square Hollow Specimen.

Expansion—Core specimens 1⅛-in. diam × 2-in. height were exposed to a temperature of 2500 F and changes in length were measured. The maximum expansion measurements are recorded in Table 2. There does not seem to be any correlation between the expansion reading and the defect.

When a mold is poured, the surface of the core is exposed to heat shock. When a specimen of core sand is tested, the heat shock is less. Also, in the expansion test, a solid mass of sand is measured. It is believed that if a thin layer of sand could be measured, its expansion and contraction due to heat shock would relate to the veining defect.

A further obstacle to accurate expansion measurements is the oxidation of the core during the test. When a casting is poured, air is excluded and the core does not oxidize.

Hollow Square Specimen—The cores used for high temperature tests were duplicates of the cores used in the test castings. They were 1 in. square, and were rammed by the standard A.F.S. sand rammer. In order to measure a surface layer or shell of sand, a hole ½ in. in diam was drilled through the cores before they were tested.

Collapsing Time—The cores were exposed to a temperature of 2000 F, a load of 5 psi and an oxidizing atmosphere. The time interval before core collapse was noted. Collapsing time did not appear to be related to the veining defect under oxidizing atmospheres.

Atmosphere—In order to prevent the core from burning up during the test, a ceramic hood was used for the hot strength and deformation tests. This made it possible to test the core in its "own atmosphere."

Hot Strength—It is apparent that the cores which did not produce veins were characterized by a low hot strength. This is shown graphically in Fig. 2.

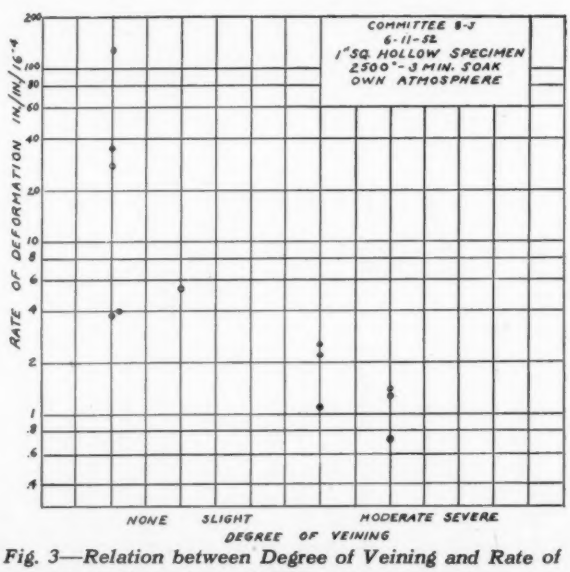


Fig. 3—Relation between Degree of Veining and Rate of Hot Deformation at 2500 F in Own Atmosphere.

TABLE 3—EFFECT OF VARIOUS SPECIMEN SHAPES AND CONDITIONS OF TEST ON CORRELATION WITH VEINING

Subject: Veining Sands
Investigation: To simplify the hot strength test that shows some correlation with veining

Data A — 1-in. Square, ½-in. I.D. Hollow Specimens; Oxidizing (furnace) Atmosphere; 3-min soaking period @ 2500 F

Mix No.	5	7	9	10	13	16-H	18-H	23-J	23-A	45-O	46-O
Hot Strength @ 2500 F, psi	none	38	17	none	12.5	none	none	3.5	8	1	—
Total Hot Deformation @ 2500 F, in./in.	—	.0078	.010	—	.0376	—	—	.0444	.0506	—	—
Degree of Veining	Severe	none	Severe	Severe	Medium	none	none	none	none	Medium	Slight
	No correlation										

Data B — 1⅛-in. Round Solid Specimens; Oxidizing (furnace) Atmosphere; 2-min soaking period @ 2300 F

Mix No.	5	7	9	10	13	16-H	18-H	23-A	45-O
Hot Strength @ 2300 F, psi	43	55	75	62	43	3.5	21	33	85
Total Hot Deformation @ 2300 F, in./in.	.0056	.016	.0185	.0116	.015	—	.0205	.0104	.0089
Degree of Veining	Severe	none	Severe	Severe	Medium	none	none	none	Medium*
				No correlation					

Data C — 1⅛-in. Round Solid Specimens; Own Atmosphere; 5-min soaking period @ 2500 F

Mix No.	5	7	9	10	13	16-H	18-H	23-A	45-O
Hot Strength @ 2500 F, psi	58	23.5	82	140	48	3	31	19	108
Total Hot Deformation @ 2500 F, in./in.	.009	.027	.021	.0172	.0285	—	.0295	.056	.0205
Degree of Veining	Severe	none	Severe	Severe	Medium	none	none	none	Medium

Below 40 psi Hot Strength the sand is free from veining defects

A 1-in. square specimen with a ½-in. center hole bored out was soaked at 2500 F for 3 min and broken. All cores having over 40 psi hot compressive strength produced veins and those having over 65 psi produced severe veining. Thus, to date, hot strength appears to be the best measure of veining tendency. Subsequent tests indicated that shape and size of specimen, soaking time, temperature and atmosphere conditions must be closely observed. Table 3 and Fig. 4 show that good correlation between veining and hot strength may also be obtained with 1⅛-in. × 2-in. solid round specimens by using a 5-min soak at 2500 F in "own atmosphere."

Hot Deformation—Sands with low hot strength usually are soft and plastic at 2500 F although not necessarily so. Total hot deformation did not show a relationship to veining. However, the rate of hot deformation which is the amount of "give" per pound of applied load did show reasonably good correlation as shown in Fig. 3.

Iron Oxide—Note in Table 2 that sand No. 5 produced severe veining. Two per cent of iron oxide was added to this sand and called No. 6 mixture. No. 6 sand did not produce veins. Silica melts at 3100 F (approx.) but Fe_2SiO_4 melts at about 2100 F.

The beneficial effect of materials which lower fusion temperature of sand have been mentioned by R. E. Morey and J. R. Kattus in their paper "Preventing Veining and Penetration on Castings made in Synthetic Sands," TRANSACTIONS of the American Foundrymen's Society, vol. 54, pp. 129-140 (1946).

Conclusions

The findings of the Committee to date on the causes and degrees of veining tendency follow:

A. Materials and Processes.

1. Impure sands decrease veining.
2. Iron oxide prevents veining.
3. Additives other than iron oxide may influence veining.
4. Excess binder promotes veining.
5. Type of binder may influence veining.
6. Hard ramming promotes veining.
7. Over-baking promotes veining.
8. High pouring temperature promotes veining.
9. Excessive moisture may promote veining.

B. Tests indicating Veining Tendency

1. ***Green Hardness***—Veining tendency increases sharply with increased green hardness.
2. ***Hot Strength***—Under described conditions of test, veining appears as hot strength rises above 40 psi.
3. ***Hot Deformation Rate***—Whenever the outer surface of a core becomes tacky or semiplastic as exhibited by a high or moderately high rate of hot deformation, veining tendencies decrease.

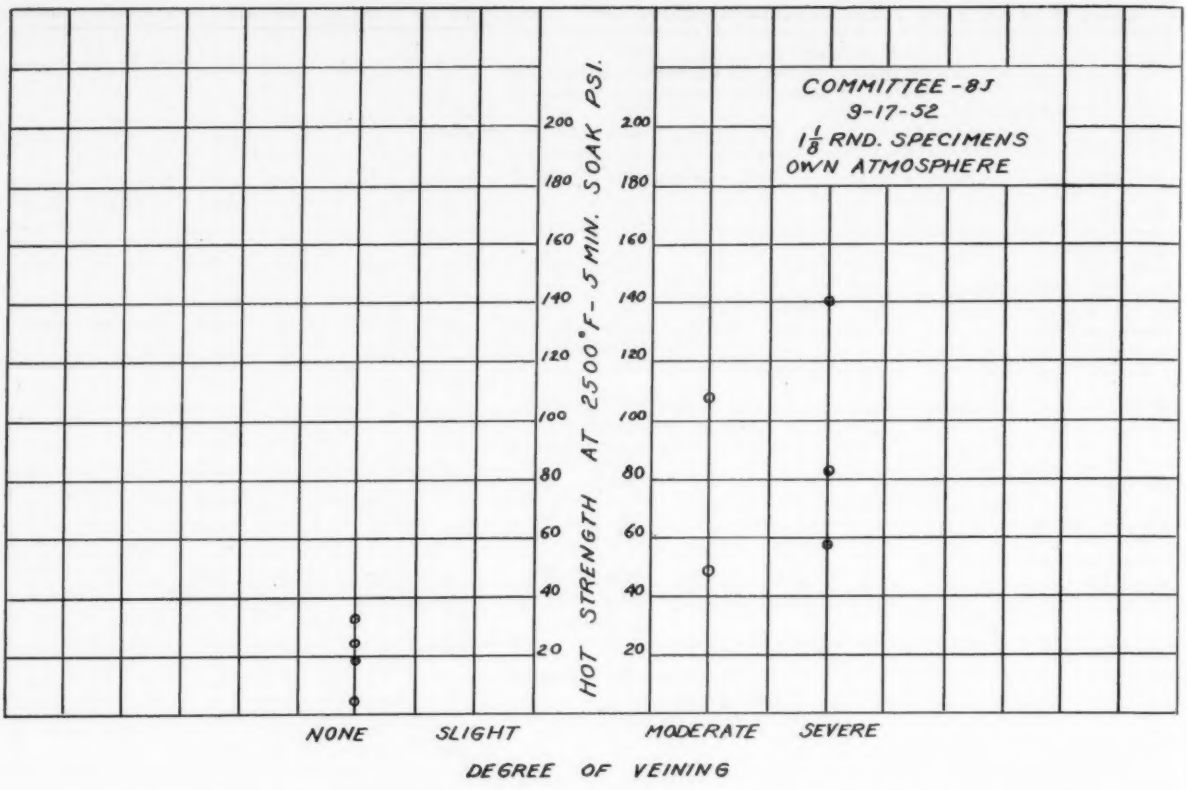


Fig. 4—Relation between Degree of Veining and Hot Strength using 1⅛ in. x 2 in. round Specimens.

There are sound scientific reasons to support the theory that veining is caused by expansion or contraction in the surface layers of the core and lack of hot plasticity on the surface.

To date, a best specimen shape and testing technique have not been discovered.

There is need for further research on the techniques of measuring thermal expansion and contraction of the core sand mixtures.

Acknowledgments

The Committee is indebted to the University of Michigan for making available its foundry facilities. Professor Wm. Spindler and the staff of the U. of M. Foundry were very helpful in aiding the members to mold, cast and clean the experimental castings.

The laboratories of the Harry W. Dietert Co. were used to carry out the core testing.

Committee Personnel

Members serving on Committee 8-J are as follows: H. H. Fairfield, *Chairman,* E. C. Zirzow, *Vice-Chairman,* W. A. Spindler, *Secretary,* R. W. Bennett, K. S. Brooker, H. W. Dietert, R. L. Doelman, J. A. Gitzen, R. L. Gollmar, R. A. Green, John Grennan, H. J. Jameson, J. D. Johnson, G. A. Kelley, Roy Korpi, R. W. Mason, A. E. Murton, E. J. Passman, V. M. Rowell, D. E. Shiels, E. W. Smith, Michael Warchol, G. R. Watson, R. E. Wilke, and E. C. Zuppann.

DISCUSSION

Chairman: E. C. ZIRZOW, Werner G. Smith, Inc., Cleveland.

Co-Chairman: H. W. MEYER, General Steel Castings Corp., Granite City, Ill.

Recorder: C. C. SIGERFOSS, Michigan State College, East Lansing, Mich.

D. C. WILLIAMS *(Written Discussion):*[1] The casting surface defect known as veining has long been a problem to foundrymen. Because this defect is located on the surface and before shakeout has extended into the molded mass it has been assumed that failure of the sand mixture occurred permitting the metal to run into the sand mixture. Veining has been somewhat of a riddle to foundrymen even though much time and effort has been spent on studying this defect from the point of view that everyone knows it is the sand mixture that has failed. Perhaps a word of caution as stated by Behar* "to answer her nature's riddles we must sometimes fly against the trickiest 'sense,' the tenth one, known as common sense or 'everybody knows'" is appropriate to this and many other foundry problems in spite of a background of 6000 years of casting manufacture.

It is not unusual therefore, that a committee of the Sand Division should elect to study the veining surface defect. It is hoped that this discussion can shed some light on the mechanism of vein formation.

The Committee report refers to the study on penetration and veining by Morey and Kattus, but apparently gave little consideration to the relation of "sweating" to veining and preferred to treat the problem from the sand point of view. The work of Morey and Kattus plus the recent studies on solidification by Pellini and co-workers at the Naval Research Laboratories should provide those interested in veining, scab-

[1] Associate Professor and Head of Foundry-Option, Department of Industrial Engineering, Ohio State University, Columbus, Ohio.

* M. F. Behar, *Instruments,* September, 1940.

bing, penetration and machinability with a fruitful basis for improvement in casting manufacture.

All the literature with the exception of the "sweating" described by Morey and Kattus implies that veining and scabbing take place as the mold cavity is being filled. This implication must be true if veining is due to expansion or contraction properties of the sand mixture. The conclusion of the committee's report contains the following paragraph. "There are sound scientific reasons to support the theory that veining is caused by expansion or contraction in the surface layers of the core and lack of hot plasticity on the surface." The report, however, does not describe these sound reasons and this writer would like to see them included in next year's report.

The important question still to be answered, in spite of the idea that most "everybody knows," is when did the vein or scab occur. A sound scientific approach will provide the answer and it is suggested that this time of vein formation become an early subject for committee study. E. C. Hoenicke** in discussing permanent mold gray iron casting before the Central Ohio Chapter in 1950 had slides which demonstrated the same kind of "sweating" that Morey and Kattus observed.

Figure 1 in the paper shows one of the test castings having veins in two of the four cavities. Since the test casting is a 4-in. cube section, final solidification around the corners of the test cores did not take place until about 30 min after pouring. It is thought that the occurrence of the veins happened just prior to that final solidification. The metal that penetrated the sand mixture should have a different composition than that of the casting as such material remained liquid and was extruded from the casting by the pressure of solidifying dendrites.

Since 1946 this writer has been interested in the mechanism of veining from the viewpoint that the extrusion or "sweating" was the important factor in the problem. Whenever the writer has been able, he had chemical analyses of the casting and vein made. Caution must be exercised in evaluating especially the reported vein analyses. The following are given only for the purpose of indicating that a vein and casting do differ.

(1)

	Casting Body	Vein
% C	3.40	1.00
% Si	2.57	4.04
% S	0.21	0.044
% P	0.09	0.233
% Mn	0.56	0.50

(2)

	Sample No. 1		Sample No. 2		Sample No. 3	
	Casting	Vein	Casting	Vein	Casting	Vein
% Mn	0.778	0.492	0.62	0.504	0.792	0.446
% S	0.046	0.057	0.08	0.092	0.079	0.097
% P	0.218	0.138	?	0.142	0.143	0.137

	Casting Body	Scab
% P	0.120	0.032
% Mn	0.45	0.43

If veins and scabs were produced during the pouring of the mold differences in composition should not occur. Much accuracy in sampling and analytical procedures will be necessary before too much reliance can be placed on analyses of scabs and veins.

Reference to Section 2 of Research Report No. 18 of the Technical Research Committee of the Steel Founder's Society gives information to show that solidification is retarded around cored cavities, such as illustrated in Fig. 1 of the 8-J Committee report. Recent studies (see "Solidification at Corner and Core Positions" by F. A. Brandt, H. F. Bishop and W. S. Pellini in this volume) indicate corners are hot spots. Using these studies for a basis of explanation it necessarily follows that if the low melting point components are to be extruded it will happen at the hot spots where the last solidification occurs.

Why did veining occur when core sand mixtures 5, 9 and 10 were tested? The quartz sand and silica flour used were of high purity. Quartz is known to fuse at 2700 F (see 8-J Committee report conclusion where 3100 F approximately is given, and this is true if you are using the cristobolite form of silica which is not present in foundry sands). The pouring temperature of the gray iron is stated to be 2650 F which is too low to bring about fusion within the cores molded from the above numbered mixtures. It is conceivable that had the pouring temperature been 2850 F or above the castings would have shown no veins where they occurred as indicated.

The problem is to produce a sufficient quantity of viscous glass which will resist the tendency of the extruded material from the casting to penetrate the molded sand mixture. In mixture No. 7, 2 per cent iron oxide was added and no veins were formed. This is reasonable for a length of time, 30 min, was available for the iron oxide to flux with the quartz at a sufficiently high temperature level to produce at least enough viscous glass which prevented the extruded material from penetrating the sand mixture. Also, it is conceivable that the same mixture would fail to prevent the extruded material from penetrating the sand mixture if the section size of the casting was halved, thereby reducing the time at elevated temperature levels and markedly lessening the quantity of viscous glass produced. It does seem a little presumptious for the conclusion of the report, to state that additions of iron oxide will prevent veining without qualification.

The report indicates that cores made from sand mixtures 16H, 18H, 23J and 23A did not permit the extruded material to enter the cores and form the vein defect. This would be expected because in the lake and bank sands there is sufficient material present to flux with the quartz to produce the required amount of viscous glass. Here also, the problem of time at elevated temperature levels is necessary. It is doubtful that for thinner casting sections the lake and bank sands are at high temperature levels long enough to permit glass formation of sufficient quantity.

This brings up the question about the kind of sand to be used. Is a high purity quartz sand desirable when used as in mixtures 5, 9 and 10? The affirmative is satisfactory if the pouring temperature is above 2750 F and the section thickness great enough. These same mixtures can be used by altering as was done in mixture No. 7. If veining occurs on thin sections it will become necessary to add an ingredient which produce the required quantity of viscous glass.

In conclusion it seems that the following recommendations are appropriate for the Committee 8-J. Further studies on veining should be made and along the following lines as well as others.

(a) Determine when veining takes place for various metal section sizes,
(b) Determine how much viscous glass is necessary to resist the pressure of the extruded material,
(c) Determine what materials should be added to mixtures in order to produce the viscous glass commensurate with section thicknesses.

** Eaton Manufacturing Co., Vassar, Mich.

REPORT OF A.F.S. FLOWABILITY COMMITTEE

By

Flowability Committee*

THE AIM AND WORK of the A.F.S. Flowability Committee during the past two years (1950-52) has been purposely restricted to developing a test simulating as closely as possible the conditions that prevail in the mold, or core surface when pressure is applied during ramming, squeezing, jolting, or blowing.

The foundryman has always been plagued with the inability of his molding materials to flow perfectly when force is applied to shape the mold, or core. One result of the molding material's inability to flow perfectly is the formation of void spaces in the mold surface that are not compatible with the particle size of the molding material. These voids can be quite large, as much as ½ in. in average diameter. These voids will be referred to as supervoids. For want of a better term, the ability of the molding material to form a uniformly compacted mold, or core surface will be called flowability.

The committee is fully aware that there are many manifestations of improper flowability. Swells and strains are two that are attributed to poor flowability. Conversely, poor flowability in conjunction with high green strength and green hardness, *may* cause large supervoids in a mold surface strong enough to withstand quite high hydrostatic pressure without deformation. That penetration can result is self-evident. This phenomenon is accentuated if the mold or core is baked. Too high flowability is possibly one factor in expansion scabbing. However, expediency has made it necessary to restrict work to the most basic phase, the evaluation and measurement of supervoids. These supervoids have, and are still causing chronic trouble in all cast metals due to metal penetration, resulting in rough surfaces and adhering sand. It is quite possible that these supervoids are the chief cause of penetration that is usually attributed to a failure of the sand itself.

At least four different tests[1,2,3,4] have been advanced to measure flowability. None have been universally accepted. There has always been a group, whose individuals have been opposed to any one of the tests. If the Committee can be taken as the cross-section of the industry, this opinion can be expressed as follows:

> Although there are proponents of all tests, the majority think that none of the tests are capable of measuring flowability. This objection was found invalid. The only valid objection to some of the older tests was that too small a difference in the values was equivalent to a relatively great change in the size of the supervoids.

A possible reason for this objection was found during a working meeting of the Committee. There are indications that the supervoids are a surface phenomena, that supervoids are accentuated at the surface and decrease greatly in size in the central portions of the sand mass. If these observations are correct, they explain why it has been difficult to differentiate gross variations in supervoids with previous tests. If supervoids are primarily a surface phenomena, they can vary greatly in size with a relatively small variation in the properties of the specimen as a whole.

It therefore seems imperative (at least at first) to measure the surface, not the specimen as a whole. Therefore, the Committee was agreed that a series of comparative standards, such as metal surface finish standards are necessary for a start. The Committee is fully aware of the difficulties and errors inherent in any visual test. Nevertheless, visual standards must be used for the primary evaluation. Control tests, either those tests now used, or new tests, can be calibrated to these primary standards. This method will give significance to the values obtained from control tests.

A series of visual flowability standards is shown in Figs. 1 and 2. The numbering system recommended is from No. 10 (highest flowability) to Number 1 (lowest flowability). Production sands will fall in somewhat the order shown in Table 1.

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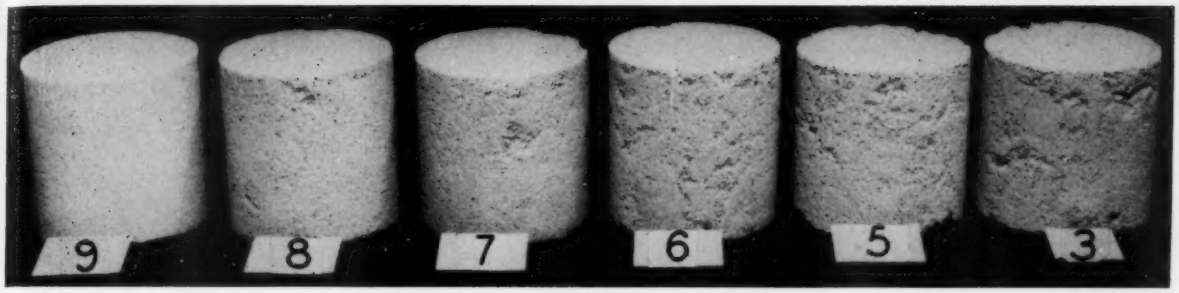


Fig. 1—Specimens from unriddled sand.

TABLE 1

Flowability No.	Occurrence of Supervoids	Typical Sand Mixes
10	None	Unbonded, washed and dried sand.
9	Trace of Supervoids in corner away from rammerhead	Oil-cereal core sand, less than 2.0 psi green strength. 3.0% water.
8	Small Supervoids in bottom ¼ in. of O.D. of specimen.	Core sand, or weak steel molding sand. (bentonite-cereal) 3.0-4.0 psi green strength, 3.0% water.
7	Small Supervoids in bottom third of O.D. of specimen.	Bentonite-cereal steel facing sand. 5-6 psi green strength. 3.0-3.5% water.
6	Supervoids occurring in about ⅔ of O.D. of specimen.	Bentonite-cereal steel facing sand. 8-9 psi green strength.
5	Larger Supervoids than in groups 6, or 7, occurring in at least ½ of O.D. of specimen.	Will occur in a sand mix like that shown in group 6 as water content is increased.
4	——	Strong dry-sand mixes will start in this group.
3	Large Supervoids in about all of O.D. of specimen except immediately below surface in contact with rammerhead.	Bentonite - fireclay - cereal dry sand facing sands with about 10 psi green strength.
2	Still larger Supervoids	Ladle linings, etc.
1	Limit	

Table 1 is to be used only as a guide. Slight variations in the clay-water ratio of most sands will cause a change of as much as three flowability numbers.

As exact supervoid size will vary depending on the type of base sand, a set of primary visual standards will be needed for each type of sand, mulling and handling practice. This means that each foundry must develop its own standards. It is hoped that a series of basic standards for various types of sands can be collected, but as this test is a shop test, specifically designed to measure shop conditions it seems possible that there must be individual shop standards.

An example is the great difference in supervoids and flowability due to riddling the sand into the specimen tube. The riddled and unriddled sands in Figs. 1 and 2 differ so greatly as to be not comparable. Any sand's flowability will vary so greatly with each foundry's sand mulling and handling system as to make universal standards questionable.

Table 2 summarizes the physical properties of the sands illustrated in Figs. 1 and 2, as well as others investigated by the committee.

Two correlations between new basic flowability numbers and those obtained from the Dietert-Valtier test are given in Table 2. A difference in basic flowability numbers from 8 to 3 correspond to a difference in Dietert-Valtiers numbers of 4 and 10 points respectively. The latter control test must not only be conducted precisely, so that a difference of one point is significant, but must be correlated to the basic flowability for each sand.

Recommended Procedure for Basic Flowability

The following test procedure is advanced as a preliminary test. The test has not been proven enough as yet to be even called tentative.

(1). **Sampling.** As this is a shop test, designed to measure flowability of the sand entering the mold, sampling is all important. If the sand is riddled onto the pattern, riddle the sand through the same size

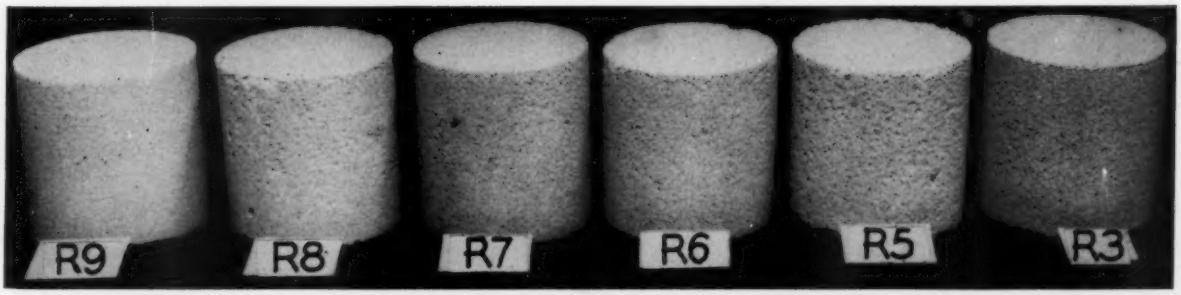


Fig. 2—Specimens from riddled sand. (These sands were riddled into a pan before filling specimen tube.)

TABLE 2

Sand	Fig. 1	Fig. 1	Fig. 1	Fig. 1	Fig. 1	Fig. 1	D1	D2	D2a	D3	AA1	AA2	AA2a	AA3
Flowability No.	9	8	7	6	5	2	8	6	5	3	8	6	5	3
% Water	3.2	3.3	3.6	3.6	5.3	4.5	2.2	2.7	3.4	3.0	2.1	2.7	3.5	3.1
% Western Bentonite	—	2.0	6.0	4.0	6.0	6.0	3.0	4.0	4.0	5.0	3.0	4.0	4.0	5.0
% Cereal	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0
% Core Oil	1.0	—	—	—	—	—	—	—	—	—	—	—	—	—
% Fireclay	—	—	—	—	—	4.0	—	—	—	—	—	—	—	—
Green Compressive Strength, psi	0.54	3.5	8.3	7.5	7.5	10.8	5.6	6.4	6.1	7.3	6.7	7.3	6.5	9.1
No. of grams, 2-in. specimen	—	—	—	—	—	—	152	152	154	151	160	162	164	160
Dietert & Valtier Flowability	—	—	—	—	—	—	75	74	74	72	75	72	69	66

screen into the standard 2-in. diam A.F.S. specimen tube. If the sand is shoveled into the mold, fill the specimen tube in the same manner. If the mold is filled from overhead hoppers, place the specimen tube and bottom plate under the hopper and fill in the same manner as the mold is filled. It is suggested that preliminary work to develop individual standards be done with both unriddled and riddled sand.

(2). **Weight of Sample.** Carefully remove surplus sand from the specimen tube to give a weight that will result in a 2-in. high sample, plus, or minus $\frac{1}{16}$ in. after compression. Place filled specimen tube and bottom plate on balance and tare out the tube and plate.

(3.) Place specimen tube, bottom plate and sand under standard rammer and *carefully* lower the 14-lb weight until the full weight rests on the sand. Allow weight to rest on sand for at least 1 min. *Do not allow weight to fall on sand. Do not ram.*

(4). Remove specimen tube from rammer, strip specimen from tube *without impact.* Compare supervoids on outside diameter of specimen with standard specimens. Classify from 10 for no supervoids to 1 for maximum size of supervoids. Figures I and 2 can be used as a guide. The specimens should be judged not only by the size of the supervoids, but also by the height of the area containing these supervoids from the end away from the rammer head.

(5). If the height of the specimen is not within $\frac{1}{16}$ in. of the standard 2-in. height, alter weight of sample to give the 2-in. height and repeat. The weight required is usually 25 to 30 grams less than the standard 3-rammed specimen.

Notes

The compression technique was selected rather than ramming because the supervoids are accentuated at this low level of compaction. Then too, the green hardness of the compressed specimens is comparable to that found in soft rammed areas of many molds. Sands will be classified in the same order of flowability at any ramming level, but the differences at higher levels is less pronounced.

Both the standard 2 in. diam and the $1\frac{1}{8}$-in. diam specimens, single and double end-rammed were tried. As the 2-in. diam specimen was as good as any, it was selected because of its wide use.

The rammer must be well lubricated and must slide freely. Friction and binding are more important than when the specimen is rammed. The specimen tube must be clean and not rusted and above all must have straight sides.

Acknowledgments

The Committee hereby acknowledges the help of the Canadian Bureau of Mines and Technical Surveys, H. W. Dietert Co., and University of Michigan for their help in conducting the tests.

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DISCUSSION

Chairman: J. B. CAINE, Foundry Consultant, Cincinnati.
Co-chairman: W. S. PELLINI, Naval Research Laboratory, Washington, D. C.
Recorder: P. E. KYLE, Cornell University, Ithaca, N. Y.

MEMBER: What riddle size would be used?

CHAIRMAN CAINE: The size normally used in your foundry.

CO-CHAIRMAN PELLINI: When the load floats is the time element important?

CHAIRMAN CAINE: Visible compression occurs up to 1 min and not much change is noted for times over 1 min.

D. C. WILLIAMS: [1] Has the work of the soil mechanics people been looked at where they establish the laws of movement of granular materials?

CHAIRMAN CAINE: No, not yet.

CO-CHAIRMAN PELLINI: Is this test sensitive to rate of loading?

MR. WILLIAMS: Yes, it is.

G. J. GROTT: [2] In some work on 12-in. diam cylinders it has been found that the faster the loading, the greater is the difference in hardness from top to bottom.

MEMBER: Can this test be correlated with molding with a slinger?

CHAIRMAN CAINE: This should be possible by filling the specimen tube with a slinger.

MEMBER: Has any study of binders been made?

CHAIRMAN CAINE: Not much has been done by the Committee. It will be necessary for each foundry to study the actual mixtures in use. It is not possible to generalize at this point. A series of tests should be run on each sand to establish your own standards.

[1] Ohio State University, Columbus, Ohio.
[2] Unitcast Corp., Toledo, Ohio.

DEFORMATION OF GREEN MOLDING SAND

By

W. G. Parker*

Molding sands possess an important physical property known as deformation. Present studies recognize two deformations—green deformation and hot deformation. This paper will attempt to cover only the work to-date on green deformation. Because today's sand studies deal with properties that are measurable, we can escape much of the guess-work heretofore experienced. The writer in his attempt to start at the beginning in this investigation may have overlooked work of importance that should be included. Any reference to data omitted will be most welcome.

The first printed works by Ries[1] and Moldenke[2] at the turn of the 20th century do not mention the word deformation but do refer to plastic qualities or the significance of such in molding sands. The first and most appraising paper given on green sand deformation was at the 1936 A.F.A. Convention by H. W. Dietert[4] and R. A. Dietert.[5] In the same and following years subsequent papers given at the A.F.A. Conventions by Dietert,[6] Valtier,[7] Dietert,[8] and Woodliff[9] made mention of green sand deformation.

The various aspects of sand additives with relation to the measurement of deformation by Dietert[10] and Woodliff[11] contains much valuable information. The following year brought out two fine papers dealing with the green sand deformation test by Buchanan[12] and Chadwick.[13] As part of the 1945 "Year-Round" Foundry Congress, a paper by Parker[14] was sponsored by the one-year old committee on Deformation, Foundry Sand Research Project, Technical Development Program of A.F.A.

Deformation Committee Formed

Because of the writer's interest in deformation and his many discussions on the subject with the late Dr. H. Ries,[1] then Chairman of the A.F.S. Sand Division, he was asked to become Chairman and to form the Green Sand Deformation Committee. In the beginning the Committee gained only little momentum in establishing the application of the deformation test in sand control. This was partially caused by the erratic results of the first models of the deformation accessory.

The writer found that consistent or checking results were not always obtained by the deformation accessory, then supplied by the manufacturer. This may be in part the reason for the lack of interest on the part of practical foundrymen in the early work on Deformation as part of sand control. The discrepancies in the mechanism were a "following along" of the arm of the Deformation accessory as the load was applied.

To correct this parallel travel, the writer designed a bracket and bearing arm freeing the Deformation attachment from the movable arm originally designed for attachment of the accessory. At the same time a magnetic tension stop was designed for the dial end of the accessory. The present model, (Fig. 1) as supplied by the manufacturer, has these refinements. Figure 1 illustrates a hand-driven model. A motor-driven model is also available and is preferred by the writer. The uniform rate of load on the latter removes the possibility of human error.

An indication of the test results now possible are shown in part by the data in Table 1.

The Deformation Test

Total Deformation, as expressed by the author, is in thousandths of an inch per 2 in. Formerly it was expressed in inches per inch of specimen length. However, for simplification the more popular version or the figure more often referred to is the total Deformation of the 2-in. length specimen.

Total Deformation is measured by calculating the decrease in the sand specimen length while measuring the green compressive strength on a separate unit known as the Deformation accessory. The unit in use becomes an integral part of the Universal Sand Strength Machine.

To make a green sand deformation test, the standard 2-in. sand specimen is used. The green com-

* Sand Technician, G.E., Elmira Foundry Co., Elmira, N. Y., also *Chairman*, A.F.S. Deformation Sub-Committee. Other members of this Sub-Committee are: P. E. Kyle, *Vice-Chairman*, Cornell University, Ithaca, N. Y.; G. W. Anselman, Vice-President and Gen. Supt., Beloit Foundry Co., South Beloit, Ill.; Robert Doelman, Miller & Co., Chicago; J. O. Ochsner, Foundry Chemist, Crouse-Hinds Co., Syracuse, N. Y.; and S. S. Phillips, Manufacturing Research Div., International Harvester Co., Chicago.

TABLE 1

Specimen	Weight, Grams	Moisture, %	Permeability	Green Strength, lb.	Deformation, (.001 in.)	STN	Avg. STN
			Sand A—4% Bond				
1	150	1.0	200	5.7	9	51.3	
				5.7	9½	54.2	52.7
			Too Dry—Difficult to handle				
2	146½	1.5	320	5.2	12	62.4	
				5.2	11¼	58.5	
				5.2	11¼	58.5	
				5.2	11½	59.8	
				5.1	12½	63.7	60.5
3	146	2.0	340	4.5	16	72	
				4.4	16½	72.6	
				4.4	16¼	71.5	
				4.8	14¾	70.8	
				4.5	15½	69.7	71.3
4	147	2.4	330	4.0	18¼	73	
				4.3	16½	70.9	
				4.4	16½	72.6	
				4.2	17¼	72.4	
				4.1	17	69.7	71.7
			Sand A—5% Bond				
5	148	1.5	320	7.4	10	74	
				7.6	10	76	
				7.4	10	74	
				7.4	10½	77.7	
				7.3	10¾	78.4	76
6	146½	2.0	350	6.0	14	84	
				6.0	14	84	
				5.9	14½	85.5	
				5.9	13½	79.6	
				5.9	14	82.6	83.1
7	148	2.5	350	4.9	16½	80.8	
				4.9	16	78.4	
				4.9	15½	75.9	
				4.7	15½	72.8	
				4.7	16	75.2	76.6
8	149	2.8	320	4.8	16¼	79	
				4.9	16	78.5	
				4.9	17	83	
				5	16½	82.5	
				5	16½	82.5	81
			Sand A—6% Bond				
9	152	1.5	230	8.8	9	79.2	
				8.0	10	80	
				8.0	10½	84	
				8.5	10	85	
				8.5	10	85	82.6
10	147	2.0	340	9.8	11¼	110.2	
				9.6	12	115.2	
				9.8	12½	122.5	
				9.8	12½	122.5	
				9.4	12½	117.5	117.5
11	146½	2.5	340	8.2	14½	118.9	
				8.2	14	114.8	
				8.3	13½	112	
				8.1	13½	109.3	
				8.2	13	106.6	112.3
12	148	2.8	340	7.3	15	109.5	
				7.3	15	109.5	
				7.3	16	116.8	
				7.2	16	115.2	
				7.3	15¼	111.3	112.4
13	155½	1.5	*Sand A—7% Bond* 160	9.5	9¼	87.8	
				9.9	9¼	91.5	
				8.9	9½	84.5	
				9.1	9¾	88.7	
				9.2	10¼	94.3	89.3
14	150	2.0	230	13.5	12¼	165.3	
				12.5	12½	156.2	
				12.7	13	165.1	
				13.2	12	158.4	
				13.1	12	157.2	160.4
			Note				
15	147	2.5	270	9.9	17¾	175.7	
				10.3	17	175.1	
				9.8	17½	171.5	
				10.3	16¼	167.3	
				10	16¼	162.5	170.4
16	149	3.0	270	8.6	17	146.2	
				8.6	17¼	148.3	
				8.6	18½	159.1	
				8.4	18	151.2	
				8.5	17½	148.7	150.7
17	150	3.3	250	7.6	18¾	142.5	
				7.4	19½	144.3	
				7.4	18½	136.9	
				7.6	18¼	138.7	
				7.5	18½	138.7	140.2
			Sand B—4% Bond				
18	150	1.1	130	6.4	9½	60.8	
				6.3	9	56.7	
				6.4	9	57.6	
				6.2	10	62	
				6.2	10	62	59.8
19	149	1.4	140	6.8	8½	57.8	
				6.4	9½	60.8	
				6.7	9½	63.65	
				6.3	9¼	58.2	
				6.3	9½	59.8	60
20	148	2.0	150	4.6	13¼	60.9	
				4.6	13½	62.1	
				4.6	13	59.8	
				4.5	13¾	61.8	
				4.4	13¾	60.5	61
21	149½	2.4	150	4.3	15¼	66.6	
				4.4	14¾	64.9	
				4.3	14½	62.3	
				4.3	15½	66.6	
				4.4	14½	63.8	64.8
22	150½	3.0	140	3.9	17	66.3	
				3.9	17	66.3	
				3.9	15	58.5	
				3.9	14½	56.5	
				3.9	16¼	63.3	62.1
			Sand B—5% Bond				
23	156	1.0	50	4.6	9	41.4	
			Very dry and difficult to handle—much unused bond visible.	4.6	9	41.4	
				4.6	8¾	40.2	41
24	150	1.4	130	7.9	10½	82.9	
			Dry and difficult to handle—small amount of unused bond visible	7.8	11	85.8	
				7.8	10	78	82.2
25	147½	1.9	150	7.8	11	85.8	
				7.7	11½	88.5	
				7.7	11	84.7	
				7.8	11	85.8	
				8.0	11	88	86.5
26	150	2.5	140	5.5	15½	85.2	
				5.4	16	86.4	
				5.2	16½	85.8	
				5.3	16	84.8	
				5.3	16	84.8	85.4
27	148	2.9	140	5.3	17½	92.7	
				4.9	18¾	91.8	
				5.4	16½	89.1	
				4.8	19¾	94.8	
			Much too heavy	5.0	19½	97.5	92.1

Key to Test Figures Shown in Table 1

Sand A—A.F.S. Grain Fineness Number 45				
Western Bentonite	4%	5%	6%	7%
Group	1-4	5-8	9-12	13-17
Sand B—A.F.S. Grain Fineness Number 60				
Western Bentonite	4%	5%	6%	7%
Group	18-22	23-27	28-32	33-37
Sand C—A.F.S. Grain Fineness Number 100				
Western Bentonite	3%	4%	5%	6%
Group	38-41	42-46	47-50	51-55
Sand D—A.F.S. Grain Fineness Number 145				
Western Bentonite	3%	4%	5%	6%
Group	56-59	60-63	64-67	68-71

TABLE 1 (CONT.)

Specimen	Weight, Grams	Moisture, %	Permeability	Green Strength, lb.	Deformation, (.001 in.)	STN	Avg. STN
			Sand B—6% Bond				
28	153	1.5	120	8.3	10	83	
				8.5	9¼	78.6	
				8.5	9¼	78.6	
				8.5	9½	80.7	
				8.2	9½	77.9	79.7
	Dry and difficult to handle—some unused bond visible						
29	149	2.0	140	11.4	12½	142.5	
				10.9	12	130.8	
				11.3	12¼	138.4	
				11.6	10¾	124.7	134.1
30	148	2.4	140	9.7	13	126.1	
				8.9	15½	137.9	
				9.8	13½	132.3	
				9.3	15	139.5	
				9.5	14	133	133.7
31	149	2.9	150	7.6	18¼	138.7	
				7.6	16½	125.4	
				8.0	16¾	134	
				7.7	17	130.9	
				7.5	17½	131.2	132
	Air in laboratory very dry—erratic readings due to samples drying						
32	150	3.3	140	6.1	21½	131.1	
				6.3	20	126	
				6.4	19½	124.8	
				5.9	22	129.8	
				5.9	22	129.8	128.3
			Sand B—7% Bond				
33	156	1.5	110	9.8	11½	112.7	
				9.8	11¾	115.1	
				9.3	11	102.3	
				9.3	11¾	109.2	
				10.5	10	105	108.8
34	150½	2.0	140	14	10	140	
				13.4	10½	140.7	
				13.3	10½	139.6	
				14.3	10	143	
				13.5	10½	141.7	141
35	148	2.5	130	12	12	144	
				11.8	13¾	162.2	
				12.3	13	159.9	
				11.4	14	159.6	
				11.1	15	166.5	158.4
36	148	3.0	150	11.1	15¾	174.8	
				10.8	16¾	180.9	
				11.2	15	168	
				10.8	16	172.8	
				10.9	16	174.4	174.1
37	151	3.5	120	9.0	20½	184.5	
				9.3	20½	190.6	
				8.9	21½	191.3	
				9.2	19¼	177.1	
				9.3	20	186	185.9
			Sand C—3% Bond				
38	161	1.0	36	5.6	9¼	51.8	
				5.6	9¼	51.8	
				6.8	8	54.4	
				5.4	9¾	52.6	
				5.5	9	49.5	52
39	155	1.4	55	7.4	11¼	83.2	
				7.9	10¾	84.9	
				8.6	10½	90.3	
				8.5	10½	89.2	
				8.6	10½	90.3	87.5
40	153	1.9	60	7.2	14	100.8	
				7.4	13½	99.9	
				7.5	13	97.5	
				7.3	12½	91.2	
				6.8	14½	98.6	97.6
41	154½	2.4	65	6.1	16	97.6	
				6.5	14½	94.2	
				5.7	17½	99.7	
				5.7	16½	94	
				5.7	17	96.9	96.4
			Sand C—4% Bond				
42	165	1.1	20				
	Too dry to handle—Much unused bond visible						
43	160	1.3	43	9.2	12	110.4	
				8.5	11¾	99.8	
				8.4	11½	96.6	
				8.9	12¼	109.0	
				8.4	11¾	98.7	102.9
44	155	2.0	57	9.7	14	135.8	
				9.6	14½	139.2	
				9.2	15	138.0	
				9.8	14½	142.1	
				9.8	14¼	139.6	138.9
45	154½	2.5	63	7.9	16¾	132.3	
				8.5	14½	123.2	
				7.9	16	126.4	
				8.1	16½	133.6	
				8.1	16	129.6	129.0
46	155	2.8	61	7.4	17½	129.5	
				7.4	16¾	123.9	
				7.5	16½	123.7	
				7.0	17½	122.5	
				7.1	17	120.7	124.0
			Sand C—5% Bond				
47	161	1.5	40	10.5	12¾	133.8	
				10.9	12	130.8	132.3
	Very dry difficult to handle—Unused bond visible						
48	154	2.0	56	12	16½	198	
				12	15¼	183	
				11.8	16½	194.7	
				12	15¼	183	
				12.2	15.5	189.1	189.5
49	153	2.4	54	9.8	17¾	173.9	
				11	14½	159.5	
				11.3	15½	175.1	
				10.5	16	168	
				11.0	15	165	168.3
50	154	2.9	56	9.3	16½	153.4	
				8.9	17½	155.7	
				9.3	15½	144.1	
				9.0	16¼	146.2	
				9.4	15½	145.7	149.0
			Sand C—6% Bond				
51	163	1.5	35				
	Too dry—could not get sample to hold together						
52	157	1.9	54	15.4	13	200.2	
				14.8	14	207.2	
				15.5	14	217	
				15.1	13½	203.8	
				14.6	14	204.4	206.5
53	152	2.4	59	15.4	14	215.6	
				15.5	13	201.5	
				15.4	14	215.6	
				15.5	13	201.5	
				15.5	13½	209.2	208.6
	Deformation readings doubtful—due to kick of pointer at fracture						
54	153	2.9	57	12.3	16½	202.9	
				12.9	16½	212.8	
				12.1	18.5	223.8	
				12.2	16¼	198.2	
				12.9	15¾	203.1	208.1
55	155	3.4	57	9.6	20½	196.8	
				9.9	20¼	200.4	
				10.1	20¼	204.5	
				9.9	21¼	210.3	
				10	20	200	202.4

TABLE 1 (CONT.)

Specimen	Weight, Grams	Moisture, %	Permeability	Green Strength, lb.	Deformation, (.001 in.)	STN	Avg. STN
			Sand D—3% Bond				
56	156	1.1	25	5.4	8	43.2	
				5.3	8½	45.0	44.1
	Very dry—could get only two samples to test. Unused bond visible.						
57	151½	1.6	37	7.5	12	90	
				7.5	12	90	
				8.1	10	81	
				7.9	10	79	
				7.6	11	83.6	84.7
58	147	2.2	49	6.8	12	81.6	
				6.4	13¾	88.0	
				6.7	12¾	85.4	
				6.5	13¾	89.3	
				6.6	13	85.8	86.0
59	147	2.4	52	6.1	15	91.5	
				5.9	14¾	87.0	
				6.0	15	90.9	
				6.2	14	86.8	
				6.1	14½	88.4	88.7
			Sand D—4% Bond				
60	156½	1.2	22	6.8	9	61.2	
	Readings doubtful as sample had crack—much too dry to handle.						
61	152½	1.6	3.9	11.6	12½	145	
				12.2	11¾	143.3	
				12.1	12	145.2	
				11.9	12	142.8	
				12	12	144	144.0
62	151	2.0	44	10.6	13	137.8	
				11.3	12½	141.2	
				11.3	12¾	144.0	
				11.1	12½	138.7	
				10.9	12¾	138.9	140.1
63	150	2.5	47	8.4	15½	130.2	
				8.3	15½	128.6	
				8.7	14¾	128.3	
				8.4	15	126	
				8.4	15¼	128.1	128.2
			Sand D—5% Bond				
64	156½	1.7	32	13.7	11¾	160.9	
				13.6	12	163.2	
				13.3	12	159.6	
				13.5	12	162.0	
				13.4	12	160.8	161.3
65	151	2.1	42	13.4	14	187.6	
				13.3	13½	179.5	
				13.2	14	184.8	
				13.2	13½	178.2	
				13.2	14	184.8	182.9
66	149½	2.4	47	12.4	14½	179.8	
				11.8	15	177	
				12.3	14¾	181.4	
				12.0	13½	162.0	
				12.3	14	172.2	174.4
67	150	3.0	49	9.3	17½	162.7	
				9.0	17½	157.5	
				9.4	16½	155.1	
				9.3	16	148.8	
				9.3	17¼	160.4	156.9
			Sand D—6% Bond				
68	160	1.6	28	14	11½	161	
				13.3	12	159.6	160.3
	Very dry—hard to get samples to hold together						
69	156½	1.9	38	15.9	12	190.8	
				16.8	13½	226.8	
				15.7	14	219.8	212.4
	Sand drying rapidly in dry laboratory air. Specimens cracking in stripping, hence only 3 readings						
70	151	2.5	46	14.9	14	208.6	
				14.7	14½	213.1	
				15.1	15	226.5	
				14.9	15½	230.9	219.7
71	152	3.0	49	10.7	20¾	222	
				11.9	18	214.2	
				11.9	17½	208.2	
				11.6	17¾	205.7	
				11.1	18½	205.3	211

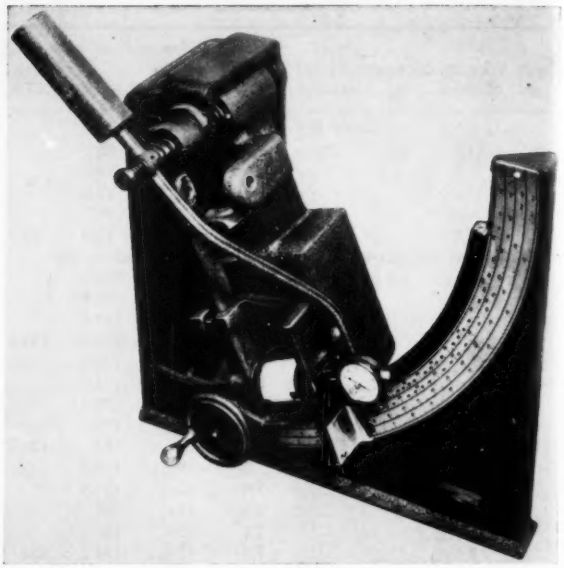


Fig. 1—Hand-operated deformation test machine.

pressive strength and the deformation test are made simultaneously. After placing the sand specimen in the holders of the machine, the deformation accessory is lowered to contact the weight arm. Adjustment is made on the trigger screw, also by turning the bezel dial of the indicator to bring the black pointer to zero setting. A slight pressure on the dial end of the accessory to make certain a firm contact between the sand specimen and test heads is generally necessary because of possible loose sand grains. The red indicator hand is set against the black hand. While load is applied and up to fracture of the specimen, proper tension on the indicator will show a constant movement on the indicator hand throughout the deforming of the specimen. Failure of the hand to keep traveling while pressure is exerted is due to lack of resistance at the indicator where contact is made on the arc of the machine. After collapse of the specimen, total Deformation is noted on the dial indicator by the red maximum hand

As previously pointed out, a competent operator will experience no trouble in getting checking results.

In a paper by Ries[15] reviewing several foreign methods of sand testing, it is interesting to note that two German investigators, Aulich and Lewerenz,[2] measured the deformation point by per cent, i.e. the pressure at which the deformation begins is expressed in percentage terms of the load required to break the test specimen under compression. Even in this early work Dr. Aulich states, "The compression test is the best one for strength, because it permits measurement of the deformation at the same time."

Nicolas,[17] in a recent article, refers to deformation values for controlling mold hardness. In the same article he also notes a reference to green plasticity used in the same vein of thought as deformation.

Figure 2 is an excellent example showing internal shrinkage caused by high green deformation. By courtesy of the H. W. Dietert Co. a description of the

conditions under which the test was made is given as follows: Lead castings were poured in flask equipment that had all the joints machined and the flasks were bolted tightly together between a machined cope and drag plate. The sand within the flask, which was round, was rammed with a special falling weight rammer so that both sands received exactly the same amount of ramming energy. The ingates were relatively small so that they would freeze off and set.

The writer for some time had entertained the thought that the first model of the autographic recording deformation accessory should be again explored. The manufacturer was contacted and has since provided a revised accessory, Fig. 3. With this accessory one is able to plot the stress curve while Deformation takes place. At this time of writing, work with the new recorder by the writer is not sufficient to warrant publishing data. However, the writer does feel that the work thus far is sufficient to recognize the new recorder as a valuable tool in research as well as routine sand tests.

Sand Toughness (STN)

The total deformation figure, when multiplied by the green compressive strength value gives a figure known as the sand toughness number. For example:

Green Strength, 8.5 lb. X deformation, 18 = STN 153.0

The early work on green sand deformation, when used in this manner used the word resilience to describe this test figure. The late Professor A. C. Davis,[3] Cornell University, asked that the A.F.S. sand committees drop the word resilience because it implied *rebound*. In its place he proposed the word toughness. His explanation was that the word toughness was a better description of the product of strength and deformation. Since that time the A.F.S sand committees have adopted the term sand toughness number.

Although the more significant use of sand toughness numbers or STN, as it is generally called, is for

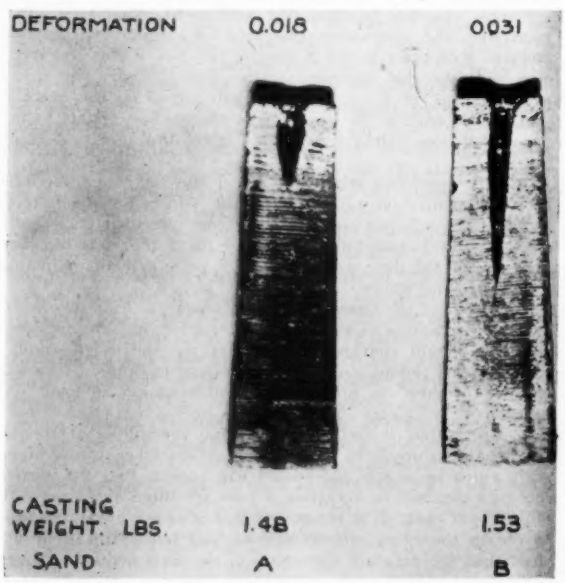


Fig. 2—Photo illustrating internal shrinkage caused by high green deformation.

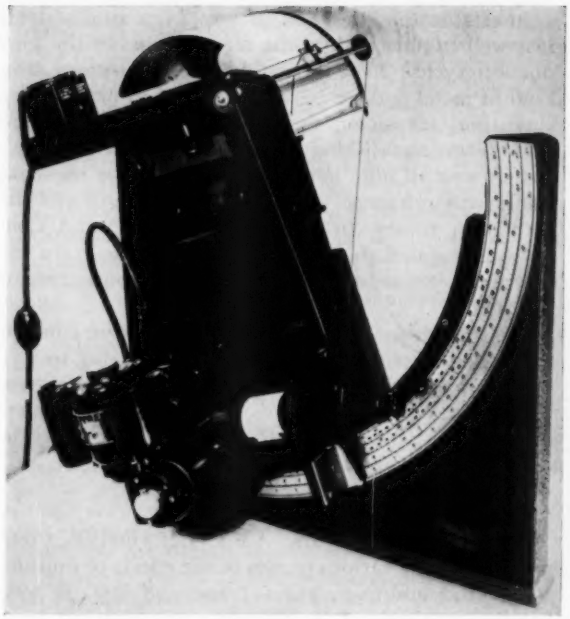


Fig. 3—Motor-driven deformation test machine with autographic recording deformation accessory.

simplification of control, all too frequently the other advantages of determining the STN are overlooked.

A considerable amount of work to determine whether or not sand mixtures with an STN of 150, for example, regardless of the type of sand, type of bond or other additives, will always give the same results. The work to-date indicates that in most instances the results will be parallel. However, sand mixtures have been found that prove otherwise. The writer has found that these sand mixtures were objectional for the jobs intended for many reasons. As grain shape, size, and screen distribution affect strength values as well as permeability, etc., deformation and the resulting STN's are also affected. It is, therefore, important to have a knowledge of materials to be used in your sand mixtures as well as the mixing cycle or mulling time necessary.

Foundrymen today speak of optimum moisture, yet very few think of optimum strength values or perhaps optimum bond content. A mistake often made among foundrymen is to consider an optimum moisture and a given green compressive strength as being all that is necessary for control purposes. Optimum moisture, as determined by bulk density, should never be overlooked. However, in sand control work, strength values as a function of optimum moisture, depend on the clay or bond envelope around the sand grains for development. The same is true for deformation values. Thus, a sand with an STN of 150 should have the correct moisture content and the right type and amount of bond addition. This does not mean, however, that the deformation value would be correct, that is, the amount of deformation best suited to the type of molding or the resulting castings. For this reason, it is necessary to recognize the amount of plasticity the various bonds will exert.

In establishing an STN for any given sand system, it is well to note each of the many factors in the production cycle. Type of gating, casting section size, kind of metal and temperature, as well as many other conditions are factors too often given little consideration when establishing a sand mixture.

It is well to note here the importance of the sand grain with reference to its effect on strength and deformation values. In an article by Ries[19] and Conant,[20] it is stated that "The character of the grain has an influence on the strength and flowing qualities of the sand."

Sand toughness numbers (STN), as used in control, have their first value in simplifying the day to day operation. The second and equally valuable use of STN is the guide it affords in equipping foundrymen with a working tool to make a better selection of their material at the sand mill.

Conclusions

Many attempts by the A.F.S. Deformation Committee to study various phases of the effects of mulling time, sand additives, grain shape and size, as well as other methods of Deformation measurement have been interesting. However, to make this work of value to the practical foundrymen, considerable more work needs to be done.

The test data shown in Table 1 is the beginning of a series of studies to note the effect of increasing moisture on physical properties over a range of bond percentages with four different sands.

The work, as initially proposed, will include other type bonds and various bond combinations. Also to be studied in the same manner is the effect of seacoal, etc. in deformation values.

Deformation and green compressive strength values vary with the quantity of bonding material added. The test figures shown in Table 1 indicate that at optimum moisture for each of the sands A-B-C-D, both deformation and green compressive strength increase with the increase in the percent of bond. However, deformation does not increase in a constant ratio with green compressive strength. Davies[18] gives a good description of this strength phenomenon in dealing with the clay envelope of the sand grains.

Sand control, as practiced today, recognizes each of the physicals that we can accurately measure. Some foundrymen, because of circumstances, place more emphasis on one or another of the physicals, often overlooking the others. For the more rounded program on sand control, each of the measured values have their place and should be considered.

Deformation values, as part of the control picture, give the foundrymen a two-fold test. First, is the effect other physical properties have on deformation, thus using deformation for a means to check off-color conditions. Second, is the part deformation has in calculating the sand toughness numbers.

Once established, sand toughness numbers are the best factor of control.

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DISCUSSION

Chairman: B. H. BOOTH, Carpenter Bros., Inc., Milwaukee.
Co-Chairman: H. W. DIETERT, Harry W. Dietert Co., Detroit.
Recorder: F. P. GOETTMAN, Standard Sand Co., Grand Haven, Mich.

D. C. WILLIAMS *(Written Discussion):*[1] 1. The author has done an excellent job, almost single-handedly, to provide the industry with a tool which he believes will permit the evaluation of sand mixtures.

2. Figure 2 in the paper proposed to show an existing relationship between deformation and shrinkage. It is not clear whether the author is referring to internal shrinkage or to external contraction. If it is internal shrinkage that is related to deformation the writer would like the author to expand on the aforementioned relationship.

3. This discussor has rammed and tested many sand specimens with the observation that most of the deformation that occurred took place in the portion of the 2-in. x 2-in. test specimen near the end opposite to the rammed end. The writer would like to ask the author if he can concur on this observation. Also, what is the reason why the author prefers to use the deformation values rather than strain values for calculating his S.T.N.'s.

4. This report is a committee report and the committee is to be complimented on starting where they did to investigate their problem. It is hoped that they will continue their good work. One important contribution made by the author was the completeness of the data presented. Such permitted an evaluation as will be later shown.

5. Any set of test data is always subject to at least two different interpretations. This discussor, on reviewing the data, has come to certain conclusions that may not necessarily conflict with those of the author. It is a difference of opinion that will lead to progress in understanding deformation in sand test specimens.

6. The author is justified in starting his studies at the 1.0 per cent moisture level. This discussor has taken the position, with which he is sure the author will concur, that the useable moisture contents in his series of tests are those of 2.5 per cent and 3.0 per cent. It is recognized that it is not always possible to obtain the exact content desired, and below it will be noticed that the data are regrouped in the increments mentioned above.

[1] Associate Professor and Head of Foundry-Option, Department of Industrial Engineering, Ohio State University, Columbus.

7. Reclassification of the data at the chosen moisture contents is shown below:

	% Bond	2.5% H_2O		3.0% H_2O	
		Density in Grams	Def. X1000	Density in Grams	Def. X1000
Sand A	4	147	17	—	—
	5	148	16	149	16½
	6	146½	13½	148	15½
	7	147	17	149	18
Sand B	4	149½	15	150½	15½
	5	150	16	148	18
	6	148	14	149	17
	7	148	13½	148	16
Sand C	3	154½	16	—	—
	4	154½	16	155	17
	5	153	16	154	16
	6	152	13½	153	16½
Sand D	3	147	14½	—	—
	4	150	15	—	—
	5	149½	14	150	17
	6	151	15	152	18½

8. The first thought that came from the above rearrangement was that the grain distribution of Sand C was different from the others and it also was thought that Sands A, B and C had about the same grain distributions. If the author would present the distributions the above questions could be answered. Based upon the above data it is interesting to note that regardless of sand grain fineness, water content of the mixture and the per cent of bond present there is a marked uniformity in density. Such a conclusion may not have been expected and it may be that the author can describe why the data indicate what is shown above.

9. The reclassification of data also indicates that regardless of moisture content, bond content and grain size distribution the deformation is practically a constant value. Plotting the data on a stress deformation curve produce a very interesting observation. Regardless of stress required to rupture the test specimen the deformation at rupture falls in a very narrow band. This is not the usual concept of the stress-deformation for materials. Maybe the author would give us an explanation for what appears to be happening.

10. One of the authors' conclusions indicated that the deformation varied with the amount of bond present in the mixture. This does not appear to be substantiated in the reclassified data in this discussion.

11. This discussor asked William H. Cox, a student in the Foundry-Option, to analyze the data by a mathematical procedure known as the analysis of variants. Such an analysis indicates that the moisture content of sand mixtures is by far the most significant of the three variables present, namely, moisture content, sand grain size and amount of bond in the mixture. Further, combinations of variables such as (1) sand grain size and per cent of bond used and (2) sand grain size and moisture content were found to be only slightly significant in contributing to a change in deformation.

12. Since it has been observed that at the moisture contents of 2.5 per cent and 3.0 per cent there is little difference in deformation and quite large variations in green compressive strength it could be concluded that the green compressive strength is as satisfactory as the S.T.N. for control. Would the author explain why he can combine a variable with a constant and obtain a value for control that is to be preferred to what he can obtain by using only the variable green compressive strength?

W. G. PARKER *(Written Reply to Mr. Williams' Discussion):* *Paragraph 2.* We have found in our work both internal shrinkage and external contraction due to excessive deformation in the molding sand. The example as shown in Fig. 2 is a good illustration of internal shrinkage in that consistency was maintained throughout the operation with the one exception, that being the deformation of the sand. Our experience with various sand mixtures on some types of grid castings have netted as much as a quarter of an inch differential in overall length due to the amount of deformation in the sand used.

In short the author believes that the stability of the mold wall is of utmost importance to control the shrinkage characteristics and that the deformation factor is a yardstick of measurement to best control this stability.

Paragraph 3. Mr. Williams is correct in his mention of where the deformation is greatest on the 2-in. x 2-in. test specimen, however, we are concerning ourselves with the total deformation for practical application in sand control work. It is quite possible to use the strain values for calculating the S.T.N., however, this would deal a more complex operation into the sand laboratory. No doubt the S.T.N. values would be different by this method. The author would be interested to see the results that might result from this angle.

Paragraphs 7 and 8. Mr. Williams has not interpreted the date in Table 1 as the author intended it. This no doubt is the author's fault in that too much of the information relative to the work to compile the table was left out of the report. Optimum moistures were not always in the 2.5 per cent to 3.0 per cent moisture brackets. With each of the four sands we found almost as large a differential in grain distribution as was found in the AFS Grain Fineness Numbers.

Paragraph 9. The author does not concur that Mr. Williams is correct that regardless of moisture content, bond content and grain size distribution that the deformation is practically a constant value. The author would like to point out that the clay-water mixture once having satisfied the demands of the sand grain area the strength factor, because of grain shape, size and distribution, is one thing, the ability of the sand to deform is another. The author found and concurs with the discussor that the deformation at rupture falls in a narrow band when the bond content as shown in Table 1 is also of close proximity.

Paragraph 10. The author wishes to add an addenda to the statement in the concluding section of the paper in reference to the fact that the deformation varied with the amount of bond present in the mixture and add that the type of bond also will vary the deformation.

Paragraph 12. It is not the author's belief that a variable and constant can be combined in any instance for control purposes. It is believed, however, that we have just as much of a constant in deformation as we have in green compressive strength with the moisture and all else of a sand mixture being constant. At best we know that moisture as a constant value is the dream of all foundrymen; strength and deformation will vary with the amount of moisture present in the sand mix. However, at usable moisture levels we recognize the effect on the two physicals in molding sand and have found the greater consistency in the Sand Toughness Numbers.

CO-CHAIRMAN DIETERT: We have taken sands from a number of shops, all making similar good castings and found that green deformation values were in a very narrow range, yet moisture varied considerably. This means that the sands were formulated in such a way that the green deformation was very close on all sands, therefore, green deformation can be used as a control test. If the gate freezes early and the mold enlarges then metal shrinkage results. Our experience shows that this type of internal shrinkage is eliminated by lowering deformation.

MR. WILLIAMS: Something else altered the change in the rate of heat transfer. This is the basic explanation for metal shrinkage of this type and not deformation.

C. A. SANDERS:[2] Changes in the sand formulation, density, heat conductivity and ramming, which is a prime factor, have a considerable effect in controlling metal shrinkage, therefore, one must consider all factors together and not just one or two.

[2] Vice-President, American Colloid Co., Chicago.

PROGRESS REPORT OF SHELL MOLDING MATERIALS TESTING COMMITTEE

By

G. A. Conger*

First meeting of the Shell Molding Materials Testing Committee (8-N) was held May 4, 1952, during the 1952 A.F.S. Convention in Atlantic City. Committee officers elected were Harry W. Dietert, Harry W. Dietert Co., Detroit, chairman, and G. A. Conger, Cambria Fdry. & Eng. Div., Stevens Mfg. Co., Ebensburg, Pa., vice-chairman and secretary.

Plan Property Evaluation Studies

The fundamental purpose and function of the committee were defined as "the determination and recommendation of methods and procedures for shell mold properties." To have access to broad facilities and information, committee members were selected from vendors and users of shell mold materials and castings.

The scope of the committee's activities was described as a series of property evaluation programs to be investigated by the committee as a whole. The following programs were enumerated (not necessarily in order of importance) as being of major current interest: (1) Reference Sand; (2) Cured Strength, Hot and Cold; (3) Mixing Procedure; (4) Cured Permeability; (5) Cured Deflection, Hot and Cold; (6) Sample Preparation; and (7) Hardness.

The first problem was the selection of a proper reference sand. Qualifications of the sand were enumerated and committee members, through independent investigations and study of pertinent data, selected Pennsylvania Glass Sand Co., Pittsburgh, as the producer of the reference sand. The sand, selected at the second meeting of the committee December 5, 1952, in Pittsburgh, is called "Tentative 8-N Reference Sand" and will be produced within the following tolerances:

Mesh	Between Sieves %	Between Sieves Tolerance	Cumulative %	Cumulative Tolerances
50	1.2	0.5–0.3	1.2	0.5–0.3
70	6.8	0.9–0.4		
100	17.0	1.4–1.1		
140	26.0	1.7–2.1	51.0	0.7–0.9
200	22.0	2.1–1.8		
270	18.0	3.0–2.1	91.0	1.6–1.8
Pan	9.0	1.8–1.6		

The sand is available in 100-lb bags marked with the sand name and production date.

Committee officers at the time of the second meeting were: G. A. Conger, chairman; T. L. Burkland, Deere & Co., Moline, Ill., vice-chairman, and J. B. Frysinger, Pennsylvania Glass Sand Corp., secretary.

Tensile Strength Important

Of greatest interest to members of the committee at the second meeting was the determination of tensile strength. A tentative testing procedure was outlined; deviations for comparative purposes and for possible test improvements were proposed.

As of February 1, 1953, the tensile evaluation program was in full operation. Tentative standards will be available in the late spring of 1953.

* Chairman, Shell Mold Materials Test Committee (8-N); Cambria Foundry & Engineering Div., Stevens Mfg. Co., Edensburg, Pa.

THE INFLUENCE OF MOLDING MATERIALS ON THE INCIDENCE OF HOT TEARING[1]

By

J. M. Middleton*

ABSTRACT

A technique that has been developed for the quantitative assessment of the hindrance to the contraction of a steel casting that is offered by various molding media is described. The effect of variations in bond content, in ramming density, and in grain size of the molding media, are indicated, and data relative to the range of temperature and to the time after pouring within and at which hot tearing occurs are presented. The trends indicated by the results obtained are applicable to castings of similar size to that employed in the test and to steels of carbon and sulphur contents different from those of the test casting. The work is being continued to determine corresponding information relative to larger castings whose greater heat capacity will affect the thermal conditions in the mold.

Introduction

It is a well-known fact that steels have practically no strength nor ductility at temperatures near to their solidus.

Should the resistance to thermal contraction offered by a mold be high at these temperatures, the stresses induced in the casting may exceed the strength of the steel and rupture or hot tearing will result. Different molding media, by reason of their chemical and physical compositions, have different strength properties at elevated temperatures, and may, therefore, be expected to offer varying degrees of hindrance to contraction while the steel is in the hot-tearing range.

Little information has been published upon the effect that different molding media have upon the incidence of hot tearing in steel castings. Some preliminary work in which the test castings were in the form of cylinders of various diameters and wall thicknesses has been reported,[1] the casting contraction being resisted by the cylindrical core and the tearing being located below the casting ingates. However, the results reported were inconsistent, tearing being associated with molding materials known to have low hot strength and not with materials known to have a high hot strength.

A large amount of work has been done in determining the strength properties of molding media at elevated temperatures, but few attempts have been made to apply this information to the solution of the problem of hot tearing. This is probably due to lack of information regarding the mode of tearing and to the thermal conditions existing in molds and castings after pouring.

The objects of the investigation were:

1) To evolve a test capable of measuring the amounts of hindrance to contraction offered by various molding media under casting conditions and the incidence of tearing in the casting.
2) To determine the range of temperature and the time after pouring during which hot tearing occurs, and the thermal conditions prevailing in the mold and casting during that time/temperature range.
3) To study the effects of variations in ramming density, bond content, and grain size of the molding media, and in the casting temperature of the metal, upon the incidence of tearing in the casting.

PART I—TEST METHOD

Apparatus

The test method which has been evolved embodies the following features:

1) The molding material to be tested is so included as to hinder the contraction of the casting.
2) The molding material is capable of being reproducibly rammed to any predetermined degree.
3) The design of the casting is such as to provide a hot spot where tearing may be initiated.
4) There are means by which the load induced in the casting by hindrance to contraction at any time after pouring can be measured.

The apparatus is shown in Figs. 1A and 1B. A 1 in. square section bar 21½ in. long is cast in a specially designed two-part molding box, and binds onto a notched steel bolt *A* which is previously placed in the mold cavity. The bolt is attached to a plunger which bears onto a flat calibrated spring, the spring being seated on two hardened steel rollers which act as fulcrums. The spring is so designed as to deflect 0.001 in. for each load increment of 10 lb, previous work having shown that restriction by a spring of this strength causes extensive tearing in a similar casting.

* Research Officer, British Steel Castings Research Association, Sheffield, England.

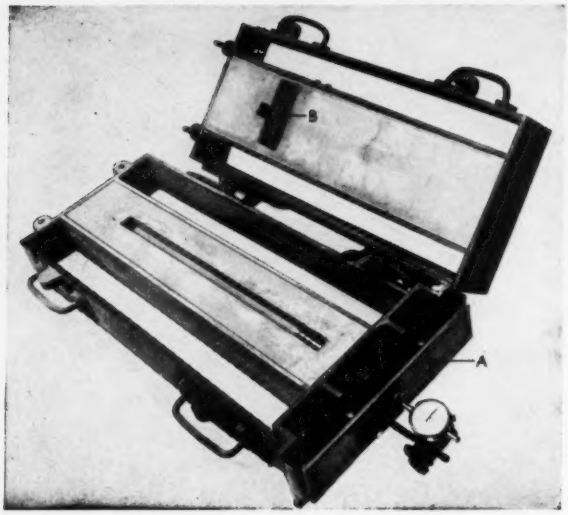


Fig. 1A—Photograph of the apparatus.

The bending moment of the spring is measured by a dial gauge that is graduated in 0.0005 in. The casting is fed by a 4x2½x1½-in. rectangular runner *B*, which is adequate to feed the 1 in. square section bar and to form a hot spot where tearing may be initiated. A rectangular-shaped block of the molding material to be tested is placed between the runner and a ⅝-in. thick steel plate that is bolted across the top part of the molding box, the plate extending to within ½-in. of the mold face.

The inclusion of this steel plate ensures that as the casting contracts, the movement of the runner is resisted solely by the test block and that variations in ramming density in the remainder of the mold can have no influence on the hindrance to contraction offered to the casting.

The solid contraction of the cast metal is, therefore, restricted both by the calibrated spring and by the test block; the casting, test block and spring form a closed stress system, in which the load induced in the casting by the restriction of movement of the runner by the test block is equal in magnitude to the load causing deflection of the spring.

If the action of the molten metal causes complete collapse of the test block no load is induced in the casting and no deflection of the spring can occur. If, however, the molding material has high strength at elevated temperatures, and therefore offers high resistance to movement of the runner, then the contracting casting will cause deflection of the spring.

The amount of deflection at any time after casting will depend upon the strength characteristics of the molding material and the rate of contraction of the casting. The rate of contraction of the casting will depend upon its casting temperature and can be kept constant from test to test.

If the resistance to contraction offered by the test block is high while the metal is weak, then tearing will occur at the hottest spot, i.e., at the junction of the runner and the bar.

Preparation and Testing of Test Block

Three base sands were used, Arnolds 26A, Arnolds 52A and Chelford medium, the gradings of which are given in Table I. All mixes were milled for at least 4 min in a 20-lb Fordath mixer. Western bentonite was used throughout the tests, except where noted differently.

In order to eliminate variations in ramming density, test blocks were made by pressing a known quantity of sand to a definite volume in a specially designed metal core box. The pressure needed for compaction was applied by means of a Denison compression testing machine, the load required for compaction being noted. The pressed blocks were 5½x4x2½-in. thick. At least three blocks were made from each mix, one to be included in the test casting, and the others for the purpose of determining their room-temperature properties.

A large number of blocks of this size gave good

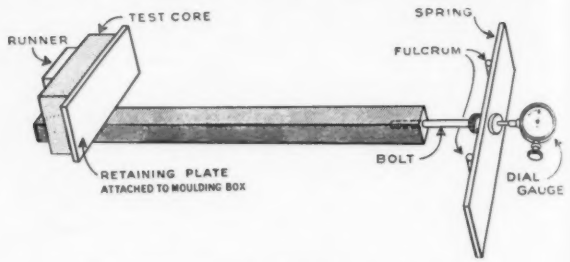


Fig. 1B—Isometric sketch of apparatus.

reproducibility of compressive strength and hardness test results, and therefore the strength of the blocks used to hinder contraction of the casting could be assessed. Drying of the test blocks where necessary was done in a laboratory oven, the clay-bonded sands being dried for 3 hr at 200 C (392 F) and the organic bonds for 1½ to 3 hr at 170 C (338 F) or 200 C (392 F) depending upon the nature of the bond.

Testing Procedure

The mold was rammed up with a thick facing of Arnolds 26A sand plus 3 pct bentonite and 2½-3 pct water, the test block being positioned in the mold immediately before pouring.

Melting was carried out in a 20-lb capacity high-frequency furnace, the melting conditions being held as constant as possible from one test to another. Deoxidation was effected by ferro-manganese and ferro-silicon and, finally, with aluminum at the rate of 2 lb per ton of steel.

Previous work[2] with a similar size of casting having shown that metal composition and casting temperature have a pronounced effect on the susceptibility of steel to tearing, the casting temperature in all tests (excepting those in which the effect of variation of casting temperature was investigated) was held at 1575 C (2867 F), and the metal composition was held within the following limits:

	Per Cent
Carbon	0.25-0.28
Silicon	0.38-0.45
Manganese	0.85-0.9
Sulphur	0.036
Phosphorus	0.032

All temperatures were measured with Pt/Pt-13 pct Rh immersion thermocouples in conjunction with a quick-reading potentiometer.

Spring deflection readings were taken immediately after completion of pouring, and subsequently at time intervals of 5 sec for a total period of 4 min.

All test bars were sectioned at the position of tearing, that is, through the junction of the bar and runner, and the sections were surface ground and etched for 7-10 min in a hot 1:1 HCl solution. The length of tear was measured in millimeters.

PART II—DETERMINATION OF TEMPERATURE CONDITIONS UNDER WHICH HOT TEARING OCCURS

Temperatures were measured by means of Pt/Pt-13 pct Rh thermocouples at three positions in the casting and five positions in the test block. The temperatures recorded are shown in Fig. 2, in which the curves may be identified with the following thermocouple positions:

Casting

a) Immediately below the runner at the position of tearing.

b) Towards the end furthest from the runner, 3 in. from the end of the insert bolt.

c) As near as possible to the center line of the bar and beneath the runner.

Test Block

d) At the metal/sand interface between the test block and runner, the single length of silica quill and thermocouple being so arranged along the test block as to protrude slightly into the mold cavity.

e) f) g) h) The thermocouples were placed so that their bare junctions were at the center line of the block and at various distances from the interface. The exact distances were determined by carefully breaking up the core after casting.

At positions a) and b) bare thermocouples were used and were so arranged as to protrude slightly into the mold cavity. By this means skin temperatures

TABLE 1

U.S. Standard Sieve Series	B.S.I. Sieve No.	Coarse (52 A)	Medium (Chelford)	Fine (26 A)
	8	0.0	0.0	0.0
6		0.0	0.0	0.0
	10	0.0	0.0	0.0
12		0.0	0.0	0.0
	16	1.0	0.1	0.6
20		2.5	0.0	1.5
	22	4.8	0.2	0.5
30		10.0	1.0	0.7
	30	27.7	1.5	1.2
40		52.5	4.5	1.8
	44	52.5	11.1	3.6
50		30.0	22.5	4.0
	60	11.5	30.4	6.7
70		3.5	43.0	11.5
	72	1.0	27.7	6.9
100		0.8	20.6	31.5
	100	0.8	20.6	31.5
140		0.4	7.2	39.8
	150	0.4	7.2	39.8
Pass 140		0.3	1.2	9.2
	Pass 150	0.3	1.2	9.2

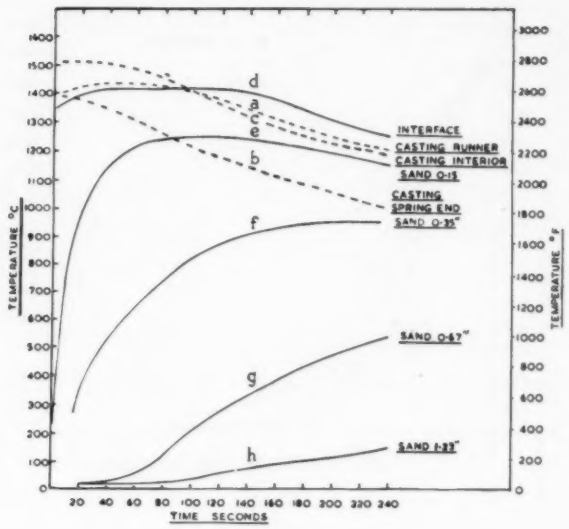


Figure 2

of the casting at these positions were recorded. The thermocouple at c) which measured the temperature existing in the interior of the casting at the position of tearing consisted of a single length of silica quill bent in the form of a U, the two thermocouple wires being butt welded and forming a junction at the center of the U.

This construction ensured that at least 1½ in. of the thermocouple was in contact with hot metal, thereby reducing any errors which might otherwise have arisen as a result of conduction. The actual position at which temperatures were measured was determined by carefully sectioning the casting when it was found that the thermocouple had moved slightly and was along the vertical center line of the bar about 0.35 in. from its upper surface.

It will be seen from curves a) and b) of Fig. 2 that a considerable temperature gradient existed along the skin of the casting from the runner to the opposite end of the bar; temperature differences of 65 C (149 F) and 180 C (356 F) being recorded 30 sec and 75 sec after completion of pouring, and the runner forming an appreciable hot spot.

The maximum interface temperature attained [1431 C (2608 F)] at the position of tearing was below that of the solidus of the steel [approx. 1470 C (2678 F)], but near the center of the section of the bar at this position, curve c), a time of 70 sec elapsed before the temperature fell below the solidus of the steel. Curve c) shows an arrest at 1510 C (2750 F), i.e., the liquidus of the steel, which was complete 30 sec after pouring. The sand/metal interface, curve d), quickly reached a temperature of 1420 C (2588 F) and remained above 1400 C (2552 F) for a period of 140 sec.

The marked temperature gradients that existed in the test block are shown more clearly in Fig. 3, but it will be noted that 55-75 sec after pouring a 0.1-in. depth of sand was heated beyond 1300 C (2372 F) and a depth of 0.05 in. was heated above 1350 C (2462 F). The strength of this depth of heated sand must largely control the amount of resistance that the

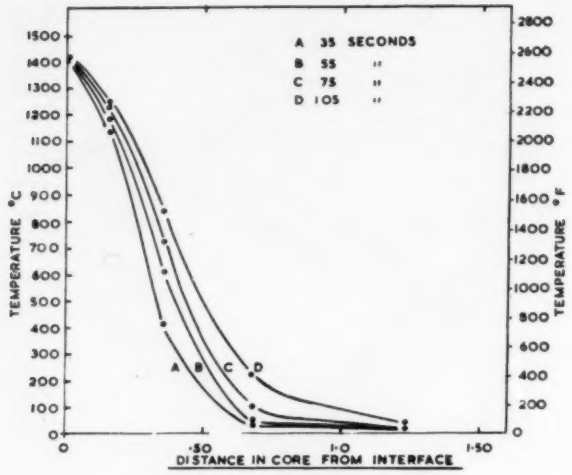


Figure 3

test block can offer to the movement of the runner, and therefore the amount of stress induced in the casting during the first few moments after pouring when tearing is most likely to occur.

Temperature of Testing

Previous work[2] on a casting having similar design has shown that tearing occurs at temperatures between 1400 and 1420 C (2552 and 2588 F) during the first 55 to 80 sec after pouring, depending upon a) the degree of hindrance to contraction offered by the mold or core, b) the casting temperature, and c) the composition of the steel. Work by Bishop and co-workers[3] puts forward evidence to show that tearing is initated at temperatures near to the solidus, when a small amount of liquid remains unfrozen, and suggests that the tears are propagated through these weak fluid films.

In order to ascertain when tearing takes place in the casting investigated, a cast was made in which the contraction of the casting was resisted solely by the spring, that is, the runner end of the casting was held rigid. The results are shown in Fig. 4.

The casting tore completely (Fig. 5) with a load of 55 lb 55 sec after pouring. At the position of tearing the temperature at the skin of the casting was 1431 C (2608 F), and in the interior of the casting it was 1488 C (2710 F). This latter temperature is approximately 20 C (68 F) above the solidus of the steel, which means that at the time of tearing at this position there would be approximately 16 pct of liquid steel in contact with 84 pct solid.

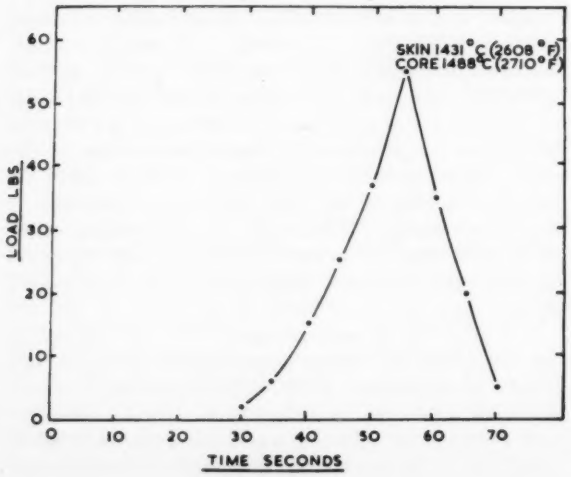


Figure 4

This supports the hypothesis of Bishop and co-workers[3] that tearing can be initiated while liquid metal is present. However, tearing does not occur at one particular temperature, but can be initiated over a temperature range, from the point at which the freezing metal forms a coherent mass down to the temperature at which the last liquid freezes, or when the metal quickly attains strength or ductility.

Cooling curves taken on the casting being studied have shown a) that some liquid metal is present in the casting for at least 70 sec after pouring, and b) that the first signs of solid contraction in a freely contracting bar occur 30 to 35 sec after pouring. The period from approximately 30 to 70 sec after pouring is, therefore, the hot-tearing range for this particular casting under the experimental conditions of steel composition and pouring temperature that were employed.



Fig. 5 — Complete tear in casting in which the movement of the runner was restricted.

In the subsequent experiments, reported in Part III, the loads that were induced in the casting by hindrance to contraction at 55, 75 and 105 sec after pouring have been used for comparison, it being considered that these readings represent the time range in which hot tearing can occur.

The correlation obtained between the load on the casting 75 sec after pouring and the extent of tearing in the casting is shown in Fig. 6.

PART III—EFFECT OF VARIABLES UPON THE INCIDENCE OF HOT TEARING

A number of preliminary tests were made to determine the degree of reproducibility of results from one test to another, and Fig. 7 shows repeat-test curves of the loads induced in the casting by hindrance to contraction at different times after pouring. The additions made to the sand mixes employed in these preliminary tests were: a) 5 pct bentonite and 20 pct silica flour, b) 1.5 pct linseed oil, and c) 5 pct bentonite. The degree of reproducibility of results shown by the curves in Fig. 7 is considered to be satisfactory.

Effect of Ramming Density of Green and Dry Molds

Test blocks composed of Arnolds 26A sand with 5

pct bentonite and 3 and 5 pct moisture were pressed to various densities, and the effect of ramming density in the green and dry state on the load induced in the casting at different times after pouring is shown in Figs. 8 and 9. The curves show that for identical densities dry sands restrict the contraction of the casting more than green sands and so induce higher loads in the casting. The amount of load induced in the casting at similar time intervals after pouring increases as the green or dry density of the sand compact is increased, and the load is induced earlier after pouring with the higher density compacts.

Figure 8 shows that it is possible for a green-sand compact having a high ramming density to induce higher loads in the casting than a dry-sand compact having a lower density, even though the dry sand has the greater compressive strength at room temperatures.

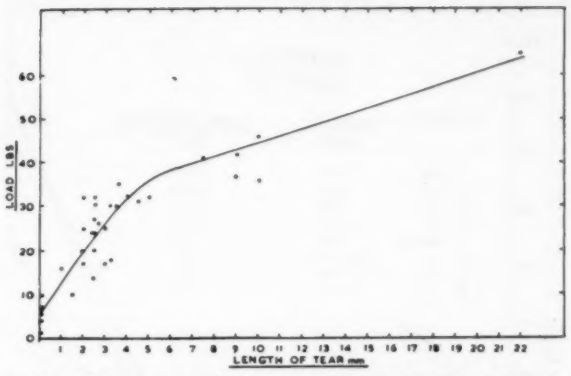
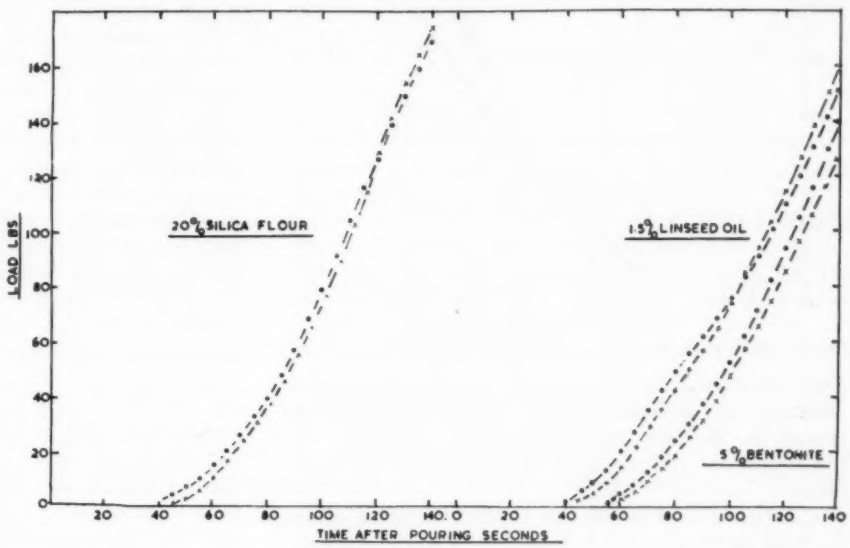


Fig. 6—Hindrance and hot tearing 75 seconds after pouring.

Fig. 7—Reproducibility of hot tearing test was checked as shown here before investigation of variables was started.



The load values taken from the curves (Fig. 9) at time intervals of 55, 75, and 105 sec after pouring were plotted against the respective compressive strengths and densities of the compact and are shown in Fig. 10. It can be seen that as the density, and therefore the compressive strength, of green- and dry-sand compacts increase, the compact offers more resistance to contraction and induces higher loads in the casting. It is noticeable that with green-sand compacts no load is induced in the casting 55 sec after pouring, and only a slight load (max. 5 lb) is induced by dry-sand compacts.

The loads due to hindrance to contraction by green- and dry-sand compacts cannot be plotted on the same curve, because in some instances higher loads are associated with the green-sand compacts, even though the dry-sand compact has the higher compressive strength. For example, 75 sec after pouring a green-sand compact (1.52 g/cc density) having a compressive strength of 10.2 lb/sq in. induces a higher load in the casting than a dry sand (1.31 g/cc density) with compressive strength of 62.0 lb/sq in.

The amount of load induced in the casting at any

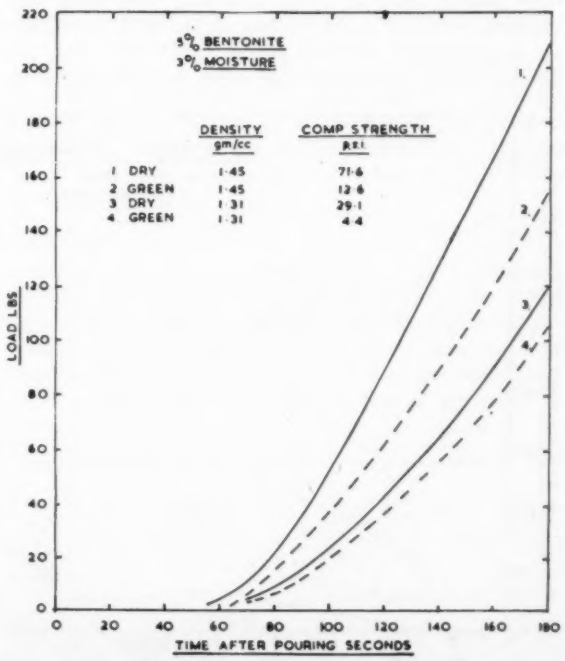


Fig. 8—Effect of ramming density of green and dry sands.

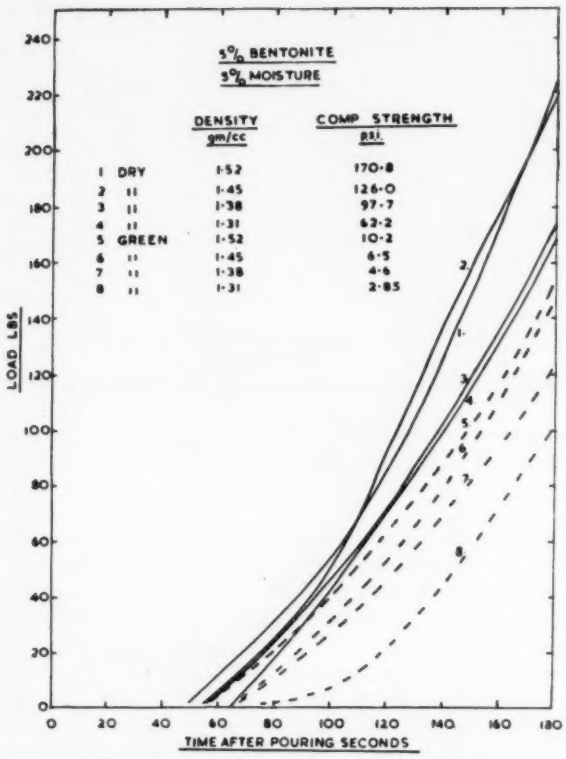


Fig. 9—Influence of density with higher bentonite.

time after pouring is governed by the amount of hindrance to movement of the runner that may be offered by the test block and must, therefore, be controlled by the strength properties of the test block.

A depth of sand in the test block, depending upon the time after pouring, is heated to a high temperature [above 1300 C (2372 F)] and will have low strength, while a much greater depth will be heated to temperatures beyond 100 C (212 F). For example, 75 sec after pouring, sand to a depth of at least 0.65 in. is at a temperature above 100 C (212 F). This depth of sand (0.65 in.) if of similar density should then have identical strength properties irrespective of whether it was originally a dry- or green-sand mix, and higher strengths in this depth of sand should be associated with higher density compacts.[4]

The strength of the cooler portion of the test block will also be effective in influencing the hindrance to contraction offered by the block. If it is very weak it will deform along with the hot portion of the block, and therefore the test block as a whole will have low resistance to contraction. However, if the cooler portion is relatively strong, it is possible that the contraction of the test bar will be resisted mainly by that portion of the test block which is at a high temperature, and therefore weaker.

The amount of hindrance to the contraction of the casting that is offered by the test block will thus depend upon the relative strengths of the hot surface layers and the cooler interior of the test block. A sand compact having moderate room temperature strength, but capable of developing a high hot strength, will restrict the contraction of the casting more than a sand compact having a high room temperature dry strength but a low hot strength. This will be especially so during the first few moments after pouring, when high temperatures are attained at the surface of the test block and when the amount of contraction which the casting can undergo is small.

Sections taken through the junction of the runner and the bar showed that small tears were present in only those castings that were restrained with the higher density (1.52 g/cc) green- and dry-sands compacts (Figs. 11 and 12). The castings restricted with those green- and dry-sand compacts having lower densities were free from tearing at this position, a typical example being shown in Fig. 13.

Effect of Amount of Bond

To determine the effect of variations in the amount of clay bond, a series of tests was carried out using

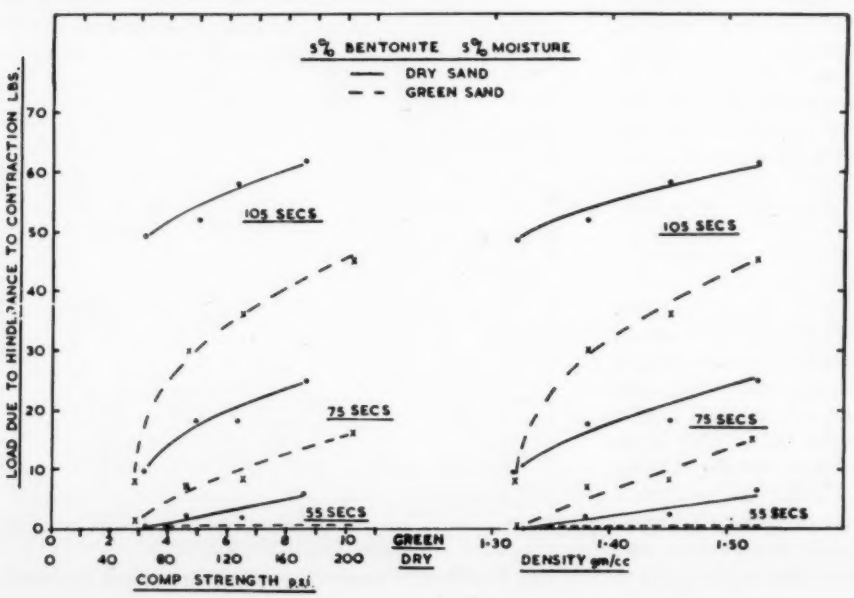


Fig. 10—Resistance to contraction of casting is higher the greater the density and compressive strength for both green and dry sand. Data are replotted from Fig. 9, on this page.

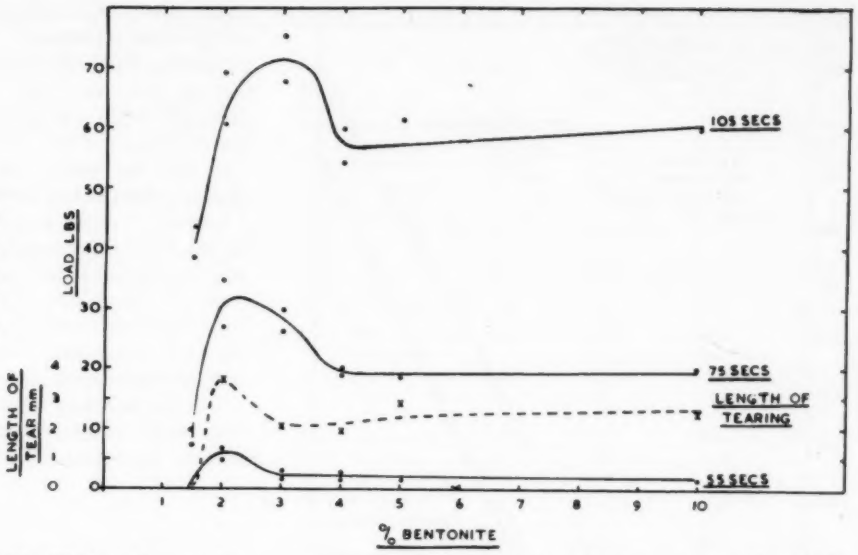


Fig. 14—Greatest resistance to contraction, measured by tests reported here, came at bentonite contents of 2 to 3 per cent. Increased bentonite reduced the resistance but not to low level found at 1.5 per cent.

test blocks composed of Arnolds 26A sand containing different amounts of bentonite from 1.0 to 10.0 pct. All blocks were pressed to the same density, 1.45 g/cc, and the moisture content was held constant at 3.0 pct with the exception of the 10.0 pct bentonite mix, which contained 5.6 pct moisture. The blocks were tested in the dry condition, and the details of compressive strength, are given in Table 2.

The curves in which the loads due to hindrance to contraction are plotted against percentage bentonite additions, at time intervals of 55, 75 and 105 sec after pouring, are shown in Fig. 14.

As can be seen from the curves, higher loads, and therefore greater resistances to contraction, are associated with the blocks containing 2.0 to 3.0 pct bentonite. An increase in bentonite from 3 to 4 pct causes a lowering of the test block's resistance to contraction, but further increases to 5.0 and 10.0 pct have little effect. The test block containing 1.5 pct bentonite offers the lowest resistance to contraction in the range investigated.

Also included in the graph (Fig. 14) is a curve showing the extent of tearing associated with each percentage addition of bentonite. It will be noted that the longest tears are found in the casting restricted by the block containing 2.0 pct bentonite, and that no tears were found in the casting held by the test block containing 1.5 pct bentonite.

The reason for this maxima in the load and tearing curves at 2.0 and 3.0 pct bentonite is not apparent. It may be due to differences in the distribution of the bond with varying bentonite contents, but further work is necessary before a satisfactory explanation can be given.

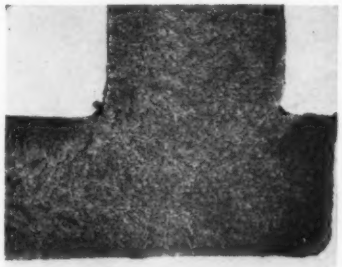


Fig. 11 — Tear in casting restrained by green sand test block rammed to a density of 1.52 g/cc and bonded with 5 pct bentonite.



Fig. 12 — Tear in casting restrained by dry sand test block rammed to a density of 1.52 g/cc and bonded with 5 pct bentonite.

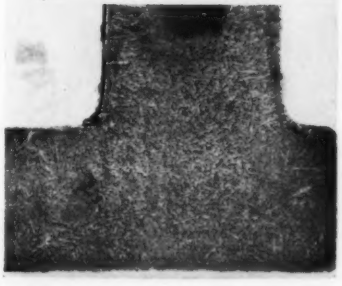


Fig. 13 — Tear-free casting restrained by a dry sand test block rammed to a density of 1.38 g/cc and bonded with 5 pct bentonite.

Effect of Sand Grain Size

Tests were carried out on three sands having different grain sizes (details of gradings given on Table 1), the mixes containing 5 pct bentonite and 3.0 pct moisture. The blocks were pressed to densities that resulted in similar strengths in the dry condition, and the details of compressive strengths are given in

TABLE 2

	Test Blocks					
	I	II	III	IV	V	VI
Bentonite, %	1.50	2.0	3.0	4.0	5.0	10.0
Moisture Content, %	3.0	3.1	3.1	3.3	2.8	5.6
Load for compaction, psi	231.0	191.0	145.1	134.4	121.0	42.2
Dry strength, psi	64.0	77.5	77.8	97.8	71.6	111.0

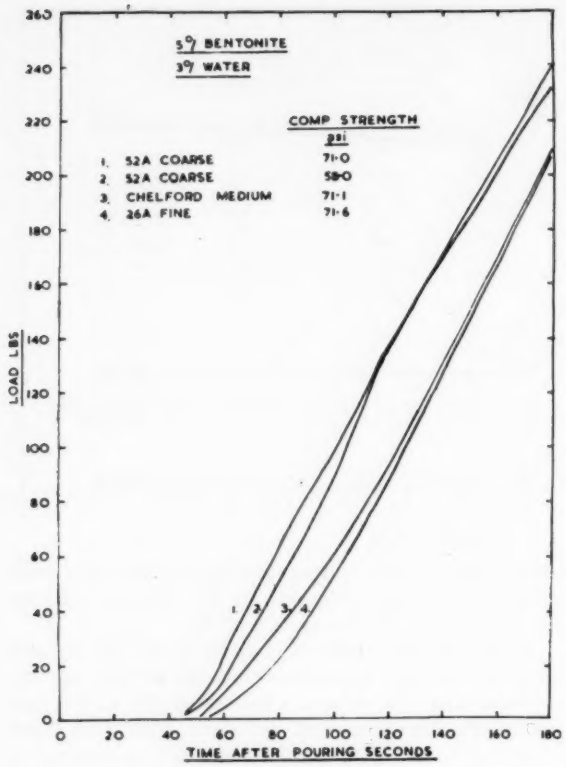


Figure 15

Table 3. The loads induced in the castings at various times after pouring for the four compacts are shown in Fig. 15.

TABLE 3

Sand	Moisture, %	Density, g/cc	Load for Compaction, psi	Dry Strength, psi
Fine				
Arnolds 26A	2.8	1.45	121.0	71.6
Medium				
Chelford	3.2	1.49	35.0	71.1
Coarse				
Arnolds 52A	3.1	1.55	173.0	70.0
	3.0	1.52	74.0	55.5

As the grain size of the sand is increased so is the hindrance to contraction offered by the compact and the amount of load induced in the casting. For example, 75 sec after pouring the loads induced in the castings by the different compacts are:

Fine sand ..... 17 lb
Medium sand ..... 26 lb
Coarse sand (Density 1.52 g/cc) ..... 40 lb
Coarse sand (Density 1.55 g/cc) ..... 55 lb

It is known that as the grain size of the sand increases so does the sintering point of the compact, and therefore its hot strength. These in turn increase the hindrance to the contraction of the casting that is offered by the compact.

Photographs of sections taken from the castings showing the extent of tearing with the four mixes are shown in Figs. 16, 17, 18, and 19. Tearing is most severe in the casting held by the higher density coarse sand, and becomes less severe as the grain size of the sand is reduced. The difference in the extent of tearing with the coarse and fine sands compacts is not so great as would be expected when the loads induced in the casting during the hot-tearing range are compared.

It will be noted, for example, that 75 sec after pouring the casting held by the coarse sand is under a load three times as great as that in the casting held by the fine-sand compact. A possible explanation for this is that a) lower metal/mold interface temperatures are associated with coarse-sand compacts than with fine-sand compacts under identical conditions, and b) coarse-sand compacts have higher thermal conductivities than fine-sand compacts.

The inclusion of a coarse sand test block will therefore result in lower temperatures and faster rates of cooling than with a fine-sand compact, and the hot



Fig. 16 — Tear in casting restrained by a test block of fine sand mixture.

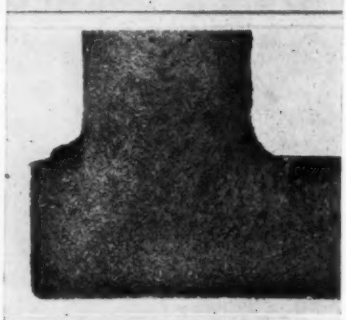


Fig. 17 — Tear in casting restrained by a test block of medium sand mixture.

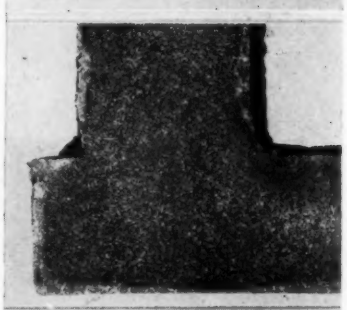


Fig. 18 — Tear in casting restrained by a test block of coarse sand rammed to a density of 1.52 g/cc.

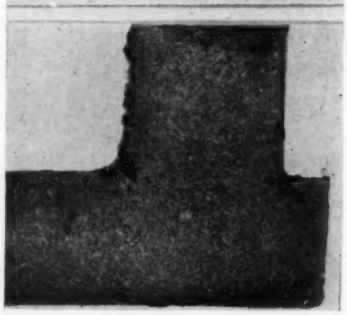


Fig. 19 — Tear in casting restrained by a test block of coarse sand rammed to a density of 1.55 g/cc.

spot in the casting should be less intense and the development of strength in the casting should be more uniform, with a resultant lessening in the likelihood of tearing.

A coarse-sand compact, therefore, influences the contraction of the casting in two ways:

a) By reason of its high hot strength, it increases the hindrance to contraction and thereby makes tearing more probable.

b) It flattens the temperature gradients in the casting (i.e., it acts as a chill) and diminishes the possibility of tearing.

Effect of Different Bonds

Tests were carried out on test blocks containing different clay and organic bonds, and the loads induced in the castings at periods of 55, 75 and 105 sec after pouring are shown in Fig. 20, together with a curve showing the extent of tearing associated with each bond.

The test blocks bonded with clay binders were made of Arnolds 26A sand, while those containing organic bonds were made of Chelford medium sand. All blocks were tested in the dry condition, and details of moisture content, density, etc., are given in Table 4.

The results show that the higher loads due to hindrance to contraction are associated with castings held by the test blocks containing organic binders (phenol resins, linseed oil and semi-solid), this being specially so at times of 75 and 105 sec after pouring.

With clay binders the highest loads are induced with test blocks containing fireclay and silica-flour additions, and the lowest loads with those containing ball clay, bentonite and bentonite plus iron oxide. A naturally bonded sand had low resistance to contraction. The addition of a cereal binder to a bentonite-bonded sand had little effect.

Determination of strengths of molding materials at elevated temperatures[5] have shown that mixes containing fireclays and silica flour additions have higher strengths than those containing bentonite, especially at the temperatures attained near the casting interface, that is, above 1300 C (2372 F).

Of the organic bonds, the highest hinderance to contraction was offered by the test blocks containing 2 pct linseed oil, and the lowest with the block containing 1.25 pct urea resin. The results confirm that sands bonded with urea resins collapse more readily than sands bonded with phenol resins, the phenol-resin-bonded sand having similar collapsibility to one containing 1.5 pct linseed oil.

The addition of a clay (1.5 pct bentonite) to a linseed oil mix reduces the media's ability to resist contraction. This is at variance with published work,[6] which suggests that clay additions to organic binders increase the compact's time of collapsibility at 1371 C (2500 F).

The extent of the tears in the castings restrained with the blocks bonded with clays is small, being of the order of 2 mm for the majority, and increasing to 4-5 mm with fireclay and silica-flour additions.

Large tears, however, are associated with the castings restrained by the blocks containing organic binders (except in the case of the urea-bonded sand), the casting restrained with the block containing 2 pct linseed oil being almost completely severed. Typical examples of the extent of tearing with various bonds are shown in Figs. 21, 22, 23 and 24.

TABLE 4

Bond	Moisture, %	Density g/cc	Load for Compaction, psi	Dry Comp. Strength, psi
Arnolds 26A Sand				
1.5% Bentonite	3.0	1.45	231.0	64.0
5.0% Bentonite	5.0	1.52	118.0	170.8
10.0% Bentonite	5.6	1.45	42.2	111.0
5.0% Bentonite + 1.0% Cereal Binder	5.2	1.52	115.0	254.0
5.0% Bentonite + 5.0% Fe_2O_3	4.7	1.52	71.0	140.0
5.0% Western Mediterranean Bentonite	4.6	1.52	184.0	105.0
5.0% Fullers Earth	4.9	1.45	50.7	71.0
7.5% Secondary Mica Clay	4.2	1.45	57.0	63.2
7.5% China Clay	3.5	1.52	52.8	34.4
7.5% Ball Clay + 2.0 % Bentonite	4.4	1.52	59.0	111.0
7.5% Fireclay	3.3	1.52	112.0	44.0
20.0% Silica Flour + 5.0% Bentonite	5.1	1.52	93.0	170.0
Naturally Bonded Sand	6.6	1.72	30.0	241.0
Chelford Sand				
1.25% Phenol Resin 2.0% Cereal Binder 0.5% Parting	3.0	1.64	78.0	700.0
1.25% Urea Resin 2.0% Cereal Binder 0.5% Parting	3.0	1.64	57.0	825.0
1.5% Linseed Oil + 1.5% Cereal Binder	2.5	1.60	72.0	1442.0
2.0% Linseed Oil + 1.5% Cereal Binder	2.5	1.60	94.8	1810.0
5% Linseed Oil + 1.5% Cereal Binder + 1.5% Bentonite	2.5	1.60	103.0	485.0
4.0% Semi-Solid		1.58	134.0	1060.0

Effect of Variations in the Amount of Linseed Oil Variations

Since severe tearing was encountered in castings restrained with test blocks bonded with linseed oil, a number of tests were made to determine the effect of variations in the amount of linseed oil addition. The castings were restrained by test blocks of similar density but containing linseed oil additions of from 0.75 to 2.0 pct. The loads induced in the casting 55 and 75 sec after pouring and the amount of tearing associated with each mix are shown in Fig. 25, and details of compressive strength, etc. are given in Table 5.

An increase in the amount of linseed oil from 0.75 to 1.0 pct causes higher loads to be induced in the casting; a further increase of linseed oil from 1 to 1.5 pct has little effect, but additions of 2 pct increase the amount of load considerably. Dietert[6] has shown that as the linseed oil/sand ratio becomes greater, so the sand collapses more readily at elevated temperatures.

As would be expected, the curve showing the extent of tearing associated with each linseed-oil mix takes a similar form to that of the load curves. Pho-

tographs of casting sections showing the extent of tearing with each linseed oil content are shown in Figs. 26, 27, 28 and 29.

Effect of Variations in Casting Temperature

In all the tests previously described the casting temperature was held constant at 1575 C (2867 F) to ensure that any differences in hindrance to contraction and the incidence of tearing were due solely to the nature of the molding material. Variation in casting temperature should, however, alter the temperature conditions in the test block and casting, and should therefore affect the amounts of hindrance to contraction operating while the steel is in the hot-tearing range.

To ascertain the effect of variation in casting temperature a series of tests were carried out using test blocks bonded with:

a) 5 pct bentonite. (Moisture, 4.8 pct; density, 1.52 g/cc; dry compression strength, 170.8 psi.)
b) 5 pct bentonite + 20 pct silica flour. (Moisture, 4.4 pct; density, 1.51 g/cc; dry compression strength, 120 psi.)
c) 1.5 pct linseed oil + 1.5 pct cereal binder. (Moisture, 2.5 pct; density, 1.60 g/cc; dry compression strength, 1442.0 psi.)

TABLE 5

Bond	Moisture, %	Density g/cc	Load for Compaction, psi	Dry Comp. Strength, psi
0.75% Linseed Oil 1.5% Cereal Binder	2.5	1.60	200.0	1110.0
1.0% Linseed Oil 1.5% Cereal Binder	2.5	1.61	219.0	1170.0
1.5% Linseed Oil 1.5% Cereal Binder	2.5	1.61	108.0	1442.0
2.0% Linseed Oil 1.5% Cereal Binder	2.5	1.61	94.8	1810.0

Casting temperatures of 1525, 1575 and 1625 C (2777, 2867 and 2957 F) were used, except in the case of the mix containing 20 pct silica flour for which casting temperatures of 1575 and 1625 F (2867 and 2957 F) only were employed.

Curves in which the load due to hindrance to contraction is plotted against time after pouring for the linseed oil and silica flour mixes at the different casting temperatures are shown in Fig. 30.

Similar trends of load/time with casting temperatures were obtained with the 5 pct bentonite mix. The curves show that as the casting temperature becomes lower so the load induced in the casting at

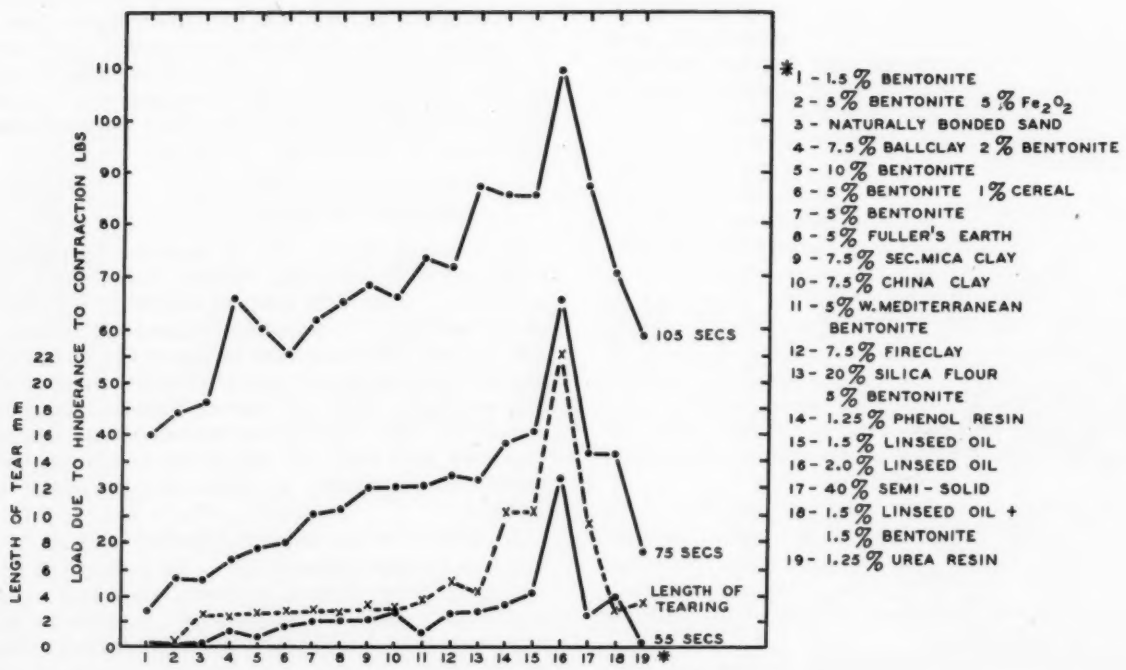


Figure 20

any time after pouring becomes greater. For example, 75 sec after pouring a casting held by a test block containing 1.5 pct linseed oil bears a load of 30 lb when poured at 1625 C (2957 F), 40 lb when poured at 1575 C (2867 F), and 76 lb when poured at 1525 C (2777 F). This trend is due to two factors:

1) With lower casting temperatures the temperatures attained in the test block will be correspondingly lower, and therefore the molding material will have higher strength and slower collapsibility, thus being capable of resisting the contraction of the casting to a greater degree.
2) Low casting temperatures make for rapid solidification of the casting and higher rates of solid contraction, thereby making stress development in the casting more rapid.

As the casting poured with the lower superheat is the one most highly stressed, it would be expected to contain the largest tears. This was not found to be so, as is evidenced by the sections shown in Figs. 31, 32 and 33.

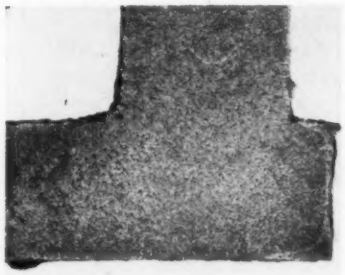


Fig. 21—Section of casting showing the amount of tear induced by test block made from naturally bonded sand.

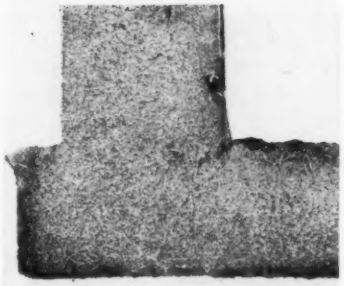


Fig. 22—Section of casting showing tear induced by test block bonded with 7.5 pct fireclay.

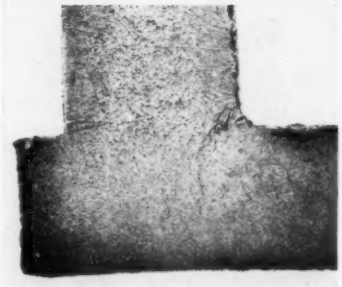


Fig. 23—Section of casting showing the amount of tear induced by test block bonded with 1.25 pct phenol resin.

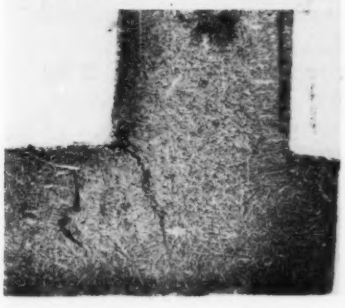


Fig. 24—Section of casting showing tear induced by test block bonded with 2 pct linseed oil.

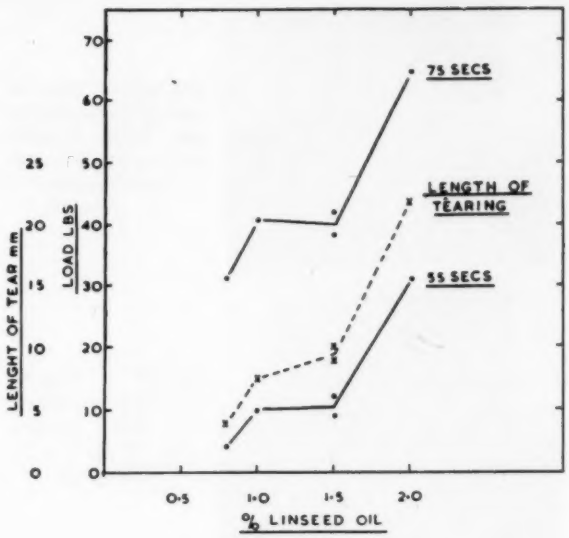


Figure 25

The tearing is most severe in the casting poured at the highest casting temperature, namely, at 1625 C (2957 F), and becomes less severe as the casting temperature is lowered. This means that test blocks bonded with 1.5 pct linseed oil have slow collapsibility, even when the temperatures in the test block are accentuated by the use of the higher casting temperature. The extent of tearing in the casting will, therefore, depend upon the relative amounts of comparatively strong (cool) steel and comparatively weak (hot) steel.

Low casting temperatures favor a more uniform solidification rate throughout the casting so that strength development is uniform. With higher casting temperatures the temperature gradients in the casting are steeper and one portion of the casting (i.e., the bar) will have undergone considerable solid contraction, while the other portion (i.e., the runner portion) will be hot enough to be very weak.

A low casting temperature will also ensure the rapid formation of a solid skin on pouring which is able to resist the contraction stresses. The range of casting temperatures (1525-1625 C) (2777-2957 F) chosen for this investigation is that usually encountered in practice; it is possible that with still higher casting temperatures more rapid collapse of the linseed-oil-bonded sand may occur and thereby make tearing less severe.

With the other sand mixes the influence of casting temperature on tearing is much less marked. Figures 34, 35 and 36 show sections taken from castings held with the test blocks bonded with 5 pct bentonite and cast at 1525, 1575 and 1625 C (2777, 2867 and 2957 F), and Figs. 37 and 38 show sections held by test blocks containing 20 pct silica flour and cast at 1575 and 1625 C (2867 and 2957 F).

The effect of increasing the casting temperature from 1575 to 1625 C (2867 to 2957 F) using test

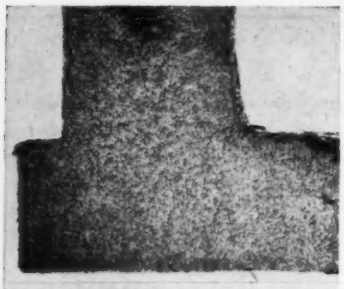


Fig. 26 — Tear in casting restrained by test block bonded with 0.75 pct linseed oil content.

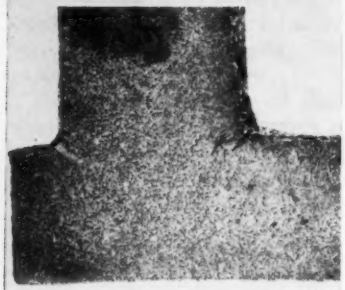


Fig. 27 — Tear in casting restrained by test block bonded with 1.0 pct linseed oil content.



Fig. 28 — Tear in casting restrained by test block bonded with 1.5 pct linseed oil content.

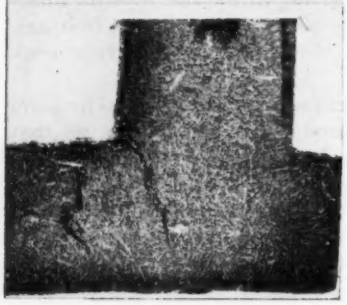


Fig. 29 — Tear in casting restrained by test block bonded with 2.0 pct linseed oil content.

blocks containing 20 pct silica flour is slightly to increase the extent of tearing. With test blocks bonded solely with 5 pct bentonite, tearing is associated only with the casting poured at 1575 C (2867 F), the castings poured at 1525 and 1625 C (2777 and 2957 F) being relatively free from tears.

The differences in behavior with variations in casting temperature between the clay and linseed-oil-bonded sands is probably due to the retention of strength properties of linseed-oil-bonded sands with increasing temperatures as opposed to a lowering of strength properties of sands containing clays and silica flour with increasing temperature. This will cause the linseed-oil compacts to offer higher resistance to contraction to the casting with higher casting temperatures.

The load curves in Fig. 30 confirm this view: at a pouring temperature of 1625 C (2957 F) and 75 sec

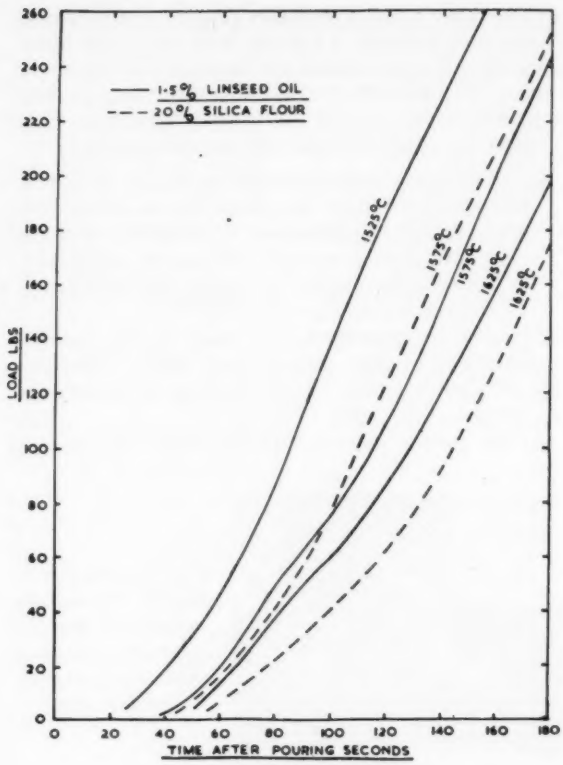




Fig. 31—Section of casting poured at 1525 C (2777 F) restrained by test block containing 1.5 pct linseed oil.

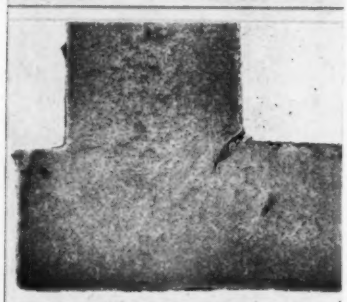


Fig. 32—Section of casting poured at 1575 C (2867 F) and restrained by test block containing 1.5 pct linseed oil.

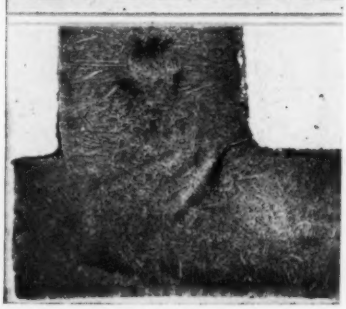


Fig. 33—Section of casting poured at 1625 C (2957 F) restrained by test block containing 1.5 pct linseed oil.

after pouring, the loads induced in the casting held by the blocks containing 1.5 pct linseed oil and 20 pct silica flour are 30 and 17 lb, respectively.

Conclusions

In this investigation the composition of the steel was constant throughout and the results apply only to the composition. Variations in the carbon or sulphur contents of the steel will influence its mode of solidification and should, therefore, increase or diminish the hot-tearing range and affect the incidence of tearing. However, the trends obtained with regard to the effects of variations in molding materials should be similar.

The test method that has been described gives a comparison of the amounts of hindrance to contraction that are offered by various molding media under the conditions of test. Any change in these conditions, e.g., casting design, will have an effect upon the results, although with castings of similar size the trends of the degree of hindrance to contraction with various bonds, effect of compact density, etc., should be similar.

With larger castings having a greater heat capacity, however, the temperature conditions in the mold will be more intense and the rate of solid contraction of the casting will be slower. Organically bonded sands will have more time in which to collapse before the casting is in the hot-tearing range, and the clay-bonded sands, being at a higher temperature, will behave differently.

It is suggested that tearing occurs over a temperature range, i.e., tears are initiated at high temperatures, probably within the solidus/liquidus region and with only slight loads (of the order of a few pounds) acting on the casting. The tears are propagated further as the stresses induced in the casting become greater down to a temperature at which the steel rapidly develops strength or ductility, i.e., subsolidus.

If the build-up of stress (due to severe conditions of hindrance to contraction) is rapid during this range then the tearing will be severe and may propagate across the casting section. If, however, the build-up of stress is slow (i.e., with mild conditions of hindrance to contraction) compared with the increase in strength and ductility with falling temperature, then the initially formed tear will not become enlarged. In particular it has been shown:

a) Hot tearing is less likely to occur and is less severe in castings poured with low temperatures.
b) High casting temperatures do not accentuate the tears in castings held by compacts bonded with clays.
c) An increase in the casting temperature with castings held by compacts containing linseed oil makes the tearing more severe.

The investigation has shown that the behavior of a molding medium under casting conditions cannot be assessed by its properties at room temperatures, e.g., sand compacts having high compressive strengths at room temperatures may offer less resistance to the contraction of the casting than compacts having low compressive strengths.

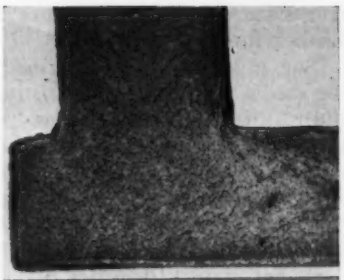


Fig. 34—Section of casting poured at 1525 C (2777 F) and restrained by test block containing 5 pct bentonite.

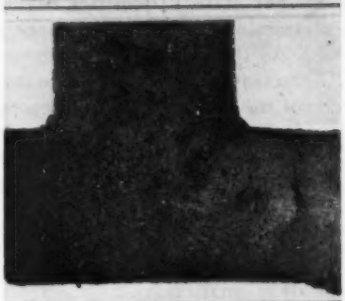


Fig. 35—Section of casting poured at 1575 C (2867 F) and restrained by test block containing 5 pct bentonite.

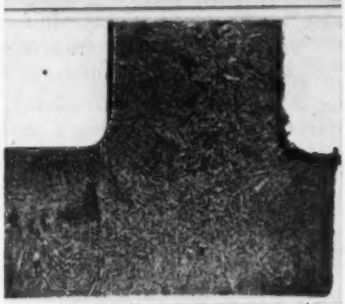


Fig. 36—Section of casting poured at 1625 C (2957 F) and restrained by test block containing 5 pct bentonite.

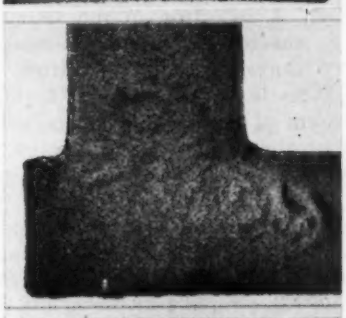


Fig. 37—Section of casting poured at 1575 C (2867 F) and restrained by test block containing 20 pct silica flour.

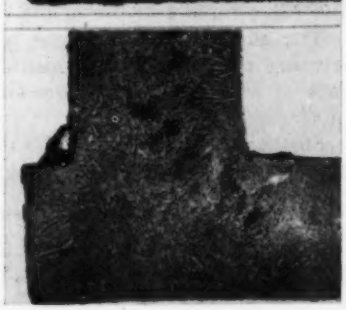


Fig. 38—Section of casting poured at 1625 C (2957 F) and restrained by test block containing 20 pct silica flour.

As the molding material in contact with the casting is heated rapidly to a high temperature [above 1350 C (2462 F)], it follows that it is the strength properties of molding media at these high temperatures which largely control its ability to resist the contraction of the casting. With larger castings the

temperatures would be higher and would progress furtner into the sand.

The investigation has verified that certain molding media are capable of resisting the contraction of the casting to such an extent as to cause tearing of the metal, and that variations in the molding media such as density, type and percentage of bond, etc., affect the resistance to casting contraction, and therefore the incidence of tearing. In particular it has been found:

a) For similar densities dry sands resist the contraction of the casting more than green sands.

b) As the density of green- or dry-sand compacts increases, so does its resistance to the contraction of the casting become greater, with a corresponding increase in the likelihood of tearing.

c) The coarser the sand in the compact the greater is the compact's hindrance to the contraction of the casting.

d) Castings restrained by compacts bonded with clays (with the exception of fireclay) contained only small tears.

e) In the range of clay binders tested, the greatest hindrance to the contraction of the casting was offered by mixes containing fireclay, and bentonite plus 20 pct silica flour.

f) Compacts containing 2 to 3 pct bentonite offer higher resistance to casting contraction than compacts with 1.5 pct and from 4 to 10.0 pct bentonite.

g) The most severe tearing encountered was in castings held by compacts containing organic binders.

h) A reduction in the amount of linseed oil in the compact renders it more collapsible and reduces the extent of tearing.

Very little difference was found with regard to the extent of tearing in castings held by blocks bonded with the various clays, the most severe cases of tearing being associated with castings restricted by sands bonded with organic materials, i.e., linseed oil and phenol resins. It would then appear with castings of the size employed, that the amount of heat available is insufficient to burn away the organic bond quickly and cause rapid collapse of the core.

The addition of extra heat to the casting by increasing the casting temperature did not affect the time of collapse of these cores and reduce the extent of tearing.

Further work, using a similar testing procedure but employing a much larger casting, is in hand.

Acknowledgments

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4. A.F.S. Committee Report "Causes of Rat-Tail Casting Defects," A.F.S. TRANSACTIONS, vol. 56, pp. 116-137 (1948).

5. R. E. Morey and C. G. Ackerlind, "A Study of the Effects of Various Binders and Additions on the Hot Strength of Molding Sands," A.F.S. TRANSACTIONS, vol. 58, pp. 411-422 (1950).

6. H. W. Dietert, MODERN CORE PRACTICE AND THEORIES, American Foundrymen's Society.

DISCUSSION

Chairman: C. B. JENNI, General Steel Castings Co., Eddystone, Pa.

Co-Chairman: J. R. GOLDSMITH, Crane Co., Chicago.

Recorder: W. R. PUNKO, Wehr Steel Co., Milwaukee.

W. S. PELLINI *(Written Discussion):*[1] The author is to be congratulated for an outstanding investigation of very great importance to the problem of hot tearing. The value of a scientific approach to complex foundry problems is demonstrated most clearly by the results of these studies.

It is of interest to note that the research findings of the British Steel Castings Research Association as presented in the subject paper and those of the Naval Research Laboratory presented previously[1,2] are approaching close agreement. There remain certain minor differences in our views which may be more apparent than real and which may in final analysis be resolved to matters of terminology and relative emphasis. It is the purpose of this discussion to analyze the significance of Mr. Middleton's data in terms of the Naval Research Laboratory concepts of the mechanism of hot tearing, i.e., to express the findings in somewhat different terms. It is my own impression that the differences of views concerning the basic mechanism of hot tearing are now approaching the point of drawing fine distinctions and do not include major disagreements.

Item one is without question that of the temperature range of hot tearing. Here we have the first fine distinction; Middleton quotes the Naval Research Laboratory conclusion as "that tearing *can* be initiated while liquid metal is present." In our view, hot tearing is possible *only* if liquid films are present. It should be noted that the concept of liquid films does not define a single temperature but a range of temperatures in what we have elected to call the "film stage." In reference (2) it is stated, "film stage is not a fixed and specific condition but a condition which covers a range of film thicknesses. As the metal first falls to a temperature which may be described as within the film stage the films are relatively thick and continuous—as the metal approaches complete solidity the thickness and continuity of the films decreases in regular fashion." It is also recognized that low melting segregates such as sulphides maintain the liquid film condition to temperatures below the true solidus of the metal—accordingly hot tearing may occur at moderately sub-solidus temperatures for such conditions, and *only* for such conditions.

FILM STAGE CONCEPT OF BASIC IMPORTANCE

The film stage concept is of basic importance for once adopted it necessarily follows that:

1. The stress level associated with hot tearing must be of very low order. In fact the only stress which can be visualized for solid particles separated by liquid films is that which results from interlocking of the dendrite arms. If such interlocking did not occur the stress level would indeed be "zero."

2. A sharp increase in strength and ductility should be developed at the point that the liquid films disappear and the metal becomes completely solid.

3. Strain (extension) of the hot spot is entirely concentrated to the liquid film regions.

The findings that hot tearing is a film stage problem forces a revision of the classical foundry thinking of the problem based on the erroneous concept that solid hot metal of low strength and ductility was ruptured due to high restraint stresses caused by the sand. Such thinking placed inordinate importance on metal variables and was responsible for much misdirected effort in the approach to hot tearing problems.

The author concludes that film stage rupture is responsible for hot tearing, however, the data which are presented for surface temperatures would, if correct, contradict his conclusion. The use of a U-tube arrangement provides data which are obviously reliable for the temperature at the center of the bar. However, it is believed that the near surface temperatures ob-

[1] Head, Metal Processing Branch, Metallurgy Div., Naval Research Laboratory, Washington, D. C.

tained by the use of thermocouples which "are so arranged as to protrude slightly into the mold cavity" are in serious error due to conduction losses. Figure 4 presents data indicating that at the time of tearing the center temperature was 2710 F (32 F above solidus) and the surface 2608 F (70 F below solidus), a total difference of 112 F. This difference, 224 F, in gradient is not possible for steel solidifying in a sand mold because of the high conductivity of the steel compared to sand. Thermal analysis of the solidification of rods and plates of this thickness using U tubes at near surface positions indicates gradients of 10-20 F. This fact is cited inasmuch as acceptance of the 112 F gradient would indicate complete tearing of the bar at temperatures such that essentially 75 per cent of the thickness is completely solidified. This conclusion is evidently not meant, even by implication, in view of the later statements that hot tearing is considered possible only if liquid is present. The conclusion that tearing required a stress of 55 lb and occurred *at* 55 sec is questioned in view of the fact that the holding of appreciable loads after 55 sec (the falling part of the load-time curve) indicates that rupture evidently was not complete (the corner edge regions appear to have held as shown by Fig. 5). It is probable that the hot tearing of the surface regions occurred somewhat earlier and the continued rise in load is due to holding of the edge regions at the corners.

Another point of possible differences of interpretation pertains to the arbitrary times of 55, 75, and 105 sec taken to determine load. Figure 2 demonstrates that the solidus is reached at the center in approximately 80 sec. This would indicate that a completely solid skin should be developed at the surface at somewhat earlier times. The "pull tests" of reference (2) and (3) indicated that a rapid rise in strength was developed when approximately the first ¼ in. of the surface of a 3-in. diam bar became completely solid. This occurred at approximately 0.6 time of complete solidification at the center. A number of the load-time curves presented by Middleton show a significant rise in strength at approximately 40-50 sec which is agreement with this view. In any event, it appears that the loads developed at times later than 80 sec (center solid) are not significant to hot tearing of this bar and merely indicate sand restraint values at times such that the possibility of hot tearing of this bar no longer exists. It is believed that 40-50 sec should be taken as the significant final time of hot tearing on a basis of surface skin formation. If Fig. 20 is considered on this basis only the 55-sec load curve is of significance to hot tearing of the subject bar. It is of interest that with one exception the loads do not exceed 10 psi and for the most part are of the order of 5 psi. These data should be considered most carefully by all who have been steeped in the foundry tradition of "high stresses" connected with hot tearing and who have considered hot tearing as strictly a stress problem.

Analogy of Mechanics of Hot Tearing

In the simplest analogy, the mechanics of hot tearing may be compared to a chain anchored at its two ends in sand; assume the chain links are hot and cooling and that one of the links is composed of solid metal blocks separated by liquid films. The stress developed in the system as the result of the shrinkage of the links will obviously be controlled entirely by the *resistance to strain* of the "liquid film" link. The stress level will remain of zero value until the liquid film regions (assumed to be gradually becoming thinner) develop bridging or solid metal contact points—a measurable stress will then be developed in the system and the sand will be called on to "react" that is to furnish the opposing force.

If the strain rate (determined by the number of cooling links and speed of cooling) is sufficiently high, hot tearing will occur prior to the time that a measurable stress is developed in the system. If the strain rate is low the semi-solid link will be permitted to develop solid contact points (bridging of dendrites) and a small stress will develop in the system which may be measured in terms of a few psi. The sand restraint action may become effective only at this time for any yielding developed by the sand represents yielding (strain) which the semi-solid link will not have to endure. However, the go-no go stress conditions which determine whether hot tearing occurs represent differences in the restraint action of the sand entailing variations which are probably less than 10 psi. Whether "essentially zero" psi or a few psi stress is required to develop hot tearing is entirely controlled by the strain rate imposed on the hot spot. Hence, whether differences of sand restraint properties play a significant part is determined by the geometry of the casting insofar as the hot spot and solid regions are affected thereby.

The above considerations naturally lead to a generalization of the mechanism of hot tearing in terms of strain as the deciding factor rather than stress. Thus, it is considered that the resistance to tearing of the film stage metal is near zero at temperatures such that the films are relatively thick and continuous. However, tearing will occur only if the strain rate (in./in./sec) imposed on the hot spot is very high. The necessary conditions are (1) very narrow hot spot (2) long region undergoing rapid contraction. Since the metal is not capable of developing appreciable resistance to the imposed strain, at the time when the films are thick and continuous, the stress developed in the system can never be of high order *irrespective of the properties of the sand.*

Strain Rates

At temperatures (near solidus) such that the film stage is approaching disappearance the films become very thin and the interlocking of dendrites becomes more pronounced. The metal will then have an *apparent* strength of small but measurable magnitude. Strain rates of moderate and low order should be sufficient to cause rupture since the amount of movement which is possible when the films are very thin must be very small. Strain rates of low order do not cause hot tearing at the higher temperature of thicker films because of the greater amount of metal movement (strain) which is permissible without tearing. Sand properties become significant for conditions of low strain rates (which require near solidus temperatures for tearing) inasmuch as the small loads which the metal can support at near solidus temperatures must necessarily be resisted by the sand. If the sand collapses readily the strain does not build up to higher levels in the metal (partially "taken up" by the sand) and tearing does not occur. However, if the sand is relatively rigid the metal continues to strain with resulting rupture when the strain limit for tearing is reached.

It should be noted that a soft sand may be highly effective in forcing continued straining of the hot spot *if* a wide bearing surface of metal is provided. This effect may be visualized by considering the resistance or "give" of snow to the load of a person wearing ordinary shoes as compared to snow shoes. If the casting design is such as to provide a broad bearing on the sand surface which restricts metal contraction it will be extremely difficult to provide sufficient collapsibility to prevent the development of minor loads such as the 5-10 psi demonstrated in this paper, Fig. 20 at 55 sec time.

For a casting design such that the bearing surface is relatively small the relative collapsibility features of the sand become more important in determining the development of tearing. These considerations explain the experience of steel foundrymen that certain castings will develop tears even with extreme precautions entailing relieving blocks, coke backing, soft cores, etc. The reaction of soft snow to a broad loading surface provides a parallel.

It is predicted that a change in the design of the equipment used by Middleton such that the runner face is made smaller should show greater effects due to sand variables; conversely, enlarging of the runner face should show less effects.

When the metal develops complete solidity a *discontinuous* strength change occurs which is very marked since it represents a sudden change from an apparent strength due to interlocking of dendrites to a true strength of hot solid metal. The change involves raising the resistance to strain by the metal from barely measurable loads to approximately 2000 psi.[3] This change is essentially instantaneous, if plotted in terms of temperature versus load carrying capacity the initial change during film stage entails a gradual rise from essentially "zero" to barely measurable stresses, then an abrupt rise to approximately 2000 psi (creep strength) at the solidus temperature. Tearing is no longer possible after the metal becomes solid and the load measured by a spring arrangement such as used by Middleton merely indicates the resistance of the sand test block to *"indentation"* by the bearing face of the runner and *not* metal strength at sub-solidus temperatures.

Strain Theory

The "Strain Theory" concept has been misunderstood by some to signify that stress is never required to develop "hot tearing." This is not the case for the "Theory" clearly postu-

lates the conditions under which "stress" in the classical foundry sense of resistance by the sand, is a go-no go factor which tips the scale in favor of tearing and cases for which "stresses" associated with hot tearing are so minute as to preclude worthwhile consideration.

"Stresses" associated with hot tearing have been considered of very great magnitude—the N.R.L. work and the excellent confirmation obtained by Middleton of the very low levels of "stresses" associated with hot tearing should serve to realign much erroneous thinking which has prevented correct analysis of many practical hot tearing problems in the foundry.

"Strain" thinking as contrasted to "stress" thinking is a new manner of visualizing hot tearing problems which provides for a consideration of all of the mechanical conditions within the metal and mold which lead to hot tearing. "Strain" thinking does not eliminate consideration of stresses, it merely sets such considerations in their proper place and value. Stress provides no basis of thinking through problems associated with the relative lengths of the hot spot and of the contracting regions, relative rates of cooling of the hot spot and cold regions, etc. For example, thinking in terms of strains rather than in terms of minuscule stresses ranging from 5 to 10 psi explains such oddities as the hot tearing at higher temperatures and lower stresses observed by Middleton and Protheroe[3] when a stiff spring replaced a softer one. The use of a stiff spring in the equipment used in reference (3) may be considered equal to using a more rigid sand which according to the usual thinking related to stress should result in higher stresses conducive to tearing. However, the higher stresses of the stiff spring system *never developed* because of the stiff spring did not absorb very much of the contraction of the cold part of the test bar, accordingly the hot spot had to strain (stretch) a greater amount and at a more rapid rate. This was sufficient to cause tearing at a time that the films were relatively thick (higher temperature) and capable of movement (without tearing) of an order which the soft spring did not "request" the metal to exceed.

The difficulties and pitfalls associated with thinking in terms of stress are demonstrated by the tests concerned with pouring temperature as a variable, Fig. 30. The casting poured with the lowest pouring temperature developed the highest stress (at the arbitrary 55 to 75 min used for reference), hence, according to stress logic should develop largest tears, which was not the case. In fact the casting poured at the highest temperature which only developed approximately 15 psi stress (compared to approximately 80 psi for the one at the lowest temperature) developed the most severe tears. At this point Middleton rightly gives up on stress, concludes that the specific sand used is of slow collapsibility (hence not temperature sensitive) and states, "The extent of tearing in the casting will, therefore, depend upon the relative amounts of comparatively strong (cool) steel and comparatively weak (hot) steel." This statement is agreed with most heartily for it represents a restatement of one of the principal elements of the strain theory. The following is quoted from reference (2) "Strain Theory of Hot Tearing."

GENERALIZATIONS ON HOT TEARING

"The various observations of the interrelated effects of metal and mechanical variables lead to a generalization of the hot tearing problem to the following principal elements:

1. The time-rate of extension of the hot zone during its film stage is the primary mechanical factor which determines hot tearing. Inasmuch as the film condition stage lasts for only a relatively short fraction of the total solidification time, hot tearing will occur only if a sufficient rate of extension is forced on the film regions during this short time.

2. The rate of extension forced into the film regions is related to: (a) *the overall extent of the hot zone,* (b) the amount of mold restraint, and (c) *the length and rapidity with which the adjoining sections cool relative to the hot zone.*" The portions which are italicized are evidently of the same meaning as Middleton's conclusion. Item 2b is considered fixed in this case by the assumption of slow collapsibility. It appears that our viewpoints here are very close indeed even if stated in different terms.

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2. W. S. Pellini, "Strain Theory of Hot Tearing," *Foundry*, November 1952, p.125.

3. J. M. Middleton and H. T. Protheroe, "The Hot Tearing of Steel," *Journal Iron and Steel Institute*, August 1951, p. 384.

AUTHOR'S REPLY TO MR. PELLINI

MR. MIDDLETON *(Reply to Mr. Pellini):* Mr. Pellini's remarks are greatly appreciated, and I would like to thank him for his major contribution to the discussion. From this it is clear that there are several minor differences in our interpretations of the results presented in the paper.

1. *Method of Measuring the Temperature of Tearing*—Mr. Pellini criticizes the use of bare thermocouples for measuring the skin temperature of the casting during the time of tearing, suggesting that the use of this type of couple will lead to serious errors and record lower temperatures due to conduction losses. Mr. Pellini's fears on this score are definitely not borne out in practice. This all-important aspect of measuring skin or interface temperatures has received detailed study as shown by the work of Atterton and Houseman[1] and by that of the writer.[2] This has shown that the bare intermediate type of couple when properly employed is as accurate as the conventional sheathed type as used by Pellini. Any inaccuracies due to conduction are small owing to the small diameter of the wires, (e.g. 0.5 mm) and contamination is not a serious problem. The accuracy of the bare type of couple as against the sheathed is evidenced by the curves *d* and *c*, Fig. 2, of the paper. Curve *d* is recorded with the sheathed type and curve *c*, with the bare couple. It can be seen that the maximum interface temperatures recorded by the two couples are very similar.

2. *Temperature of Tearing*—While we both agree that tearing is initiated during the later stages of solidification when a small amount of liquid remains, Mr. Pellini is of the opinion that the temperature of tearing stated in the paper is too low, as it is below that of the solidus of the steel. It is evident from his published work[3] that Mr. Pellini takes his solidus temperatures from the iron-carbon diagram as determined under equilibrium conditions and bases his "film stages" on these temperatures. The writer, however, contends that these temperatures are not applicable in practice, for the following reasons:—

(a) Even under ideal conditions, i.e. equilibrium conditions of very slow cooling and of agitation, the accurate determination of solidus temperatures is extremely difficult. This is well evidenced by the disagreement between various investigators regarding the solidus temperatures of pure iron-carbon alloys.[4] Under conditions of non-equilibrium, however, as would be encountered in practice with small castings, it is practically impossible to determine the solidus temperatures of steel.[5]

(b) The attainment of equilibrium in an alloy at any given temperature is dependent upon time, and at normal rates of solidification, few alloys, if any, approach this condition. Therefore, the degree to which an alloy departs from equilibrium is a function of the rate of cooling from the liquid state. With rapid rates of cooling, as would be encountered in practice with small castings, the steel undergoes considerable under-cooling, and hence solidus temperatures are much lower than those that would theoretically be obtained under equilibrium conditions.

(c) The steels under consideration also undergo a peritectic reaction during their solidificaton. This type of reaction is particularly susceptible to under-cooling, the velocity of the reaction being limited by the solution and redeposition of a solid phase and by the velocity of diffusion in the solid state. Both these reactions are slow in comparison with that of crystallization and extremely slow cooling is essential if the reaction is to proceed to completion, i.e. equilibrium. It is therefore possible with small castings which cool at a rapid rate, that the composition of the peritectic liquid (0.55 per cent C) is not materially altered, and hence will have a solidification temperature well below that of the base steel.

(d) A relatively small amount of sulphur and phosphorus also cause liquid films to persist to temperatures below the "true" solidus of the steel. The presence of such films means that the solidus of the steel is depressed still further and tearing can occur at temperatures well below those taken from the iron-carbon diagram, and not at the moderately sub-solidus temperatures suggested by Pellini.

Finally let it be noted that while Mr. Pellini criticizes the writer's recorded tearing temperatures as being too low, his own indications are similar as is shown in the following table.

TEMPERATURES OF TEARING (SKIN)

Carbon, %	Pellini and Others[3]	Middleton
0.25	2595 F	——
0.28	2645 F	2608 F
0.31	2595 F	——

3. *Time after Pouring at which Tearing occurs*—The writer maintains, as stated in his paper, that the manner of assessing the actual time of tearing, i.e. by the release of tension on the spring is both valid and accurate. Previous unpublished work on a similar casting, in which the tension of the spring was released at different time intervals before that at which tearing occurred gave the following results:—

Time after Pouring	Load	Results
a) 35 sec	25 lb (Load released)	No tear.
b) 50 sec	52 lb (Load released)	No tear.
c) 60 sec	68 lb (Tearing shown by release of spring.)	Severely torn.

The steel was poured at 1600 C, (2912 F) and had the following composition:—C, .21%; Mn, .8%; Si, .35%; S and P, .035%.

From theoretical consideration of the thermal equilibrium diagram of the iron-carbon system it might be deduced by Mr. Pellini that all tearing should take place in the writer's casting within 55 sec after pouring. It was however established by experiment that, for the test casting employed, tearing was not initiated in less than 55 sec and that the theoretical solidification temperature at the position of tearing was not reached until 80 sec after pouring. As was discussed previously the film stage during which tearing occurs is protracted both of undercooling and by film forming elements and hence final solidification and tearing may occur at times later than the theoretical (80 sec). To cover the extreme hot tearing range of the casting investigated the time limits of 55 and 105 sec were therefore selected.

4. *Degree of Stress to cause Tearing*—While it is agreed that stress as low as 5-10 psi can cause tearing, the tears so caused have been shown after deep etching to be small and of the order of a few millimeters. To obtain extensive tearing in the particular casting investigated, stresses of between 30-60 psi are necessary. It may be that with different designs and dimensions of casting the stresses to cause extensive tearing will also be different.

A tendency to tearing in steel extends from the time the dendrites interlock and form a coherent network, down to the time at which the last traces of liquid freeze and the alloy assumes ductility. The strength of the alloy during this stage will be dependent upon the amount of contact between the dendrites. The nearer to the solidus, the greater will be the amount of contact between the dendrites, and therefore the stronger will be the alloy. For a given casting, the stress required to cause tearing is not constant, but depends upon the hindrance to contraction operating. As shown previously,[6] the more severe the conditions of hindrance to contraction, the higher the tearing temperature and the lower the stress necessary to cause tearing; i.e., tearing occurs when the dendrite arms have made little contact. With moderate degrees of hindrance to contraction, stress development is slower compared with gain in strength of the alloy and tearing, if occurring under these conditions, will take place at lower temperatures (when the dendrite arms have made more contacts) and with higher stresses.

Mr. Pellini's statement regarding an instantaneous increase in stress resistance of a casting from a "barely measurable" stress to 2000 psi does not seem to be in accord with his work.[3] The sudden increase in strength with falling temperature occurred in fact when the casting was already capable of withstanding stresses of between 200 to 400 psi. Stresses of this order are far from being "barely measurable" and could be looked upon as being moderately high. The particular instance quoted by Mr. Pellini therefore does not in the writers view give support to the suggestion that tearing may occur with zero stresses. However, it is felt that according to the design and size of casting, conditions of pouring and degree of hindrance to contraction operating, tearing may take place at stresses in the order of a few psi, or to quote Mr. Pellini, "with minuscule stresses."

5. *Effect of Strain Rate on the Incidence of Tearing*—I am in agreement with Mr. Pellini that the strain rate is all-important, as it is this which largely controls the amount of stress induced in a hot spot at any given time after casting. Mr. Pellini refers to high and low strain rates, but gives no indication of what order of strain rate will induce tearing in the casting. This, of course, must depend upon the temperature conditions existing in the casting, but it is possible that it may be of a similar low order to the stress.

This is substantiated by calculations made from the data given in this paper, e.g. a casting tearing 55 sec after pouring with a stress of 55 psi was contracting at a rate of .000009 in. per in. per sec.

The strain theory is not new and has of course been used by many investigators in the past, though expressed in differing terms, to account for the effect of such variables as casting temperature, casting design, different degrees of hindrance to contraction, etc. on the incidence of hot tearing. The experimental results presented in this paper indicate the stresses necessary to cause tearing in the casting investigated under the conditions of testing as described. It is apparent that variations in testing procedure must influence the results, e.g. changing the pouring temperature must have its effects on the temperature gradients present in the casting and the mould, and will therefore alter the amount of stress required to cause tearing.

Stating the results presented, in terms of strain rather than stress would lead to precisely the same conclusions as those already drawn in the paper.

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1. D. V. Atterton and D. H. Houseman, "Measurement of Temperatures of Metal/Mould Interface Temperatures," *Nature*, May 30th, 1953.

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3. H. F. Bishop, C. G. Ackerlind and W. S. Pellini, "Metallurgy and Mechanics of Hot Tearing," AFS TRANSACTIONS, vol. 60, pp. 818-833 (1952).

4. S. Epstein, *Alloys of Iron and Carbon*, New York, 1936, McGraw Hill Book Co., Inc.

5. D. R. F. West, "Solidification of Horizontal Steel Castings," *Jnl.*, I.S.I., Feb. 1951.

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CASTING MAGNESIUM ALLOYS IN SHELL MOLDS

By

Nicholas Sheptak*

ABSTRACT

Magnesium castings were successfully produced in shell molds when inhibitors were used as addition agents or as mold washes. Ammonium borofluoride (½ to 1 per cent) as an addition agent and boric acid and Dow No. 181 agent as mold washes were the most satisfactory inhibitors.

Tests were developed for evaluating sand-resin mixes, with the hot cure test and transverse strength test considered essential.

Shell core mixes with good breakdown properties were developed with phenol formaldehyde-phenol furfural and phenol formaldehyde-urea formaldehyde resin combinations.

The cooling rate and average grain size of magnesium alloys cast in shell molds were varied by the pouring temperature and the use or omission of a backing agent.

Introduction and Literature Survey

Since the early part of 1947 when the shell molding process was made public,[1] great strides have been made in developing the process, especially in the iron and steel foundry industry. The emphasis has been on iron and steel rather than the light alloys for several reasons. First, other processes exist for casting light alloys to close tolerances. Second, the high strength and high thermal decomposition temperature of phenol formaldehyde resins make them more suitable for the higher melting point alloys.

Shell casting magnesium presented problems not common in shell casting ferrous alloys. In studying these problems, tests were developed for evaluating sand-resin mixes, the effectiveness of inhibitors was investigated, shell-core mixes were evaluated for ease of breakdown, and cooling rates of shell-cast metal were studied. Finally, the principles were applied to a small production casting.

Although there has been only brief reference to shell molding magnesium in the literature, it might be worthwhile to review some of the existing articles on the technology of shell molding.

The current status of shell molding was reported by Tindula,[2] including a bibliography of 33 references plus 3 references on other processes. Articles by Ames, Donner, and Kahn[3,4,5] described the metallurgy and development work on the shell molding process. Sokolosky[6] described some recommended gating systems for shell molds. The application of shell molding has been reported by Bello,[7] Stone,[8] Derby,[9] and Calder.[10]

Some of the work reported in this paper was sponsored by Frankford Arsenal, U.S. Army Ordnance Corps.

Tests and Sand-resin Mixes

To date, there are no standard tests for evaluating sand-resin mixes used for shell molding. The tests described in this paper were designed to take into consideration the variables of time and temperature on the curing characteristics of the sand-resin mixes. Hardness, rigidity, transverse strength, and permeability were measured as functions of cure time at constant temperature. Resin concentration was held constant at 5 pct.

a. **Hot Cure Test**—The hot cure test was designed to determine the cure time for the sand-resin mixes, using hardness as a function of the degree of curing and therefore of the polymerization of the resin. Indentation hardness readings of a 4-in. diam by ¼-in. thick specimen prepared on a hot plate were taken at regular intervals of time at fixed temperatures in the range of 400 to 700 F. Hardness readings were taken with a core hardness tester using a ½-in. ball indenter. Table 1 lists the compositions of some of the mixes tested and the cure times required for maximum hardness.

The effect of curing temperature on cure hardness as a function of time for a 5 per cent phenol formaldehyde-bonded mix is illustrated in Fig. 1. At 400 F, a maximum hardness of 98 was reached after 170 sec, while at 700 F, it was reached after only 60 sec. Higher temperatures would yield still shorter cure times, which would be desirable in a mass-production operation, but at the higher temperatures, shell making is made more difficult by the breakdown of the silicone release agents used.

To check the effect of varying sand grain size on the cure rate, three mixes were prepared with 5 pct of 50 pct phenol furfural—50 pct phenol formaldehyde resin and silica sands of A.F.S. grain fineness Nos. 62, 90, and 114. Initially, the mix containing the finest sand cured faster, due to the increased surface area, but all of the three mixes reached the

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same maximum hardness at the same cure time.

Varying the sand component had little effect on the cure rate. Mixes were prepared with silica sand, zircon sand, and 50 pct silica sand—50 pct zircon sand. The zircon sand mix cured faster initially, but the same maximum hardness was obtained at the same cure time for all three mixes.

From the aforementioned results it can be concluded that the nature of the bonding agent is the sole determinant of cure rate at a constant temperature and resin concentration.

b. **Transverse Strength Test** — The transverse strength was chosen in preference to the tensile strength to determine the bonding characteristics of the various sand-resin mixes. It was felt that the transverse strength test would more nearly describe the forces to which the shell molds would be subjected in stripping and handling. Specimens 8 in. × 1 in. × ¼ in. were made on the hot plate, using fixed temperatures in the range of 400-600 F and cure times determined by the hot cure test. They were tested on a beam tester using 6-in. centers and center point loading.

In general, the transverse strength depended on the resin formulation, length of cure, temperature of cure, and sand grain fineness. At 400 and 500 F, maximum strength was obtained at a cure time greater than that required to obtain maximum hardness. This is illustrated in Table 2 which summarizes the data for a 5 pct phenol formaldehyde-bonded mix. At 600 F, maximum transverse strength coincided with the cure time required to obtain maximum hardness.

At a resin concentration of 5 pct of 50 pct phenol furfural—50 pct phenol formaldehyde, a greater transverse strength was obtained with sand of A.F.S. grain fineness No. 114 than with sands of fineness Nos. 62 or 90. At 400 F, these values were 12.1 psi for the No. 114 sand, 11.3 psi for the No. 90 sand, and 10.2 psi for the No. 62 sand.

Phenol formaldehyde-bonded shells had a transverse strength of 15.1 psi while 50 pct phenol furfural —50 pct phenol formaldehyde-bonded shells had a strength of 11.0 psi. Shells bonded with 40 pct phenol formaldehyde and 60 pct urea formaldehyde had a transverse strength of only 4.3 psi.

c. **Permeability Test**—The permeability test was made according to the A.F.S. test for standard permeability, using a specimen 2 in. in diameter by ¼ in. thick prepared on a hot plate. Table 3 summarizes the data for a 5 pct phenol formaldehyde-bonded mix cured at 400, 500, and 600 F. It can be readily seen that the permeability exceeded the normal magnesium molding sand permeability of 80 to 100. This proven high permeability of shell molds made it unnecessary to continue the permeability test as a control test.

d. **Hot Rigidity Test**—This test was designed to determine the cure time required to produce a rigid shell. Specimens 8 in. × 1 in. × ¼ in. were prepared as for the transverse strength test. After a given cure time at a fixed temperature, the specimen was moved to the edge of the hot plate and 4 in. extended at a normal to the edge of the plate. The angle of slump or depression was read within 30 sec.

The data for a 5 pct phenol formaldehyde-bonded mix listed in Table 4 indicate that zero slump, or a rigid shell, was obtained at the same cure time as required to obtain maximum hardness in the hot cure test. Since this correlation between the two tests existed, the hot rigidity test was discontinued.

Inhibitor Evaluation

The reactivity of magnesium alloys in the molten state with oxygen, water, and silica sand prompted a study of the inhibitor requirements of shell molds. In normal magnesium green sand foundry practice, boric acid, sulphur, glycol, and various fluorine compounds are used as sand agents to inhibit these reac-

TABLE 1—HOT CURE AND TRANSVERSE STRENGTH RESULTS OF VARIOUS SAND-RESIN MIXES

Sand Mix No.	Resin—5%	Sand—95%	A.F.S. Sand Fineness No.	Cure Time, Sec at °F				Transverse* Strength, psi	Remarks
				400	450	500	600		
	Type A								
1.	Phenol Formaldehyde 50% Phenol Form.—	Silica	114	240	150	120	90	15.1	½% NH_4BF_4 Added
2.	50% Phenol Furfural 40% Phenol Form.—	Silica	114	300	180	120	90	11.0	½% NH_4BF_4 Added
3.	60% Urea Formaldehyde 50% Phenol Form.—	Silica	114.	480	270	180	120	4.3	½% NH_4BF_4 Added
4.	50% Phenol Furfural 50% Phenol Form.—	Silica	62	300	—	120	90	10.2	
5.	50% Phenol Furfural 50% Phenol Form.—	Silica	114	300	—	120	90	12.1	
6.	50% Phenol Furfural 50% Phenol Form.—	Silica 50% Silica	90 114	300	—	120	90	11.3	
7.	50% Phenol Furfural 50% Phenol Form.—	50% Zircon	129	300	—	—	—	14.4	½% NH_4BF_4 Added
8.	50% Phenol Furfural 25% Phenol Form.—	Zircon	129	300	—	—	—	20.2	½% NH_4BF_4 Added
9.	75% Phenol Furfural 25% Phenol Form.—	Silica	114	360	210	—	—	9.0	
10.	75% Phenol Furfural	Silica	114	360	210	—	—	8.1	1% NH_4BF_4 Added
	Type B								
11.	Phenol Formaldehyde	Silica	90	180	—	110	75	25.0	

* Transverse strength specimens made at 400 F and tested at room temperature.

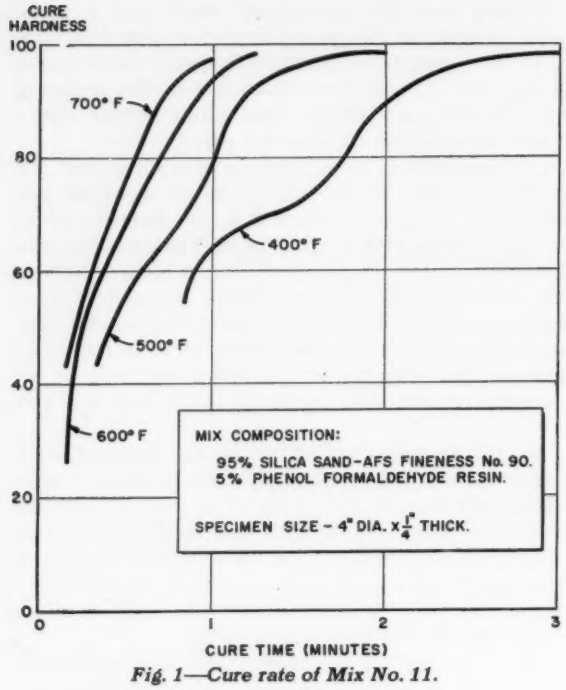


Fig. 1—Cure rate of Mix No. 11.

tions. In addition, the mold cavity is sometimes flushed with sulphur dioxide gas prior to pouring.

a. **Procedure**—The sand-resin mixes were prepared in a laboratory muller, using kerosene as a wetting agent. The mixes were placed in a dump box and shell molds made, using the magnesium pattern plates illustrated in Figs. 2 and 3. The pattern in Fig. 2 was designed with a heavy section, a 2-in. cube, and a thin-walled section. The pattern in Fig. 3 was designed to produce a casting with a heavy section, or, with a shell core, a thin-walled section. Magnesium was used as pattern plate material due to its light weight, machinability, and availability. The patterns were lubricated with silicone grease and silicone emulsions. The shells were made using pattern temperatures of 400-500 F and contact times of 7 to 20 sec to produce a shell buildup of 1/8 in. to 3/16 in. The shells were cured in a resistance furnace at 400-600 F for a time determined by the hot cure test.

TABLE 2—EFFECT OF CURE TIME AND TEMPERATURE ON TRANSVERSE STRENGTH OF MIX NO. 11*

Cure Time, Sec	Transverse Strength, psi, for Cure Temperatures of 400 F	500 F	600 F
30	—	—	11.8
60	9.8	20.5	25.5
90	—	—	24.3
120	15.7	24.1	25.9
150	—	—	25.6
180	22.7	24.6	25.4
240	25.2	24.1	25.9
300	25.2	24.5	25.0
360	25.3	26.4	22.2
420	24.6	25.3	—
480	23.9	24.5	—
540	24.9	23.5	—
600	18.9	—	—

* See Table 1 for composition.

TABLE 3—EFFECT OF CURE TIME AND TEMPERATURE ON THE PERMEABILITY OF MIX NO. 11*

Cure Time, Sec	Permeability For Cure Temperatures of 400 F	500 F	600 F
30	—	—	134
60	—	138	148
90	—	—	154
105	133	—	—
120	141	138	145
150	133	—	141
180	137	136	139
210	124	—	153
240	135	140	151
270	122	—	—
300	141	141	147
330	127	—	—
360	128	144	135
420	132	155	—
480	131	141	—
540	152	136	—

* See Table 1 for composition.

TABLE 4—EFFECT OF CURE TIME AND TEMPERATURE ON RIGIDITY OF MIX NO. 11*

Cure Time, Sec	Angle of Slump, Degrees, at 400 F	500 F	600 F
35	—	—	1
45	—	—	1
60	—	2	0
75	—	—	0
90	—	1	
100	—	<1	
120	2	0	
150	1		
180	0		

* See Table 1 for composition.

The cured shells were stripped from the pattern plate, sprayed with mold wash if used, assembled with clamps, bolts, or masking tape, positioned on the pouring floor without backing material, and poured with AZ92A alloy (Mg-9Al-2Zn). Castings were also made with AM80A (Mg-8Al-0.2Mn), AZ-31B (Mg-3Al-1Zn), ZK51XA (Mg-5Zn-1Zr), and EK-30XA (Mg-3 rare earth metals-0.25Zr) alloys.

The AZ92A alloy was melted, chlorinated, superheated to refine the grain, and cooled quickly to the pouring temperature. The shell mold castings were allowed to cool to room temperature before being shaken out. The castings were examined for surface smoothness, discoloration, adhering sand, and protection from burning.

b. **Discussion**—The results of the inhibitor evaluation are listed in Table 5. Three methods of inhibitor application were used. Sulphur dioxide gas was used to flush the mold cavity prior to pouring. Boric acid, Dow No. 181 agent (boric acid plus sulphur), potassium borofluoride, magnesium oxide, lampblack, molybdenum disulphide, and a rubber compound containing boron and fluorine were used as mold washes. Inhibitors added to the sand-resin mixes were boric acid, Dow No. 181 agent, potassium borofluoride, ammonium borofluoride, ammonium sulphate, and ammonium fluosilicate.

A flush of the mold cavity with sulphur dioxide prevented burning of the 2-in. cubes, but did not prevent the metal-sand reaction. No additional protection was detected when the sulphur dioxide flush



Fig. 2—Test pattern and shell used in inhibitor evaluation. Shell mold size 10 in. x 12 in.

was used with the other inhibitors. Of the mold washes, boric acid, Dow No. 181 agent, and the rubber compound gave satisfactory results. Ammonium borofluoride was the only satisfactory addition agent. Its use resulted in a slight discoloration of the surface, but there was no evidence of burning or the metal-sand reaction. The other addition agents either did not inhibit the metal-sand reaction, or they caused a roughening of the casting surface along with pronounced surface discoloration.

Boric acid additions to the sand-resin mix had a weakening effect on the strength of the shells, so that sound shells could not be made.

Hot cure and transverse strength tests were carried out to determine the effects of a 1 pct addition of ammonium borofluoride on the curing characteristics and strength of the sand-resin mix. There was no apparent effect on the cure rate of the resin, but the transverse strength was reduced 10 pct by a 1 pct addition of the inhibitor.

Shell Core Breakdown Evaluation

The reported poor breakdown properties of phenol formaldehyde-bonded shell molds and the low heat capacity and relatively low pouring temperature of magnesium alloys made it necessary to investigate sand-resin mixes for breakdown. Resin formulations and concentrations were varied, and inhibitor additions made to achieve better breakdown of the shell cores.

a. **Procedure**—A simple core blower was designed and shell cores were made using the mixes listed in Table 6. The shell cores were pasted into the shell molds illustrated in Fig. 3 and the mold halves were assembled with clamps or tape. The shell molds were placed on the pouring floor without backing and were poured with AZ92A alloy at 1400 F. The castings were allowed to cool to room temperature and were shaken out. The breakdown of the shell cores was rated qualitatively as indicated in Table 6.

b. **Discussion** — Shell cores bonded with 5 pct phenol formaldehyde resin had a high retained strength and were very difficult to shake out. The



Fig. 3—Test pattern used in inhibitor and shell core breakdown evaluation. Shell mold size 8 in. x 8 in.

same was true for 5 per cent alkyd type resin mixes and 5 pct of 50 pct phenol formaldehyde—50 pct phenol furfural resin mixes. These mixes had breakdown ratings of 1. A conventional urea formaldehyde-bonded core made solid had a breakdown rating of 3. The binder was less than 50 pct burned out but the retained strength was low. Sand-resin mixes bonded with 3 pct of 50 pct phenol formaldehyde—50 pct phenol furfural resin and 3 pct and 5 pct of 40 pct phenol formaldehyde—60 pct urea formaldehyde resin had breakdown ratings of 3 to 5, with a rating of 5 denoting complete burn out of binder. Shell cores could not be made with sand-resin mixes containing commercially available urea formaldehyde, melamine formaldehyde, and melamine-urea formaldehyde resins.

Metallurgy of Shell Cast Magnesium

The insulating properties of silica sand shell molds have been reported by Ames, Donner, and Kahn.[3]

TABLE 5—INHIBITOR EVALUATION FOR SHELL MOLDING AZ92A ALLOY

Inhibitor	Application	Casting Evaluation	
		Surface Color	Surface Smoothness
None	—	Black	Poor
SO_2	Flush	Black	Poor
H_3BO_3	Wash	Silver Bronze	Good
Dow No. 181 Agent	Wash	Silver Bronze	Good
KBF_4	Wash	Gray-White	Fair
Rubber Compound	Wash	Blue-Black	Good
1% KBF_4	Addition Agent	Gray-White	Fair
1% NH_4BF_4	Addition Agent	Slight Gray	Good
1% $(NH_4)_2SiF_6$	Addition Agent	Bronze	Good to Poor
1% $(NH_4)_2SO_4$	Addition Agent	Black	Fair

Note: Inhibitor additions were made to mix No. 11. Pouring temperature was 1450 F.

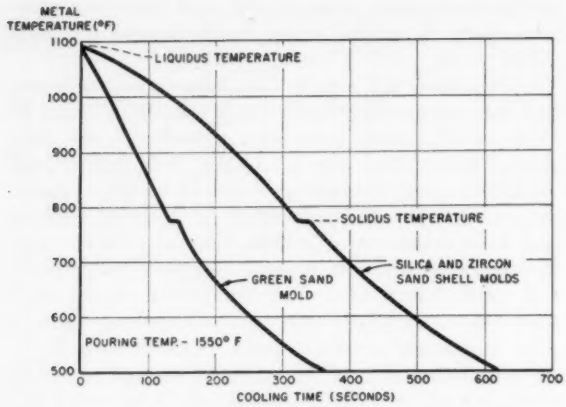


Fig. 4—Cooling rate of AZ92A alloy in green sand molds and unbacked shell molds.

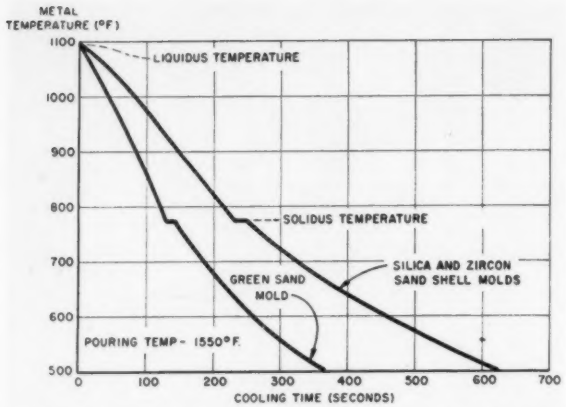


Fig. 5—Cooling rate of AZ92A alloy in green sand molds and backed shell molds.

They also reported that shell molds made with zircon sands produced finer grain size in aluminum and tin bronze alloys due to the greater rate of heat extraction over silica sand. Chamberlin and Mezoff[11] reported that oil-bonded conventional cores made with zircon sands and steel shot showed no significant difference in cooling rate, using magnesium alloys, when compared to silica sand cores. These apparently conflicting data prompted a study of the cooling rates and grain sizes of AZ92A alloy cast in green sand molds, silica sand shell molds, and zircon sand shell molds.

a. **Procedure**—The shell molds illustrated in Fig. 3 were made with silica sand and zircon sand. Both mixes were bonded with 5 pct of 50 pct phenol formaldehyde—50 pct phenol furfural resin and contained ½ pct added ammonium borofluoride as an inhibitor. One of the shell mold castings was used as a pattern to make green sand molds. Thermocouples were placed in the mold cavities of the green sand and shell molds. Metal temperatures were recorded by a high speed recorder.

Virgin melts of AZ92A alloy were made, chlorinated, superheated, cooled quickly to either 1450 or 1550 F, and poured into the molds. All the molds were poured from the same crucible. The castings were shaken out and sections 1 in. × 1 in. × ½ in. were cut from the center of the castings for metallographic study.

The shell molds were poured both backed with dry heap sand and unbacked.

b. **Discussion**—Typical cooling curves obtained in this phase of the investigation are illustrated in Figs. 4, 5, and 6. In Fig. 4, zero time is taken at the liquidus temperature since all three molds cooled from the pouring temperature, 1550 F, to the liquidus, 1095 F, in approximately the same time. The cooling rate to the liquidus was too fast to be recorded. The green sand casting cooled faster through the liquidus to solidus range than the unbacked silica sand and zircon sand shell mold castings, which cooled at the same rate. Although the cooling rates for the silica sand and zircon sand shell mold castings were the same, the average grain size of the zircon sand shell mold castings was .014 in. compared to .020 in. for the silica sand shell mold castings. The green sand castings had an average grain size of .007 in. When the shell molds were backed with dry heap sand and poured at 1550 F, the cooling through the liquidus to solidus range was faster than

TABLE 6—SHELL CORE BREAKDOWN EVALUATION*

Mix No.	% Resin	Resin Type	Inhibitor Additions	Breakdown Ratings**
A	5	Phenol Formaldehyde	—	1
B	5	" "	0.5% NH_4BF_4	1
C	3	" "	0.5% NH_4BF_4	1-2
E	5	50% Phenol Formaldehyde—50% Phenol Furfural	—	1
F	5	" " " "	0.5% NH_4BF_4	1-2
G	3	" " " "	0.5% NH_4BF_4	3-4
H	5	40% Phenol Formaldehyde—60% Urea Formaldehyde	—	2-4
J	3	" " " "	—	3-5
P	5	Alkyd Type Resin	0.5% NH_4BF_4	1
S	5	25% Phenol Formaldehyde—75% Phenol Furfural	—	3
X***	—	Urea Formaldehyde		3

* Pouring Temperature Used—1400 F.

** Breakdown Ratings: 1. Binder less than 50% Burned Out—Retained Strength High.
2. Binder more than 50% Burned Out—Retained Strength High.
3. Binder less than 50% Burned Out—Retained Strength Low.
4. Binder more than 50% Burned Out—Retained Strength Low.
5. Binder completely burned out—No Retained Strength.

*** Mix X was a conventional urea-bonded production core sand mix. Core was made solid.

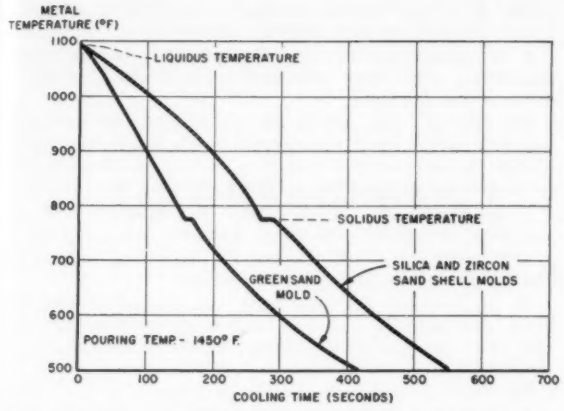


Fig. 6—Cooling rate of AZ92A alloy in green sand molds and unbacked shell molds.

for unbacked shell molds, indicating that the shell molds were saturated with heat before the metal was completely solidified. This faster cooling rate resulted in finer average grain sizes for the backed shell mold castings—.014 in. for silica sand shell mold castings, and .009 in. for zircon sand shell mold castings.

When the pouring temperature was decreased to 1450 F, the cooling times through the liquidus to solidus range were decreased for the shell molds, and the average grain sizes were comparable to the green sand castings poured at 1450 F. There was no change in average grain size between backed and unbacked shells poured at 1450 F, although there was a slight difference in the cooling rates.

These data tend to resolve the apparent conflict between the results cited earlier by Chamberlin and Mezoff, and Ames, Donner, and Kahn. Chamberlin and Mezoff reported cooling rates while Ames, Donner, and Kahn reported grain sizes. Apparently there is some factor other than cooling rate operating to produce a finer grain size in the zircon sand shell mold castings poured at 1550 F. It may be possible that there was an instantaneous difference in the cooling rates of the metal at the liquidus or in the degree of undercooling caused by the silica and zircon sands which was beyond the sensitivity of the high speed recorder.

TABLE 7—EFFECT OF MOLD MATERIALS AND CASTING TEMPERATURE ON COOLING RATE AND GRAIN SIZE OF AZ92A ALLOY

Mold Material and Type	No. of Tests	Pouring Temp. °F	Cooling Time, Sec, For Liquidus to 500F	Liquidus to Solidus	Average Grain Diameter, 0.001 in.
Green Sand	6	1550	360	135	7
Silica Sand Shell	3	1550	600	315	20
Zircon Sand Shell	3	1550	600	315	14
Silica Sand Shell (Backed with Sand)	3	1550	605	220	14
Zircon Sand Shell (Backed with Sand)	3	1550	585	190	9
Green Sand	2	1450	405	150	10
Silica Sand Shell	1	1450	540	270	8
Zircon Sand Shell	1	1450	540	250	6
Silica Sand Shell (Backed with Sand)	1	1450	640	230	9
Zircon Sand Shell (Backed with Sand)	1	1450	—	—	8

TABLE 8—TENSILE PROPERTIES AND GRAIN SIZES OF AZ92A-T6 CAST IN SHELL MOLDS, GREEN SAND MOLDS, AND PERMANENT MOLDS*

Casting Method	No. of Tests	%** E	Y.S. 1000 psi	T.S. 1000 psi	Average Grain Diameter, 0.001 in.
Shell Mold	10	1	25.2	39.2	2
Green Sand	10	1	23.6	33.1	4
Permanent Mold	10	1	22.3	36.7	10

* Specimens Machined from Castings
** Per cent elongation in 2 in.

Application of Shell Molding

A small production casting which had been made in green sand and permanent molds was made in shell molds to try to obtain a comparison of the three molding methods.

a. **Procedure** — Magnesium pattern plates were made to produce the shells for the production casting. A urea formaldehyde-bonded conventional core (Fig. 7) was used with the shell molds to produce the casting. The shell molds were made with a 5 per cent phenol formaldehyde, 95 pct silica sand of A.F.S. grain fineness No. 90, mix to which ½ pct ammonium borofluoride was added. The shells were assembled with bolts and clamps and backed with dry heap sand, then poured with AZ92A alloy at 1450 F.

Five castings made in shell molds were given a solution heat treatment at 770 F for 16 hr and were aged at 425 F for 5 hr (T6 condition) with five castings each produced in green sand and permanent molds. Test bars were cut from each of the castings and were radiographed prior to being tested in tension. The fractures of the tested bars were examined and the bars were then studied metallographically.

b. **Discussion**—The tensile properties of the castings produced by shell molding, green sand molding, and permanent molding are listed in Table 8. The shell molded castings had a higher ultimate tensile

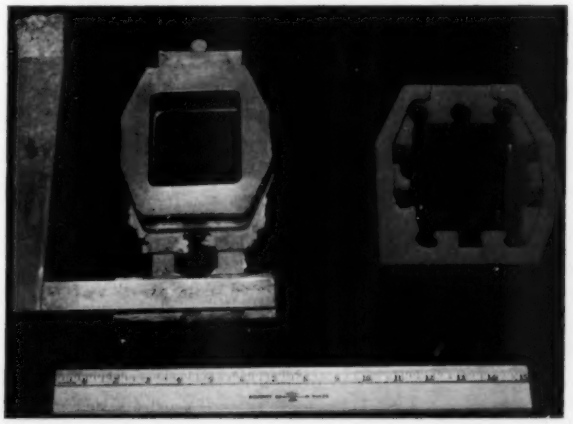


Fig. 7—Production casting made in shell mold (left) and urea formaldehyde-bonded core (right).

strength than the green sand castings and the permanent mold castings. The yield strength for the shell mold castings was also higher. The elongation for all castings was 1 pct. The average grain size for the shell mold castings was .002 in. compared to .004 in. for the green sand and .010 in. for the permanent mold castings.

Radiographs of the bars tested revealed some porosity in the permanent mold bars.

No conclusions can be drawn from these limited data because of the differences in metal treatment and gating and risering.

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DISCUSSION

Chairman: F. P. STRIETER, The Dow Chemical Co., Midland, Mich.

Recorder: G. R. GARDNER, Aluminum Company of America, Cleveland.

JOHN ALLEN: [1] What practical advantages are there in shell mold casting magnesium as compared with present better known methods?

MR. SHEPTAK: The advantages of shell mold casting magnesium will depend on the design specifications of the part being cast, the quantity of castings being made, the extent to which the application is to be engineered, and the alloy in which it is to be cast. All or some of the benefits of shell mold casting of other alloys cited in the literature should apply equally to magnesium—smooth surface finishes, close tolerances, better yield of metal, reduction of the amount of sand required per mold, the ability to use unskilled or semi-skilled labor to make complex molds as easily as simple molds, the ability to use mass production techniques, the increased utilization of floor space, and the ability to store molds. Each part being considered for shell molding will have to be compared to the part supplied as a die casting, green-sand casting, permanent mold casting, dry sand casting, or plaster casting and should show a reduction in cost per piece, or better surface finish, or a more precise casting, or some other advantage before the shell molding process is chosen.

J. W. MEIER: [2] What were the properties of the various magnesium alloy types compared to properties obtained by other casting processes?

MR. SHEPTAK: The only property comparison we have made was with AZ92A which was reported in this paper. We hope to include a study of the properties of various alloys as shell mold castings in our future investigations on the process.

D. L. COLWELL: [3] In Table 8, were the permanent mold bars cut from castings and the others separately cast test bars?

MR. SHEPTAK: All of the test bar properties reported in Table 8 were from bars cut from castings.

W. M. SINCLAIR: [4] In your work with phenol formaldehyde resins and 50/50 phenol formaldehyde-phenol furfural resins, was there any noticeable difference in the breakdown rate of the bond?

MR. SHEPTAK: While the results in Table 6 do not show a great difference between the breakdown of 5 per cent mixes of the two resins, more recent work indicates that the 50/50 phenol formaldehyde-phenol furfural bonded shell molds and cores do breakdown at a faster rate than phenol formaldehyde bonded shell molds and cores.

W. W. COLEMAN: [5] Have you established any relationship between the nature of backing materials, the conditions of backing, and the surface finish of the castings?

MR. SHEPTAK: Our work on backing materials was limited to the use of molding sand, but it would be interesting to study the effects of steel shot and other materials and see if any relationship exists. In the casting that we used for cooling rate studies, we saw no difference in surface finish between backed and unbacked castings.

S. A. KUNDRAT: [6] What was the effect of fineness of sand on the surface of the casting?

MR. SHEPTAK: Recent work has indicated that with a sand of a given fineness, the surface finish of the casting will depend on the alloy, the section thickness, the size of the casting, the rigging, and the pouring temperature. In general, the larger the casting and the heavier the wall, up to a given thickness which will depend on other factors, the better the surface finish that can be obtained with a sand of a given fineness.

R. J. PETTO, JR.: [7] What was the effect of the shell-mold sand-grain size on cooling rate?

MR. SHEPTAK: All of our cooling rate work was done with a sand of AFS Fineness No. 114, so we do not know the effect of varying sand-grain size. However, since the resin particles coat the individual sand grains in bonding the shell molds, it is not anticipated that a sand-grain size in the range of from AFS Fineness No. 100 to No. 155 would have a significant effect on the cooling rate.

MR. SHEPTAK *(Author's Closure):* The author wishes to thank the people who have contributed to the discussion of this paper. The nature of the questions asked indicate that there is a need for more work on the variables which might effect the quality of a shell mold casting. We have reported on what we considered the most important variables—inhibitors, breakdown, tests for evaluating mixes, and cooling rates. Our results have shown that the shell molding process can be used for magnesium but, shell molding should be used for a casting only if it offers the foundry some advantage over other molding processes.

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DEVELOPMENT OF FOUNDRY SAND RECLAMATION

By

Clifford E. Wenninger*

This paper is intended as a general discussion of (1) historical developments, (2) fundamental concepts, and (3) past reclamation methods. It is hoped the information presented herein will provide a suitable background for the other papers on modern reclamation methods in this Symposium, pp. 191-227.

Historical Notes

In 1926, Pat Dwyer[1] prepared a series of articles summarizing the status of sand reclamation at that time. His material was obtained from correspondence and interviews with foundrymen who had tried means and methods to extend the useful life of their old sands.

Dwyer found confusion to be existent with respect to just what constituted foundry sand reclamation. Some foundrymen deemed rejuvenation of old sand heaps with new sand/clay additions to be reclamation. Others felt the mechanical reconditioning of an old sand mass to remove lumps, foreign debris, and fines, should be accepted as reclamation. Only a few conceived reclamation to be a matter of treating individual grains to restore them to a physical state approximating that of new sand grains.

Further, Dwyer found it almost impossible to discuss reclamation without becoming involved in related discussions on the complexities inherent in the selection, preparation, testing, and control of sands in general. Seemingly, the average foundryman was interested in sand conservation and/or reclamation to the same degree he would be interested in any feature capable of reducing costs or increasing production. However, and to an equal degree, he was adverse to experimentation or changing established foundry practice in the absence of more basic information and a prior elimination of existing sand problems. In other words, the foundryman felt that reclamation had little to offer if it tended to further complicate an already complex situation. In the face of this more-or-less industry-wide attitude, Dwyer concluded that a more general promotion of sand reclamation would have to await (a) the development and distribution of additional information on foundry sands and sand controls, and (b) a parallel development of equipment and methods better able to satisfy the economical/technical requirements of an average foundry.

Interim Years

Dwyer assumed the mechanical development of reclamation would be paralleled by a related technical development of information on sand problems in general, and reclamation problems in particular. Unfortunately, and for a number of years, an industry-wide concern with the creation of a more satisfactory general sand technology actually delayed the development of fundamental reclamation information and data. Because of this delay, technical developments failed to keep ahead of mechanical developments, and, in turn, a succession of reclamation methods were evolved with fundamental flaws inherent in their designs and operations. Some methods, or systems, were quite elaborate and, upon being found unsatisfactory, they were abandoned with considerable losses in time, effort, and money. In the absence of explanatory fundamental facts, the poor performance of such systems seemed to confirm the average foundryman's skepticism with respect to the future of foundry sand reclamation.

It was not until around 1938 that the work of different investigators initiated the development of successful wet-reclamation methods.[3,6,7] Shortly thereafter, several types of thermal reclamation systems made their appearance.[4,5] In 1948, a pneumatic installation was described in the literature.[8]

Most of these latter-day methods and systems were accepted as being satisfactory in accordance with the conditions and requirements peculiar to the particular foundries in which they were installed. However, none seemed to be sufficiently free of technical, economical, or operational limitations to win a ready acceptance by the industry as a whole. Generally speaking, most foundrymen continued to view reclamation as a potential trouble-making innovation in the absence of proven technical information and data.

Because of the more strict limitations applicable to

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their re-using old sands, steel foundries have pioneered in the development of sand reclamation from the very beginning. Therefore, it was natural for the Steel Founders' Society to recognize the need for additional basic information, and to authorize a research project to study reclamation in 1948. The project was initiated to (a) determine the most detrimental contaminant or relationship influencing the re-use of old sands, and (b) to evaluate the comparative merits of different methods in reclaiming clay-containing sands for re-use in molding mixtures. The published results served to clarify some controversial questions, and inspired a more considerate attitude towards reclamation by steel foundrymen in general.[9]

In 1950, another investigation sponsored by a manufacturer of foundry equipment culminated in the creation of a new reclamation method based upon a pneumatic dry-scrubbing of old sands and individual sand grains.[10] The successful commercial development of this method has been completed only in recent months. It is understood that other papers on this program will describe the method in greater detail.

Obviously, no attempt has been made to provide a detailed resume of historical development. Rather, only sufficient background material has been presented to explain why development has been prolonged over so many years. Only in quite recent times has the pressure of economic factors been combined with a more general distribution of technical knowledge to give reclamation a new status in the eyes of Mr. Average Foundryman. In fact, the organization of this Symposium can be interpreted as indicating that foundry sand reclamation has come of age.

Fundamental Concepts

To project some fundamental concepts for sand reclamation, it becomes necessary to consider what happens to a system sand when it is subjected to repeated re-use. At some time or other most foundries try to repeatedly re-use their old sand in order to reduce their expenditures for new sand. Unfortunately, any initial period of success is invariably followed by a progressive deterioration in workability and casting finish with the passing of time. Eventually, savings effected by re-using the old sand become questionable as increasing losses are suffered in production and cleaning operations. Finally most foundries find it expedient to revert to either continuously diluting the old system with fresh sand additions, or to using new sand facing with old sand backing mixtures.

The progressive deterioration results from the accumulative effects of physical changes (a) within a sand mass as a whole, and (b) upon the surfaces of individual sand grains. In preparing foundry sand mixtures, individual grains are provided with coatings of bond. In successive cycles through a system, such coatings become thicker with each added layer of fresh bond. Eventually, most grains become encased in "shells" of old bond—and the "shells" tend to become hard and brittle with the passing of time and repeated subjection to high temperatures.

On one hand, the continuous artificial enlargement of individual grains creates a sand mass with an increasingly greater number of grains being retained on the coarser sieves of a distribution analysis. On the other hand, under mechanical and thermal stresses, the embrittled coatings on some grains crack and separate to produce a multitude of fine particles. Thus, over a period of time, a progressive change in mass grain distribution results in more and more fines becoming dispersed throughout a matrix containing more and more out-size grains (Fig. 1).

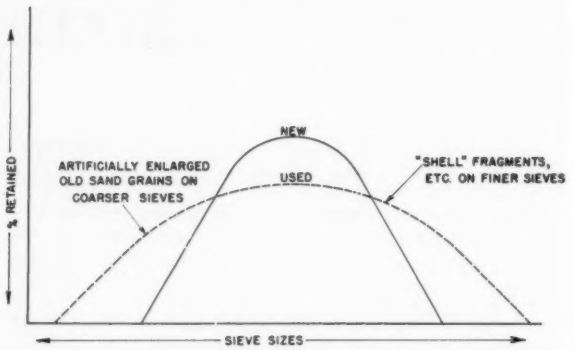


Fig. 1—Generalized comparison of grain distribution repeatedly used sand vs original new sand.

The creation and presence of compound grains also contributes to the coarsening of a sand mass. Compound grains result when a number of small grains bond particles become cemented together to create large irregular shapes possessing uncertain physical stability in the presence of mechanical and thermal forces.

Obviously, as the original grain distribution changes with repeated re-use, the total surface area of the sand mass increases by leaps and bounds. To compensate for increasing surface area, and to maintain workability, it becomes necessary to increase strength and moisture levels with each additional cycle through the system. Eventually, a point is reached where workability and castability can no longer be maintained by simply controlling strength and moisture levels, and the sand must either be discarded or subjected to other treatment.

An important physical change with respect to individual sand grains and their contribution to mass deterioration, is the creation of "pickle" sand grains; grains having surfaces covered with wart-like projections because of small particles becoming embedded in the old-bond coatings. Such projections not only serve to greatly increase the total surface area of each grain but, by interfering with the movement of one grain past another, the projections decrease mass flowability and response to ramming energy. When the resistance offered by one or two grains is multiplied by thousands of grains, one comes to understand why old used sands are so difficult to ram into compact uniform mold surfaces. Further, any attempt to use increased ramming energy simply transfers trouble from the grains to the sand mass as a whole. As ramming energy increases, more particles are broken from the "shells" of old bond, more fines are created, and the sand mass suffers accordingly (Fig. 2).

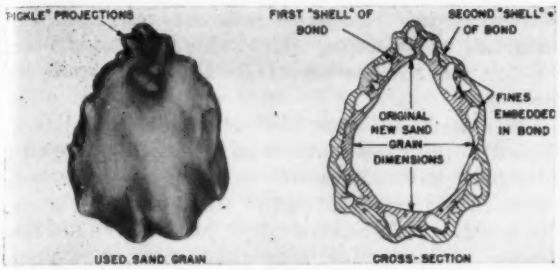


Fig. 2—Note: "Shell" created by successive layers of bond, and "pickle" surface resulting from particles becoming embedded in bond.

Rejuvenation by Dilution

An old system sand can be kept within control if it receives daily additions of fresh sand either in the form of core sands at shake-out or direct new sand additions to the system. The incoming fresh sand serves to constantly dilute the system, thus offsetting the influence of the old sand still remaining in the system. If dilution is closely supervised in accordance with changing production conditions, the accumulative effects of the various physical changes are never permitted to reach a point where they can contribute to an epidemic of casting defects. However, for every ton of fresh sand added, a ton of old sand must be removed to keep a system at a more-or-less constant tonnage level. The loading and disposal of waste sand can become a very expensive item in operating costs. In recent years, many foundries have found it necessary to haul their old sands a considerable distance to reach permissible dumping areas.

Reconditioning

Some foundries have been moderately successful in extending the useful life of their old sands by resorting to reconditioning systems. In such systems, the old sand is passed over magnetic pulleys, through lump breakers and screens, then aerated to remove fines. Unfortunately, reconditioning treats a sand mass as a whole and fails to materially affect the physical state of individual grains. Though aeration may remove fines and thus reduce total mass surface area, it does little towards smoothing, or removing, the old-bond coatings on individual grains and breaking up compound grain formations. The reconditioned sand may require less moisture and bond in re-use, and it may be somewhat easier to control, yet it cannot perform like new sand because nothing is done to restore the surface condition of old grains to a physical state comparable with the surface condition of new sand grains. Even after a thorough job of reconditioning, most foundries find it necessary to blend reconditioned with new sand in preparing satisfactory facing and mixtures.

It is important to note that reconditioning is applicable to a sand mass. True reclamation requires that a uniform and controllable amount of work be performed on each individual grain within the mass. The latter statement is fundamental to modern reclamation theory and practice.

Past Reclamation Methods

In discussing what contributes to the progressive deterioration of an old used sand we have simultaneously defined the physical changes and effects that must be countered by modern reclamation methods. With this information at hand, one can understand why some reclamation methods of the past were not too satisfactory.

Dry Mulling Plus Aeration—Some foundries have tried to use sand mullers and an intensive mulling of dry old sand batches to smooth grain surfaces and/or remove "shells" of old bond from individual grains. The dry-mulled sands are subsequently aerated to remove "shell" debris and other fines.[2]

Foundry sand mixers of the intensive type have been designed to coat individual sand grains with plastic materials by combining pressure and frictional forces. They never have been intended for applying impact and abrasion towards the smoothing or removal of the comparatively hard durable "shells" on old sand grains. Though the mechanical stirring and agitation may smooth the coatings on some grains, and the impact and pressure of mullers may crack "shell" segments from other grains, it is almost impossible to treat all grains to a like degree. To be completely satisfactory, small batches must be mulled for lengthy periods of time, and mulling must continue until practically all surface material has been removed from all grains. Where mulling removes only segments of the grain coatings, the remaining rough irregular surfaces are just as unsatisfactory as the initial "pickle" surfaces on unmulled sand grains.

Aeration subsequent to dry-mulling is also a problem. Thorough air-separation requires that each grain and particle be exposed to the separating force—(the air)—in such a manner that each is free to be removed from the sand mass in accordance with weight, size, and velocity limitations. Maximum effectiveness requires that a uniformly thin curtain of sand fall at a steady rate through a constant-velocity airstream. When satisfactory aeration conditions are obtained, many "shell" fragments still remain with the sand mass because they have not been reduced to air-floatable proportions, and, therefore, cannot be entrained within and removed by an air-stream. Such fragments, because of their non-symmetrical shapes, interfere with the packing of true sand grains and continue to exert undesirable physical effects when the reclaimed sand is returned for re-use.

Because of the foregoing factors, and the wear and power consumption attendant to lengthy mulling periods, both efficiency and economy suffer in comparison with other reclamation methods. Though the writer's firm manufactures intensive sand mullers, our investigations have led us to devise other equipment specifically designed to meet the basic requirements of reclamation.

Thermal Methods—Thermal or ignition methods are being successfully applied to reclaiming used foundry core sands—sands bonded only with carbonaceous materials such as cereals and core oils.[4] They have not been too successful when applied to mixtures of core and molding sands containing varying amounts of clay binders.

It is rather difficult to heat all individual grains within a sand mass to the same degree in a continuous and uniform manner. When clay-containing sands are being thermally reclaimed, some grains receive just enough heat units to dehydrate, bake, and/or embrittle their "shells." Other grains, at higher temperatures, have their coatings more firmly attached to their surfaces because traces of basic oxides become fluxing agents and promote an incipient fusion of coatings to grains. Carbonaceous materials are combustible, and create heat units.

Consequently, the ignition and destruction of some carbonaceous material may serve to free clay from some grains. On the other hand, a concentration of heat units can contribute to a firmer adhesion of clay on other sand grains. All of this results in some sand grains emerging from thermal reclamation with their clay shells still present to maintain the influence of enlarged and irregular grain surfaces. Other grains emerge with their coatings susceptible to cracking and spalling when they encounter mechanical and thermal forces during subsequent cooling and re-use.

If the embrittled "shells" separate from their respective grains before or during the mulling of a sand mixture, additional bond and moisture is required to compensate for the presence of innumerable dead clay particles, or "frits." Such dehydrated particles are difficult to wet and possess poor adhesiveness with fresh bond. If the "shells" separate under the influence of thermal shock during pouring operations, adjacent grains are freed from adhesion with bonds and with each other—and surface failure becomes general over a widespread area of a rammed sand face.

To counter the ill effects of clay in thermal reclamation, it has been suggested that (a) the ignited sands be dry-mulled subsequent to ignition,[5] or (b) the clay content be removed prior to ignition. Obviously, the problems of dry-mulling and aerating an ignited sand are essentially the same as for an unignited sand, and, if anything, investigation has shown that ignition prior to mulling may but make matters worse.[9]

If the clay content of mixed sands is removed prior to ignition, it becomes debatable whether or not ignition is essential to satisfactory re-use. Wet-scrubbing of an old sand will remove considerable carbonaceous as well as clay material, and there is evidence to indicate that the complete destruction of carbonaceous coatings on individual sand grains contributes little towards satisfactory re-use in either cores or molds. While the appearance of a washed and ignited sand may be visually pleasing to the eye, the color of a reclaimed sand is not indicative of its ability to duplicate the casting performance of new sand.

Merit of Thermal Reclamation

As stated herebefore, true thermal reclamation has merit when applied only to sands bonded with carbonaceous materials. In most foundries it is difficult to keep core sands separate from molding sands at shake-out, and, if a foundry is going to wash out the clay prior to ignition, they might as well settle for a complete wet reclamation system, especially so since the rising cost of fuel for ignition may seriously effect the economics of thermal reclamation in the not too distant future. This factor alone may restrict thermal methods to low-tonnage high-value installations such as for the reclamation of shell-molding sands, for example.

Wet Methods—Some wet reclamation installations failed to meet expectations in the past because their designers assumed the solvent action of water to be sufficient to remove "shells" from old sand grains. In actuality, it is not enough to just wash an old sand mass. The individual sand grains must be subjected to an intensive wet-scrubbing action. Such an action can be obtained by pumping or mechanically agitating a properly proportioned mixture of old sand and water. Peak efficiency in wet-scrubbing is only obtained when sufficient sand is suspended in water to promote a maximum of impact and abrasion between colliding sand grains. The "shells" possess a certain amount of durability, and it is the continuous impact and abrasion that smoothes and reduces the thickness of old-bond coatings, or almost removes them entirely from the sand grains. By controlling the density of a sand/water mixture and the time devoted to scrubbing, a more-or-less uniform amount of work can be performed on each grain to a degree compatible with the conditions under which the sand is to be re-used.

Modern wet reclamation methods rely upon wet-scrubbing to reduce and/or remove the "shells" from old sand grains. The wet-scrubbing is then followed by a wet separation to remove "shell" debris and other fines. Wet separation, in turn, may be coincident with, or followed by, a wet classification to obtain some desired range in grain classification. After being processed, the reclaimed sands must be drained and dried prior to being returned for re-use in the foundry. If the successive stages in processing are properly controlled, most wet-reclaimed sands can be made to perform as good, or better, than new sands in most applications.

From a fundamental viewpoint, wet reclamation methods have become acceptable. Work is performed both upon individual grains and a sand mass as a whole. Over-size grains are reduced, compound grains are destroyed, and the grain distribution of the old mass is adjusted to approximate that of the new sand.

The dark color associated with reclaimed sands is chiefly derived from material remaining in pits and crevices on individual grains. Such material can serve a useful purpose by rounding out the surface contours of the grains. Grains with symmetrical shapes are more flowable, and less fresh bond is required in filling the pits and interstices common to adjacent rammed grains. It is believed that a more flowable sand with a more consistent grain distribution accounts for most cases where the reclaimed sand seems to perform better than the original new sand.

Some foundrymen object to the presence of the residual material when a reclaimed sand is to be reused in cores. It is debatable whether the residual material is as influential upon core-oil consumption as some believe. Some operating results indicate that, after residual clay has been reduced to less than 1.0 per cent by A.F.S. standards, the fines content of a sand becomes far more influential than its clay content with respect to core-oil consumption in re-use.

By closely controlling the elimination of fines during reclamation, reclaimed sands can be made to exhibit tensile strengths superior to new sands when rebonded with like amounts of bond under like test conditions.

Despite their ability to satisfy fundamental requirements, wet reclamation methods have been found to be unacceptable to some foundries because of economical factors and/or operating features. Such foundries have been prone to await further improvement, or the development of a method more compatible with their particular conditions. Possibly, the recently developed pneumatic method may prove to be better able to satisfy their requirements.

Pneumatic Methods—As noted elsewhere, a pneumatic method for sand reclamation was developed and described in 1948.[8] The method involved entraining sand grains in an air-stream and blasting them against a fixed target. While initially deemed to be successful, it is understood that the method failed to prove satisfactory over a period of time. It is believed that the limited amount of impact and abrasion was insufficient to do much towards smoothing and/or reducing the coatings on heavily-bonded old sand grains.

The recently developed pneumatic method has been specifically designed to meet the same set of fundamental requirements now being met by the wet reclamation methods. The method centers around an air-scrubber unit that parallels and duplicates the functions of a wet-scrubber unit in a wet reclamation system. From a fundamental viewpoint, the only difference between a pneumatic scrubber and a wet scrubber is the respective mediums utilized for suspension and motivation. In one, the promotion and control of impact and abrasion between sand grains is obtained by suspending and motivating the grains in air; in the other, the same thing is accomplished in water.

Results from operating installations indicate the pneumatic method is capable of performing a controllable amount of work on individual sand grains, and returns a product that consistently meets grain distribution specifications. The indications are that the method does satisfy the fundamental requirements that have been projected in discussion. It will be left for other papers in this Symposium to discuss its features in greater detail.

Summation

In the preceding pages consideration has been given to the general development of foundry sand reclamation, the projection of certain fundamental concepts, and the technical faults peculiar to some reclamation methods of the past. Discussion has indicated that, from a fundamental viewpoint, satisfactory reclamation requires:

1. A uniform and controllable amount of work be performed upon individual used sand grains to—
   - (a) break up compound grain formations
   - (b) smooth, reduce, and/or remove the "shells" of old bonds
   - (c) restore grains to about original physical dimensions
   - (d) confer more symmetrical shapes to sub-angular and angular grains
2. A satisfactory means of separation and classification may be applied to a sand mass as a whole to—
   - (a) remove the "shell" debris and other fines created in scrubbing grains.
   - (b) return the reclaimed product with a grain distribution approximating that of the original new sand.

It is hoped that this presentation will contribute towards a better appreciation of the information and data in the papers that follow in this Symposium.

Acknowledgments

The author wishes to thank his associates for help in preparing and editing the material, and particularly wishes to thank Mr. Bruce L. Simpson, President, National Engineering Co., Chicago, for permission to present this paper.

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DISCUSSION

Chairman: J. A. RASSENFOSS, American Steel Foundries, East Chicago, Ind.

Co-Chairman: R. H. JACOBY, The Key Co., East St. Louis.

Recorder: J. B. CAINE, Foundry Consultant, Cincinnati.

D. S. BOSMA:[1] Is more bond required with reclaimed sand than with new sand, when used for cores?

MR. WENNINGER: If residual clays are reduced to about 1.5 per cent or less, and if the fines are controlled, the same tensile strengths can be expected with the same bond addition.

T. S. ABBOTT:[2] Will not the structure of the material filling the pits, if porous, effect the efficiency of the liquid binders?

MR. WENNINGER: Yes, if porous it will adsorb liquid binders. However, this pore material is usually hard and solid. Then, too, the more symmetrical grain shape caused by reclamation decreases the number and shape and depth of the pits, and thus produces a counter-effect in the form of decreased surface area per grain and for the sand mass as a whole.

W. A. DEAN:[3] How applicable is dry reclamation to sand bonded with urea formaldehyde binders?

MR. WENNINGER: It is rather difficult to give a definite answer at this time. Preliminary tests have been satisfactory, but we have no experience with this type of binder in production.

T. A. TARQUINIO:[4] Will air scrubbing make subangular sands more rounded?

MR. WENNINGER: Yes, and experience indicates that a more flowable, more moldable sand results.

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RECLAMATION OF SAND BY PNEUMATIC DRY SCRUBBING

By

Henry W. Meyer*

ABSTRACT

A chronological discourse relating experiences leading to the finding that certain sand grain coating characteristics affect the thermal stability of steel molding sands, and contribute to either good or poor casting results. Proper mulling conditions were found greatly influencing sand grain coating characteristics. Taking advantage of these findings resulted in greater use of reconditioned sand, and later selection of a dry method of sand reclamation employing the principle of pneumatic scrubbing. Complete elimination of new crude sand in an all-purpose steel molding sand mix followed.

Present day cost of new sand, sand mixture ingredients, and the increasing difficulty and expense experienced in the disposal of used sand make it imperative that foundries make greater use of their used or discarded sand. This may be done by either reconditioning or reclaiming used sand.

There is no doubt but that this paper will differ greatly from the other papers presented in this Symposium on Sand Reclamation. Instead of dealing solely with a given sand reclamation method and its benefits, also included is the finding of differences in sand grain coating characteristics and the influence these coating characteristics exert in causing a sand to give either good or poor casting results. Employing the practical applications of the coating characteristics indicative of improved casting results, from the standpoint of decreased defects attributed to molding sand, resulted in a greater use of reconditioned sand; it resulted in decreased use of new sand and later the wise selection of a sand reclamation unit whereby the use of new sand could be completely eliminated from a molding sand mixture.

Reconditioned Sand Defined

The term "reconditioned sand" when used in this paper may be considered used sand that has received some degree of cleaning but not enough to make the sand suitable for use as new sand throughout the foundry and core room. Reconditioning of used sand involves reducing used core and molding sand lumps to individual sand grains, removing metallic particles, sintered clusters of sand grains, excess fines and other tramp and objectionable material. The equipment involved in reconditioning used sand may consist of some form of crushing medium, magnetic separator, vibrating or rotating screens and dust collecting system. Reconditioning of used sand varies with individual foundries and is dependent upon the amount of necessary equipment the foundry may have to perform the task.

Reclaimed Sand Defined

The term "reclaimed sand" is rather loosely used in the foundry, inasmuch as reconditioned sand is frequently called reclaimed sand. Reclaimed sand, from the writer's point of view, is used sand that has been restored to comparable quality of the new sand purchased by a given foundry, and may be satisfactorily substituted for new sand in core and molding sand mixtures without appreciable change in sand practice.

Sand reclamation, regardless of whether the method employed is a wet or dry process, has the same underlying idea of attack—the removal of accumulated coating from around the sand grains, excessive fines or dust and other objectionable materials that have made the sand unusable from the standpoint of quality casting production.

For all practical purposes the foundry contemplating sand reclamation should determine the most suitable sand reclamation method to fit easily and conveniently into its present sand practice. If at all possible, a foundry should not be expected to change its sand practice to any appreciable degree, or it is almost certain that difficulty will be experienced in maintaining casting quality results.

It is important that a foundry give careful consideration to the method of sand reclamation it wishes to choose; this is especially true of the foundry using new crude sand. This foundry may discover it is changing its sand practice to coincide more closely with that of new washed sand and losing the benefits derived from a small per cent of natural clay content or sand grain coating, and the use of lesser amounts of binding material. This latter experience costwise is of great importance to the foundry preparing 1000 to 1500 tons of sand daily.

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The foundry purchasing new washed sand can more readily measure the effectiveness and benefits to be experienced from a sand reclamation unit, than can the foundry that has established a sand practice involving the use of new crude sand. Basis for the preceding statement rests upon the belief that it is impossible to clean used sand to a degree greater than the original new washed sand quality. The foundry using washed sand can measure the degree of undercleaning, change in sand grain distribution and difference in casting quality results obtained from a reclaimed sand by comparing data and results with its current washed sand practice.

The foundry using new crude sand, when evaluating the effectiveness of a sand reclamation unit, is confronted with more than the problem of determining if the sand grain distribution will be changed or casting quality reduced. It must also determine if the used sand can be returned to approximate new crude sand quality and the reclamation process halted, so as not to lose the benefits of the use of a lesser quantity of binding material and other desirable characteristics derived from the natural clay content and sand grain coating present in crude new sand.

The Commonwealth Plant of General Steel Castings Corporation has long been a user of new crude sand and is aware of the benefits derived from its use. Even though sand practices assuring a conservative use of new sand, a minimum amount of binding material and quality casting results were established, the total new sand consumed daily was rather large, due to the quantity of sand prepared each day. In a chronological manner experiences will be related, which lead to a more satisfactory use of reconditioned sand and later the substitution of pneumatic dry scrubbed reclaimed sand for new sand in a molding sand mixture which constituted approximately 80 per cent of all sand prepared for daily use.

All-Purpose Molding Sand Mixture

An all-purpose molding sand mixture in which used or reconditioned sand predominates, capable of being used as mold facing as well as backing, has been in successful use since September 1946. This mixture has given equal or better results than those obtained when using new sand mold facing. Costwise, the mixture is so formulated that it is comparable to the total sand cost in the mold when using a combination of relatively expensive new sand mold facing and a low cost backing sand mixture.

Elevated temperature testing data and correlated casting results were used as a guide in the formulation of all-purpose molding sand mixtures, as well as all subsequent mixture changes. The thermal shock tests which play an important part in sand mixture formulation are made by exposing 1⅛-in. diam x 2-in. long test specimens at 2500 F temperature in the Dietert dilatometer and observing the ability the sand possesses to withstand the thermal shock. The thermal shock characteristics have been found a positive guide to the degree of burned-on sand or metal penetration, veining, scabbing and erosion that can be expected from a given sand mixture formulation. Ideal results are those where the sand specimen remains free of

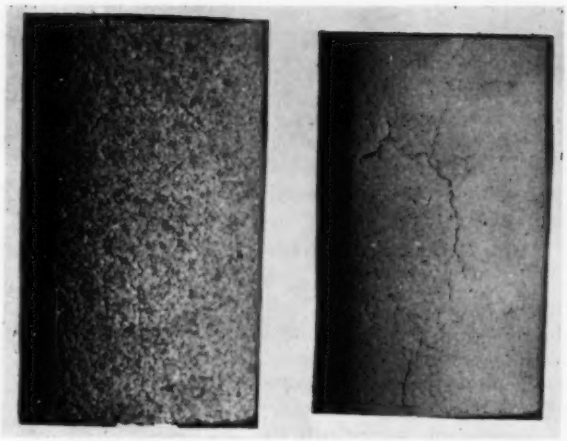


Fig. 1—Photo of test specimen having good and poor thermal shock characteristics.

cracking, crazing and spalling. Figure 1 contains photographs of specimen having good and poor thermal shock test characteristics, respectively.

The initial all-purpose molding sand mixture contained 50 per cent new crude sand; within 2 years the amount of new sand was gradually reduced to 20 per cent. This practice continued for 2½ years. The pouring weight of castings produced during this period of time varied from a few pounds up to 85,000 lb. Naturally, after 4½ successful years' use of this practice, there was little doubt that at any future time would any appreciable difficulty arise which could not be readily remedied.

In the early part of 1951, it was observed the thermal shock characteristics of the all-purpose molding sand mixture were slowly deteriorating. As the condition became more apparent, the new sand ratio in the mix was increased, and the deterioration halted. However, the thermal shock test characteristics were not improved. The magnitude of this problem may be more easily realized when one becomes familiar with the fact that a 10 per cent increase in use of new sand on the respective rate of production at the time amounted to 140 tons of new sand a day, a sizeable cost item.

Numerous experimental mixtures, employing all our previous knowledge, were made in an effort to regain the desired thermal shock test characteristics initially experienced. While these seemingly fruitless tests were being made, it became apparent it would be necessary to look for some minute differences which were apparently greatly influencing thermal shock characteristics. Sand mixtures having good and poor thermal shock test characteristics at first appeared under microscopic examination to be alike. After considerable study and re-examination, it was observed that there was a difference in the coating characteristics of sand grains, producing good and poor thermal shock test results.

All-purpose sand mixtures which produced good thermal characteristics were found to contain individual sand grains more uniformly coated, and a predominating number of the grains uniformly and



Fig. 2—Sand grains drawn to show coating characteristics affecting thermal stability of steel molding sands.

thinly coated. Likewise, sand mixtures which produced poor thermal shock results contained individual sand grains less uniformly coated and a wide variation in degree of coating of sand grains comprising the over-all sand sample. The author was unable to photograph successfully the contrasting differences in sand grain coatings as observed when viewing composite samples of sand mixtures producing good and poor thermal shock characteristics. Therefore he prepared simple drawings (Fig. 2) exemplifying sand grain coatings.

Sand Coating Characteristics

"A" represents a heavily coated sand grain. If this type of coated grain predominates in the composite sand mixture, poor thermal shock characteristics and inferior casting results can be expected. "B" represents non-uniformly coated sand grains. Regardless of whether the coating is thick or thin, this type of non-uniformity produces poor results. "C" shows uniform medium and thinly coated sand grains. When this type of sand grain coating predominates, ideal results can be expected. "D" represents sand grains with a uniform thin or smear coating, a coating characteristic applied through mulling new sand or reclaimed sand from which the coating was completely removed. This type of coating is conducive to satisfactory results. "E," non-coated sand grains, definitely contribute to poor thermal shock characteristics and casting results, even when comprising only a slight minority of the composite sand mixture.

While the factors relating to sand grain coating, thermal shock characteristics, and casting results were being further proved, it was observed that certain mulling conditions contributed to poor sand grain coating. An extensive study of mulling was made to determine the best mulling practice. The result of this study proved that there was a definite relationship between the size of batch, space between muller wheel face and surface against which mulling is done, and green compressive strength of a given molding sand mixture. Sand mixtures mulled under such a relationship contained a greater number of uniformly coated sand grains. The thermal shock characteristics and casting results were greatly improved. Taking advantage of the information obtained while making the mulling study, the author reduced the new sand ratio in the all-purpose molding sand mixture to 15 per cent, and later to 10 per cent.

Further verification of the effect that sand grain coating characteristics have on the thermal shock test characteristics was obtained while investigating the possibilities of certain polyelectrolytes. An addition of 0.65 lb to a 1300-lb size batch, or the equivalent of 0.05 per cent of the polyelectrolyte, resulted in all-purpose molding sand mixtures containing sand grains predominantly more uniformly coated and having greatly improved thermal shock test characteristics. Our present sand practice and the current cost of the polyelectrolyte did not permit taking advantage of these findings.

Recent investigation of two dry sand reclaiming methods, one employing the principle of impact, sand grains against a metal plate, the other a pneumatic scrubbing action of sand grain against sand grain, a method recently made available, resulted in further confirmation of the coating characteristic theory, Table 1.

TABLE 1—COMPOSITE DATA OBTAINED EVALUATING TWO DRY METHODS OF SAND RECLAMATION

Reconditioned sand, lb	20.0	—	—	1105.0	1105.0
Ottawa Crude Sand, lb	—	—	—	195.0	—
Scrubbed Reclaimed Sand, lb	—	20.0	—	—	195.0
Impact Reclaimed Sand, lb	—	—	20.0	—	—
Western Bentonite, lb	—	0.32	0.35	20.0	20.0
Cereal Binder, lb	—	—	—	6.0	6.0
Moisture, %	3.8	3.8	3.8	3.8	3.8
Grams of sand required for 2-in. test specimen	175	176	175	170	171
Green permeability	173	193	198	193	185
Green compression, psi	4.5	4.4	4.6	6.7	6.8
Dry tensile, psi	—	—	—	42.5	40.(
Max. expansion 1500 F, in./in.	—	—	—	.0157	.0165
Hot Strength @ 2500 F, 2 min soaking time, psi	—	—	—	15	17
Thermal shock characteristics 2 min soaking time	Light to Medium Cracks	Light Cracks	Medium Cracks Crazed	Light Cracks to Crazed	Slight to Light Cracks

The impact method of reconditioning sand resulted in an uneven removal of coating from the sand grains. All-purpose molding sand mixtures containing the unevenly coated sand grains produced thermal shock characteristics greatly inferior to identical sand mixtures containing reconditioned sand not treated by any reclamation method.

The effect of pneumatic scrubbing of sand grain against sand grain was found to produce a sand around which the thickness of coating was uniformly reduced. This sand, when used in all-purpose molding sand mixtures, produced thermal shock characteristics superior to the all-purpose molding sand mixtures containing an equal per cent of new crude sand. Investigating the relative value of used sand scrubbed at the rates of 1 through 6 tons and 9 tons an hour, resulted in finding sand scrubbed at the rate of 6 tons an hour could be satisfactorily substituted for new crude Ottawa sand in an all-purpose molding sand

mixture. The substitution of sand scrubbed at the rate of 6 tons an hour did not necessitate the changing of any other components of the sand mixture. An improvement in elevated temperature characteristics and increased density were experienced. Used sand scrubbed at lesser rates, when incorporated in the all-purpose molding sand mixture, required greater amounts of binding material. Elevated temperature characteristics were improved; however, the increasing improvement was not as great as initially experienced changing from new crude sand to scrubbed sand. Sand scrubbed at the rate of 9 tons an hour and substituted for new crude sand required less binding material. Elevated temperature characteristics were comparable to those of the molding sand mixture containing new crude sand.

Since it is impossible to have all sand grains coated alike, the question may arise as to which contributes the greater influence, coating thickness or uniformity of coating around the sand grain. The thickness of coating, within reasonable limits and as long as it is uniformly distributed, has been found less deterring to the formulation of molding sand mixtures capable of producing ideal thermal shock characteristics and casting results, than has been experienced with non-uniformly coated sand grains. Non-uniform, thinly coated sand grains have produced sand mixtures possessing inferior thermal shock test results. Uniform medium and thinly coated sand grains are most desirable for incorporating in molding sand mixtures.

Improved workability or moldability was observed in all molding sand mixtures when uniformity of medium and thinly coated sand grains predominated.

Reasonable control in the reduction of coating thickness and clay content can be accomplished through the dry reclamation method employing pneumatic scrubbing.

Experience has proven this method of reclamation is economical and maintenance of equipment a negligible cost when reclaiming sand at the rate of 6 tons an hour.

The sand grain coating characteristic theory is applicable to all foundries. Some modification of its application may be required, dependent upon the sand practices the foundry employs.

DISCUSSION

Chairman: J. A. RASSENFOSS, American Steel Foundries, East Chicago, Ind.

Co-Chairman: R. H. JACOBY, The Key Co., East St. Louis, Ill.

Recorder: J. B. CAINE, Foundry Consultant, Cincinnati.

D. S. BOSMA:[1] What is the polyelectrolite which you referred to?

MR. MEYER: The polyelectrolite referred to in this report is an extensively advertised soil conditioner.

M. H. HORTON:[2] What is the per cent clay in the feed sand and how much is removed?

MR. MEYER: At a scrubbing rate of 6 tons per hour the 5 per cent A.F.S. clay in the feed sand is reduced to 2½ to 3 per cent. A further reduction is possible but not required for our practice.

D. C. WILLIAMS:[3] How much is true clay and how much is A.F.S. clay?

MR. MEYER: We do not know. We test for A.F.S. clay only.

MEMBER: Are you using your reclaimed sand for cores?

MR. MEYER: We are experimenting along that line now. More information is necessary before anything definite can be said; however, results to date are encouraging.

MEMBER: Why do you not remove more of the old clay?

MR. MEYER: More bonding material would be necessary, and it would be more difficult to coat uniformly all the sand grains. A uniformly reduced coating, not a cleaner sand grain, is most advantageous in our sand practice.

[1] Superintendent of Foundries, Bucyrus-Erie Co., So. Milwaukee, Wis.
[2] Foundry Metallurgist, Deere & Co., Moline, Ill.
[3] Associate Prof., Ohio State University, Columbus, Ohio.

DRY RECLAMATION OF MOLDING SAND FOR STEEL CASTINGS

By

James A. Cannon*

ABSTRACT

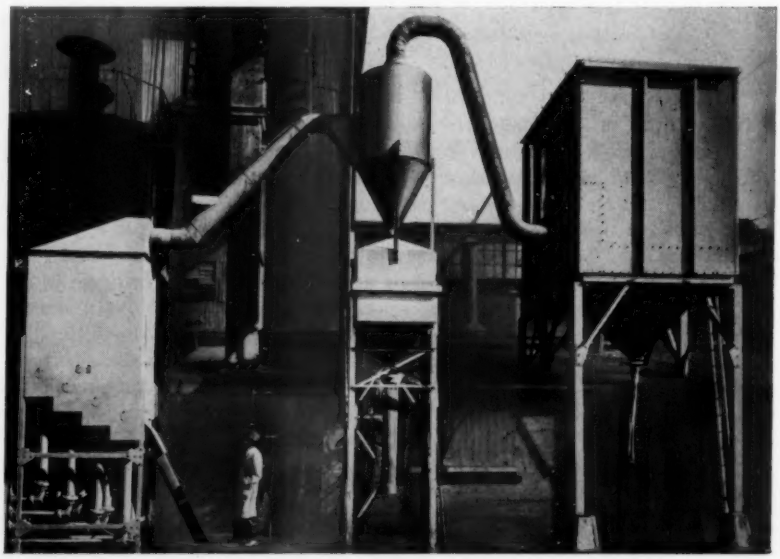
Reclaimed sand produced during an eight-month period with a dry method reclamation unit resulted in decreasing the amount of new sand used by more than 75 per cent. This reclaimed sand was used for facing mixes in molds for steel castings up to 3000 lb. Evaluation of the reclaimed sand is based on a comparison with new sand as used for facing during twelve months preceding the start of sand reclamation. A study of casting defects and casting quality during periods each sand was in use indicates casting quality to be as satisfactory when using reclaimed sand facing as when using new sand facing.

■ A base sand of washed and dried, round-grain Ottawa sand was reclaimed by the dry method in a unit described by C. E. Wenninger at the 1952 A.F.S. Convention ["Pneumatic Reclamation for Foundry Sands," *A.F.S. Transactions*, vol. 60, pp. 330-336 (1952)]. Essentially, this unit is a dry scrubber which removes old clay and fines by agitation and abrasion in an air stream.

Prior to installation of the reclaimer unit, this foundry used new sand in cores and in mold facing sands. With the present reclamation system, new sand is used in cores and reclaimed sand is used in facing for molds. The Ottawa base sand averages 53 A.F.S. grain size with an A.F.S. clay content of about 0.5 per cent. Dry reclaimed sand averages 50 A.F.S. grain size and has an A.F.S. clay content averaging 2.7 per cent. In this report, new sand and reclaimed sand are compared in their abilities to serve as facing sands for steel castings.

This foundry pours acid electric carbon and low alloy steel. Castings range in size from 1 lb to 3000 lb, averaging about 50 lb. Minimum temperature of metal entering the mold is 2850 F (1566 C) with most jobs ranging from 2900 F (1593 C) to 3000 F (1649 C). Small castings are poured from 150-lb shank pots, medium castings are poured from 1000-lb bull ladles, and large

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A typical installation (right), showing sand scrubber, fines collector and dust collector. Vibrating conveyor and screen, surge bin, and vibrating feeder complete the sand reclamation system.

castings from 2500-lb and 6000-lb bottom pour ladles.

Only a few experimental cores have been made of reclaimed sand. Because new sand must be introduced into the system, it is introduced at the core room as it happens that the amount of sand consumed by the core room has equalled sand losses from the system and maintained a balance.

The Old and the New

Figure 1 shows a flow diagram of the sand system when new sand was used in facing for molds. New sand entered the system at the core room and at the facing sand mill. Two-thirds of the cores were of new sand and one-third of old sand. New sand was used for facing sand except for small castings where facing was a half and half blend of old and new sand. Old sand amounted to about 20 per cent of the total facing sand used; balance was new sand.

Most core sand went to the cleaning room with castings after shakeout and from there went to the dump. Most molding sand, both facing and back-up, entered the shakeout storage tank. Sufficient sand was removed from storage daily to provide room in the sand system for entry of new sand from the facing mill.

Figure 2 shows the flow diagram for the present dry reclamation sand system. All new sand enters the system at the core room. The core room now uses new sand for all cores. Thus, new sand for the core room as in Fig. 2 is 240 tons instead of 160 tons as in Fig. 1.

New Sand from Cores

After shakeout of castings, new sand in the form of cores enters the cleaning room with castings. This new sand is recovered from the cleaning room and delivered into the shakeout storage tank. There is a small loss of sand from the cleaning room due to contamination by pulverized shot. All facing is now reclaimed sand and there is the advantage of uniformity compared with use of new and blended sands as in the old system. As molds are shaken out, most all facing and back-up sands are delivered into the shakeout storage. Sand in the shakeout storage tank contains 4.0 per cent new sand from cores, 18.0 per cent reclaimed sand from facing sands, and 78.0 per cent old sand from back-up sands.

Sand from the shakeout storage tank is conveyed in sufficient quantity to the reclaimer to maintain sand for mold facing. The balance of sand in the shakeout storage enters the back-up sand mill.

Sand entering the reclaimer has an A.F.S. clay content varying from 5.5 per cent to 8.5 per cent. Fines and clay removal by reclamation represent a reclaimer loss of 12.0 per cent. The reclaimed sand has a clay content of 2.0 per cent to 3.1 per cent averaging 2.7 per cent A.F.S. clay with reclamation rate of 4 tons per hour.

In addition to a 12 per cent loss at the reclaimer, another 6 per cent is lost due to use of sand linings in ladles plus losses of contaminated sands in cleaning room and foundry. The total 18 per cent loss is balanced by new sand introduced at the core room in cores.

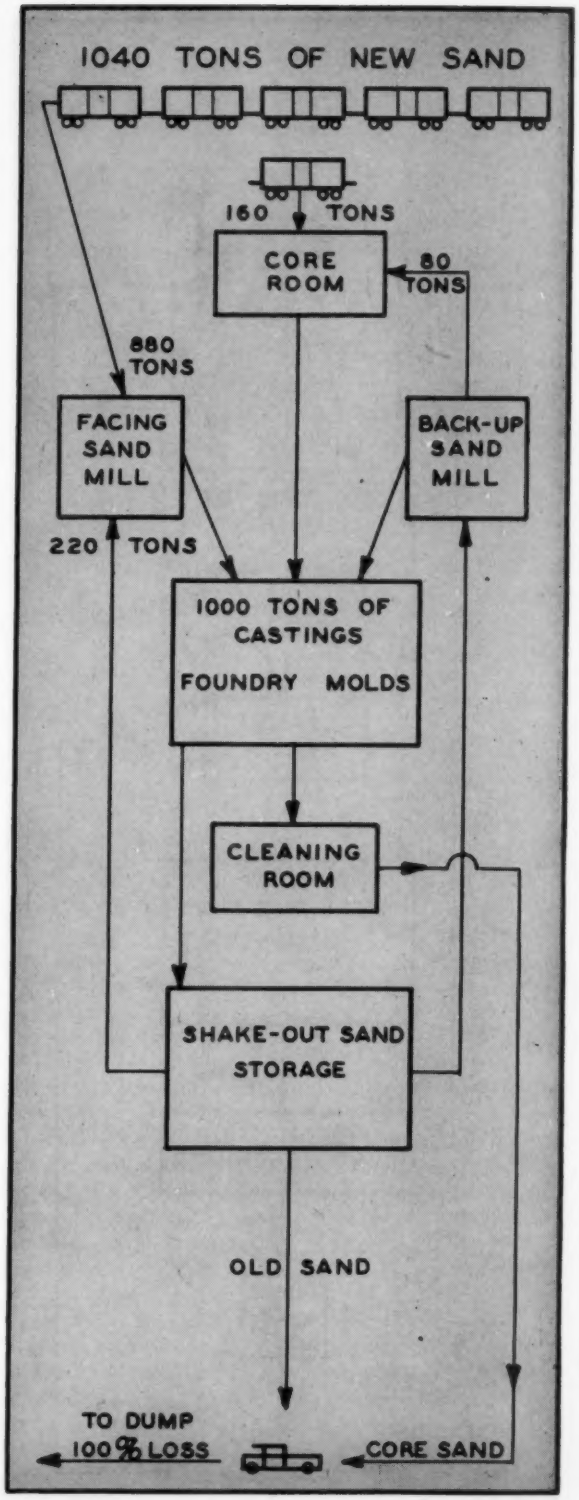


Fig. 1—Original Sand System, showing flow diagram with new sand used for facing molds. Test was made with two-thirds of cores using new sand, balance using the old. New sand entered system at either core room or facing sand mill. Progression through foundry operations is illustrated in the block diagram shown here.

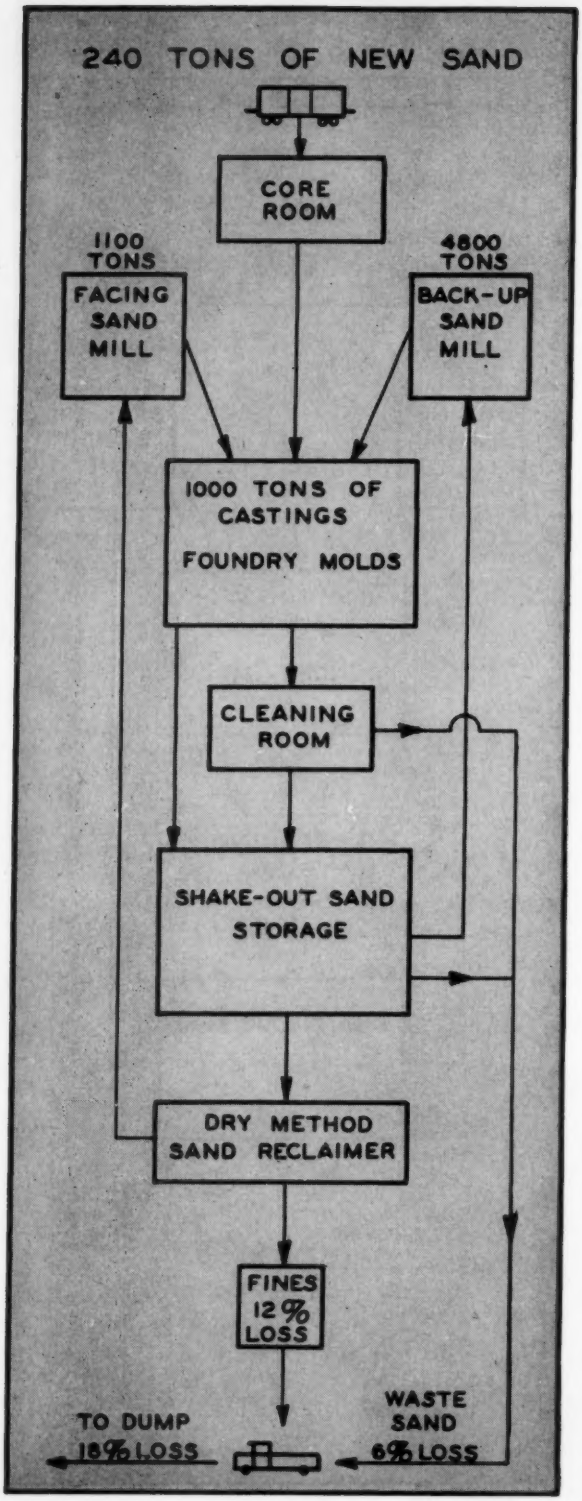


Fig. 2—Dry Reclamation Sand System

Changes in Screen Analysis

During reclamation of sand, daily samples were taken to obtain A.F.S. clay percentage and screen analysis for the reclaimed sand. The same data were obtained from sampling of each car of new sand delivered at the core room. These daily values were plotted and averaged monthly. Figure 3 summarizes and compares screen analysis of new and reclaimed sands for the eight-month period.

The material retained on the 20 and 30 mesh screens for reclaimed sand is not sand, but particles of tile, scale, and sintered sand grains. This foreign material was cause for concern, but has created no trouble as yet. The percentage of sand retained on the 40 and 50 mesh screens is consistently higher with reclaimed sand than with new sand. The percentage of new sand on 70, 100, and 140 mesh screens is consistently higher than reclaimed sand. Both sands have nearly equal amounts on 200 and 270 mesh screens and on pan. The A.F.S. fineness number averages 53 for new sand and 50 for reclaimed sand. The A.F.S. clay content for new sand is 0.5 per cent and A.F.S. clay content of reclaimed sand as shown in Fig. 4 varies by monthly averages from 2.3 per cent to 3.1 per cent. At a reclamation rate of 4 tons reclaimed sand per hour, the A.F.S. clay content averages 2.7 per cent.

Sand tests taken on each batch of a facing mix for heavy castings were basis for data shown in Fig. 6. This graph summarizes the room temperature properties by showing monthly averages during the eight-month period being discussed. Moisture and green compression were controlled to produce a desirable sand mix. The resulting permeability was closely watched. Though permeability gradually decreased for a period of months, it eventually returned to values equivalent to permeability experienced before reclamation. The drop in permeability cannot be explained, but no ill effect was experienced. Increased green compression during the last three months was an intentional change.

A typical facing mix made of new sand was made with 1 per cent cereal binder and 4 per cent western bentonite. When this mix was made with reclaimed sand the amount of cereal binder was not changed, but due to higher clay content of reclaimed sand, it was necessary to decrease the bentonite addition to 2.3 per cent to obtain equivalent values in green strength and moldability. This decrease in bentonite addition was the only major change in milling practice when converting from new sand to reclaimed sand.

Good Casting Quality Retained

Casting quality as judged by appearance seems the same with new sand or reclaimed sand mold facing. Sand peels easily leaving a clean smooth casting surface with either sand. However, quality can best be evaluated by plotting amounts of scrap castings and amounts of welding rod used for repair. Figure 5 shows monthly average values that make possible comparison between castings made during twelve months of 1951 versus castings made during eight months of 1952.

Average amount of welding rod used to repair defects during reclamation is 68.1 per cent of welding rod used before reclamation. Average amount of total scrap during reclamation period is 85.4 per cent of total scrap made before reclamation. When considering only that portion of scrap attributed to sand de-

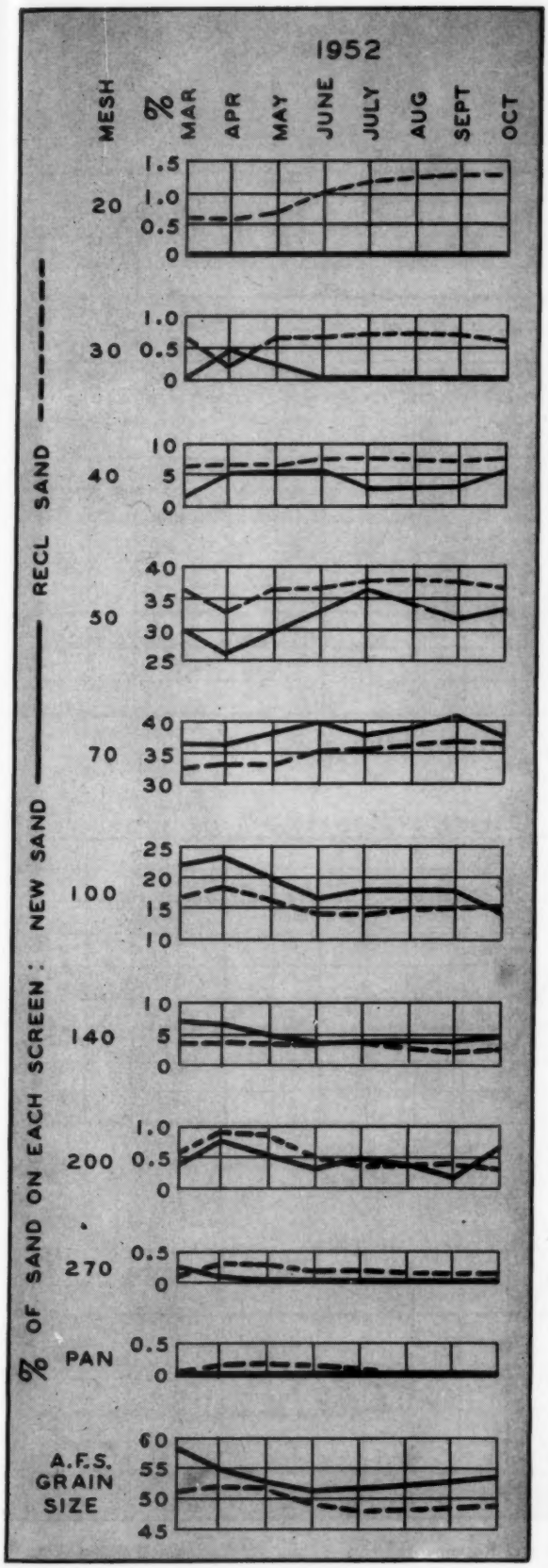


Fig. 3 (left)—Screen Analysis

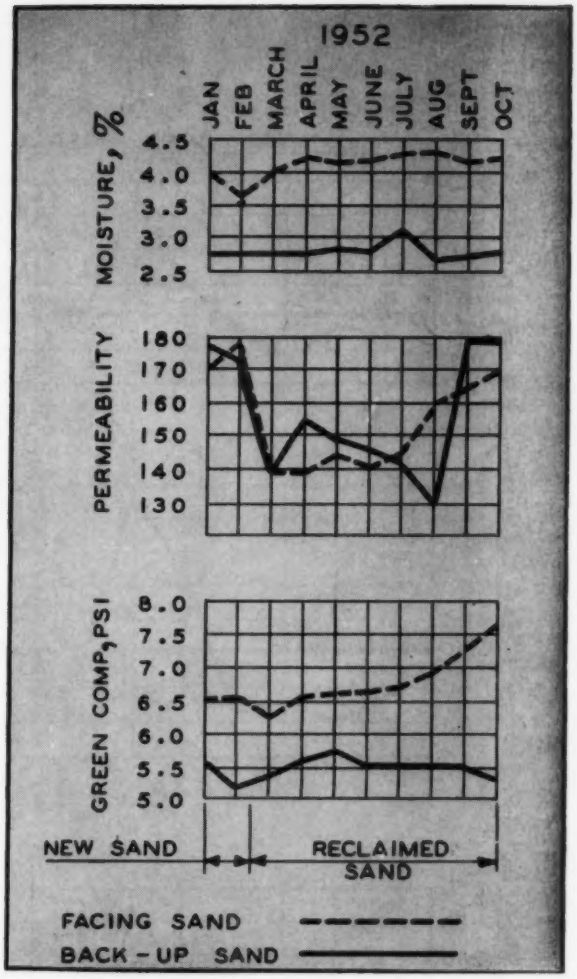


Fig. 4—Room Temperature Properties

fects such as sand, blows and scabs, the amount of scrap during the reclamation is 81.7 per cent of the same class of scrap made before the reclamation period. Finally, scrap due only to sand erosion is compared for the two periods and scrap due to sand is practically the same when using either new sand or reclaimed sand for mold facing.

It is not to be assumed that the decrease in total scrap and decrease in welding rod used during the reclamation period is due to the sand only. This improvement must be the result of improved foundry techniques as well as the sand. It can be safely assumed, however, that quality of castings has not decreased due to use of reclaimed sand for mold facing.

The use of reclaimed sand as a facing sand in molds for steel castings has presented no objectionable problem in the sand system, the reclaiming operation, in milling or in molding. The use of reclaimed sand has decreased new sand consumption by more than 75 per cent. The reclaiming operation not only provides a more uniform sand for facing, but renews the entire

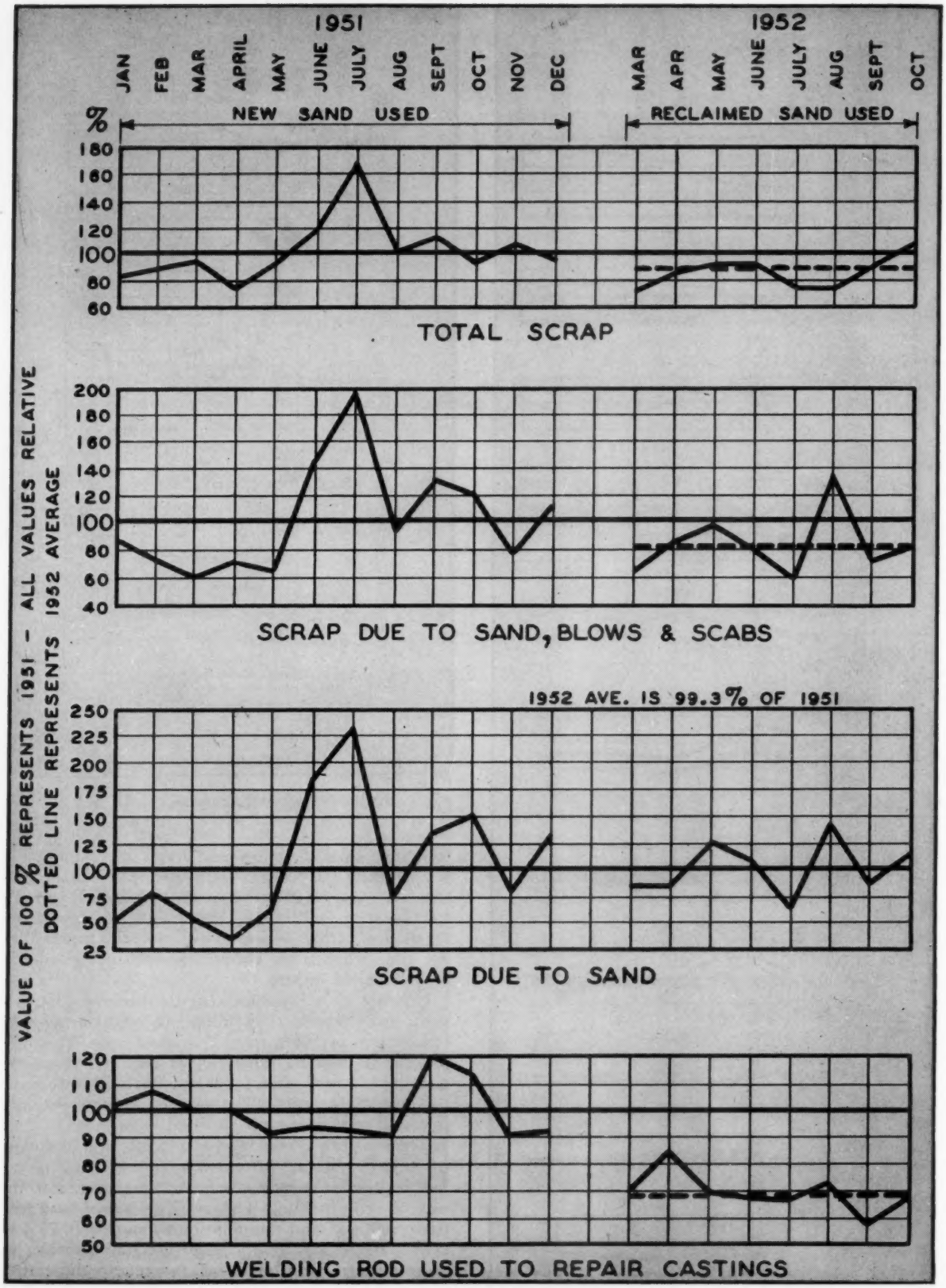


Fig. 5—Scrap and Repair

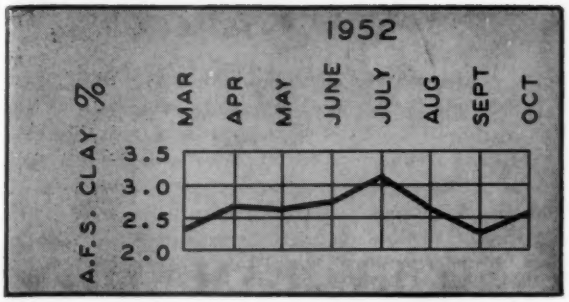


Fig. 6.—Clay Content of Reclaimed Sand

sand system every eight to ten days and provides uniform sand for back-up sand as well.

Although the above-mentioned advantages for dry reclaimed sand are desirable, the final test is in the end result. How good are the castings? A poorer sand will result in more scrap and more repair of castings. During the reclamation period being discussed there has been less scrap and less casting repair than formerly. Thus it can be concluded that dry-reclaimed sand is as satisfactory as new sand for use as a facing sand in molds for steel castings up to 3000 lb in weight.

Acknowledgments

The writer wishes to express appreciation to those persons offering the opportunity to present this paper and for permission to use the information obtained and also for the help in securing data and advice in preparing the manuscript. Thanks are extended especially to S. W. Duncan of Duncan Foundry & Machine Works, Inc., W. C. Carroll, superintendent, R. Corwin, metallurgist, and J. B. Caine, consultant.

DISCUSSION

Chairman: J. A. RASSENFOSS, American Steel Foundries, East Chicago, Ind.

Co-Chairman: R. H. JACOBY, The Key Co., East St. Louis, Ill.

Recorder: J. B. CAINE, Foundry Consultant, Cincinnati.

T. A. TARQUINIO:[1] What happens to oils and dry binders in core sands during dry reclamation?

MR. CANNON: We use no core oil. There has been no observable build up of phenol resin, or cereal binders. We have made satisfactory cores from reclaimed sand, but conditions in our plant do not justify the use of reclaimed sand for cores. We must introduce new sand into the system someplace and the best place in our plant is through the core room.

MEMBER: Is your figure of 18 per cent of sand turnover used for cores typical of the steel foundry?

MR. CANNON: Yes, but individual foundries may vary over a range of at least 100 per cent.

MEMBER: Can reclaimed sand with 2.7 per cent A.F.S. clay be used for cores?

MR. CANNON: Yes, but we use no core oil.

CHAIRMAN RASSENFOSS: What are your core mixtures? What is the cost of your reclaimed sand?

MR. CANNON: Two core mixtures are used for the majority of our cores. One is a bentonite-cereal sand, the other a standard phenol resin sand. Our reclaiming cost, including overhead is $1.43 per ton, using one unit at the rate given in the paper.

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[1] Division Foundry Foreman, American Manganese Steel Division of American Brake Shoe Co., Chicago Heights, Ill.

A WET METHOD OF SAND RECLAMATION

By

P. C. Will* and R. H. Shurmer**

ABSTRACT

This paper shows by means of direct comparisons that waste sand reclaimed by the wet method has proven to be equivalent to new sand in physical properties, grain distribution, casting finish and quality; that it has all the attributes of new sand for foundry purposes; that it can be produced for a fraction of the cost of delivered new sand without sacrifice in casting quality. A wet method reclaiming pilot unit is described and illustrated. Photomicrographs show that the gray color of reclaimed sand is due merely to a deposit within the surface imperfections of the grain and that the grain surface is not actually adversely affected.

This paper outlines a wet method of sand reclamation and shows the results of this method in terms of physical properties and grain distribution of reclaimed sand as compared with new sand. The authors' wet method pilot reclaiming unit is shown in schematic outline in Fig. 1.

This pilot unit has a capacity of 2 tons per hour for most sands but can be adjusted to operate at from 1½ to 3 tons per hour. In pioneering the wet method of sand reclamation for the past 15 years the author's company tested hundreds of waste foundry sands in its pilot reclamation system. These sands have come from iron, steel and non-ferrous foundries from every section of the United States and Canada and from some foreign countries.

In order to follow this flow diagram (Fig. 1), the authors will describe the component parts in sequence of flow. The lumpbreaker is a rotary barrel having a center section of perforated plate. The waste foundry sand is introduced into the lumpbreaker and wet down. Sand and water pass through the perforations while lumps are tumbled in the barrel and are abraded to individual sand grains. After passing through the perforations the slurry is screened over a vibrating unit equipped with a 6-mesh screen deck. With this screen the oversize material is removed and deposited into a skip box; tramp metal is retained in the lumpbreaking barrel and is passed out the discharge end into the same oversize skip box.

After screening, the sand and water slurry is pumped to the primary classifier (Fig. 2). This classifier is of the hydraulic-hindered settling type. The purpose of this classifier is to remove undesirable free fine material in order to increase the efficiency of the scrubbing action described later.

* Vice President, Engineering and ** Sand Technician and Foundry Engineer, The Hydro-Blast Corp., Chicago.

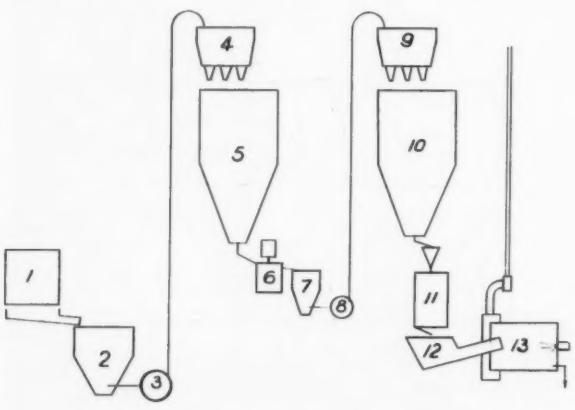


Fig. 1—Schematic outline of the authors' wet method pilot reclaiming unit. Legend: 1. Lumpbreaker; 2. Sand Sump Tank; 3. Sand Pump; 4. Primary Classifier; 5. Scrubber Feed Tank; 6. Scrubber; 7. Sand Sump Tank; 8. Sand Pump; 9. Secondary Classifier; 10. Feed Tank; 11. Dewatering Cylinder; 12. Screw Feeder; 13. Dryer.

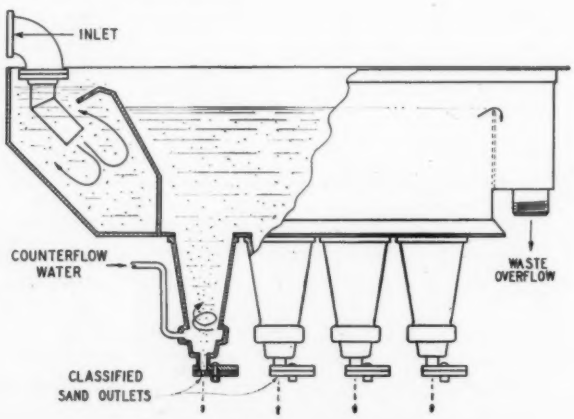


Fig. 2—Hydraulic Counterflow Classifier.

The amount of fines removed depends on:

1. The retention time and velocity in the classifier tank.
2. The size of the bottom orifice opening.
3. The amount and velocity of counterflow water.

The desired classification is obtained by adjusting these three factors to expel the undesirable fines over the overflow weir into the waste, while permitting the proper-sized grains to settle to the bottom of the classifier cones and to pass through the discharge orifices. It is possible to recover some finely divided silica flour along with the sand, but usually a large part of the silica flour is carried away with the waste in the initial classification in order to insure complete clay removal. In the authors' wet method reclaiming unit, no provision is made to salvage the extremely fine silica such as silica flour or splintered sand grains. This equipment is, however, extremely flexible and capable of reclaiming most finenesses of sand now being used.

It is possible with this type of classification, because of its flexibility and adaptability, to produce a sand to meet the demands of the foundry and the type of work it is doing at any given time. A good example of this is that, in the automotive gray iron field where lake sand-bank sand blends are used, in some cases a reclaimed product is offered that will match the grain distribution of the lake sand only, the bank sand being rejected in the overflow of the classifier. In other cases, a reclaimed product is produced with a grain distribution which will match the lake sand-bank sand blend.

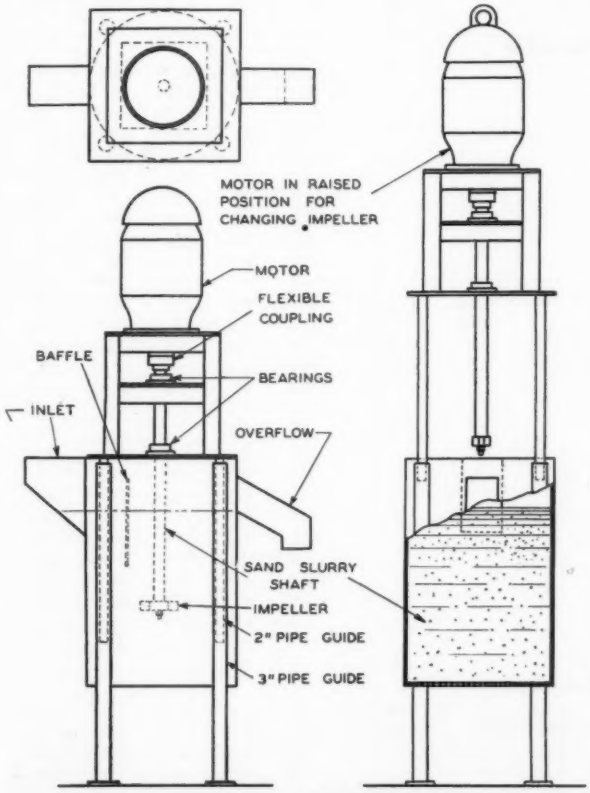


Fig. 3—Drawing of Sand Scrubber.

Sand passing through the primary classifier orifices flows to the scrubber feed tank which serves not only as storage but also as a means of dewatering from a dilute slurry to a 70 per cent solids slurry, the desired consistency for most efficient scrubbing. This heavy slurry is drawn at a uniform and continuous rate through a metering orifice into the sand scrubber, Fig. 3. The slurry retention time in the scrubber is varied depending upon the type of sand to be reclaimed and on the type of material adhering to the grain surface. Generally steel foundry sands are scrubbed for approximately 5 min while iron foundry sands require about 10 min. From this retention time it is obvious that gray iron waste sands are more difficult to cleanse than are the steel sands. This is due to the amount and nature of additive materials used in gray iron sand practice and also because the base sand used in the gray iron industry is generally of greater angularity.

Scrubbing Action

The scrubber impeller operates at 1750 rpm. The scrubbing action is obtained through impingement of the high velocity stream of slurry as it leaves the impeller against a slower moving stream along the outer circumference of the scrubber. Circulation is maintained in a vertical motion as well as in the direction of travel of the impeller thereby reducing the possibility of short circuiting through the scrubber. The impingement of sand grains against each other produces an intergranular friction which is sufficiently great to properly scrub the individual sand grains.

The impeller is a simple paddle type which can be cast by the foundry using the reclaimer. It requires no machining and is easily replaced by raising the motor support and shaft as illustrated in Fig. 3. The impeller has a life of from 8 to 16 hr depending upon the type of sand being scrubbed and the kind of metal used in the impeller. The impeller is the only high-wear item of the scrubber.

After scrubbing, the sand flows to the second sand transfer tank from where it is pumped to the secondary classifier. The secondary classifier removes the material which has been loosened in the scrubbing operation. The equipment and operation are similar to the primary classifier. Sizing classification for proper grain distribution takes place at this point. The sand next flows downward into the secondary sand storage bin and is again metered from the bottom of this bin to the pressure-type dewatering filter. This is an automatic batching operation which is controlled electrically as follows:

1. It is filled with a sand-water slurry at approximately 30 per cent moisture.
2. The compressed air is applied to force the bulk of the water from the slurry through a screen door at the bottom of the filter.
3. The sand is discharged from the unit at 8-10 per cent moisture.

On large capacity units a horizontal vacuum-type filter table is used for dewatering with comparable results, thus achieving maximum dewatering by mechanical means before drying in order to reduce fuel costs. The damp sand is then fed into a metering

Table 1—Properties of Test Sand Mixtures

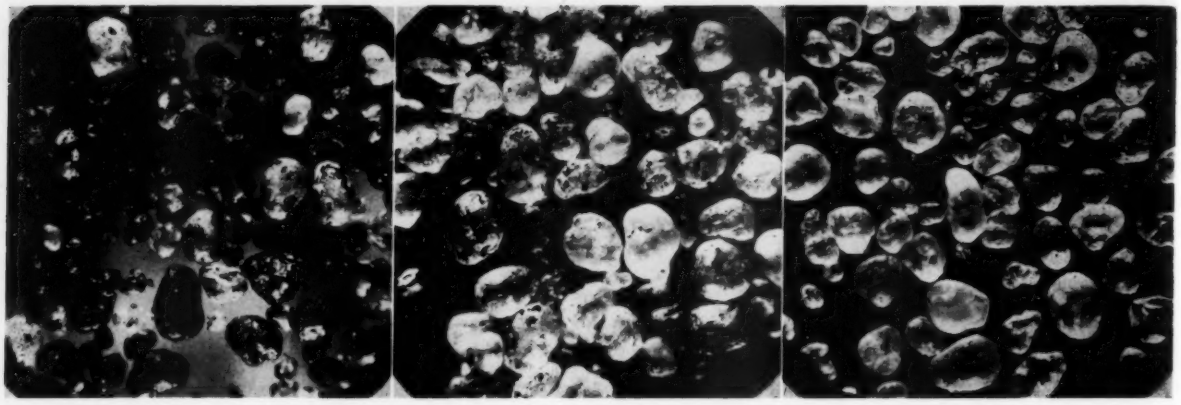
Foundry No.		Wet Method Reclaim %	Ottawa W&D %	Ottawa Crude %	Blended Ottawa %	Wisconsin Portage %	Other %	Silica Flour %	Western Bentonite %	Cereal %	Core Oil %	Iron Oxide %	Other Binders %	Moisture %	Green Compression, psi	Green Permeability	Baked Permeability	Baked Tensile, psi
Steel Foundries																		
1	Reclaim	87.57						10.65	0.62	0.27	0.89			3.6	3.2	108	243	50
	New				87.57			10.65	0.62	0.27	0.89			3.8	3.3	102	226	55
2	Reclaim	86.52						8.65		1.35	1.74	0.87	0.87 (h)	4.9	2.5	85	—	185
	New				86.52			8.65		1.35	1.74	0.87	0.87 (h)	4.8	2.5	77	—	190
3	Reclaim	90.16						7.72		0.45	1.67			4.0	1.2	102	226	210
	New			90.16				7.72		0.45	1.67			3.9	1.8	63	173	175
4	Reclaim	95.23							1.43		1.91		1.43 (i)	4.5	2.2	146	313	157
	New						95.23 (a)		1.43		1.91		1.43 (i)	4.4	2.1	112	275	161
5	Reclaim	95.24						2.25	0.34	0.9	1.27			3.2	1.5	119	212	155
	New			47.62		47.62		2.25	0.34	0.9	1.27			3.3	1.7	108	167	140
6	Reclaim	96.69								1.93	1.38			3.0	1.2	146	341	330
	New					96.69				1.93	1.38			2.9	1.6	119	275	305
7	Reclaim	82.72						12.0	1.44	1.44			2.4 (j)	5.6	1.7	115	173	110
	New						82.72 (a)	12.0	1.44	1.44			2.4 (j)	5.8	1.7	82	138	110
8	Reclaim	96.9								1.55	1.55			5.8	0.6	173	440	285
	New				96.9			1.75		1.55	1.55			5.9	0.9	173	440	250
9	Reclaim	95.85						1.75		1.0	1.4			3.0	0.9	134	226	270
	New				95.85					1.0	1.4			2.9	0.8	88	205	315
10	Reclaim	86.0						9.6		0.4			4.0 (k)	4.7	1.8	105	138	110
	New					86.0		9.6		0.4			4.0 (k)	4.7	1.9	93	134	120
11	Reclaim	79.15						17.6	1.5	1.0	0.75			6.6	8.2	48	99	125
	New				79.15			17.6	1.5	1.0	0.75			6.5	8.6	50	99	115
12	Reclaim	98.0								1.0	1.0			3.0	0.8	163	226	175
	New			49.0			49.0 (b)			1.0	1.0			3.1	0.8	193	275	165
13	Reclaim	85.86						11.5	2.3	0.34				5.5	5.6	126	313	75
	New			85.86				11.5	2.3	0.34				5.5	6.6	105	278	80
14	Reclaim	98.48								1.18	0.32			1.6	0.8	119	275	155
	New		32.83			65.65				1.18	0.32			1.7	0.8	112	264	145
15	Reclaim	97.7								1.5	0.8			3.0	0.6	138	212	190
	New						97.7 (c)			1.5	0.8			2.9	0.5	128	198	185
16	Reclaim	94.5						2.6	0.53	1.3	1.06			4.1	3.6	122	300	125
	New		94.5					2.6	0.53	1.3	1.06			3.9	4.4	128	287	100

Foundry No.		Wet Method Reclaim %	Lake Sand %	Bank Sand %	Other Sand %	Core Oil %	Cereal %	Western Bentonite %	Iron Oxide %	Other Additives %	Moisture %	Green Compression, psi	Green Permeability	Baked Permeability	Baked Tensile, psi
Gray Iron Foundries															
17	Reclaim	98.65				0.8	0.55				2.9	0.5	105	219	197
	New				49.32 (d) 49.32 (e)	0.8	0.55				3.0	0.6	122	243	175
18	Reclaim	73.26		24.42		1.25	1.07				1.6	0.6	119	205	195
	New		73.26	24.42		1.25	1.07				1.7	0.7	138	219	205
19	Reclaim	98.25				1.0	0.75				2.0	0.6	235	287	260
	New		98.25			1.0	0.75				2.1	0.6	235	313	265
20	Reclaim	98.0				1.0	1.0				2.9	0.6	156	219	260
	New		98.0			1.0	1.0				3.0	0.5	219	275	270
21	Reclaim	97.5				1.11	1.39				1.8	0.7	156	235	270
	New		97.5			1.11	1.39				1.7	0.7	193	287	285
22	Reclaim	96.43				1.19	1.3		1.08		2.9	0.9	178	287	280
	New		96.43			1.19	1.3		1.08		3.0	0.8	185	300	290
23	Reclaim	95.7				1.8			0.8	1.7 (l)	0.0	0.0	105	—	235
	New				95.7 (f)	1.8			0.8	1.7 (l)	0.0	0.0	105	—	185
24	Reclaim	98.0				0.5	1.5				1.9	0.8	212	253	185
	New		98.0			0.5	1.5				2.0	0.8	253	341	175
25	Reclaim	97.86				0.66	1.48				2.1	0.7	167	253	190
	New		97.86			0.66	1.48				2.0	0.8	173	205	185
26	Reclaim	98.17				1.0	0.83				2.5	0.5	156	212	205
	New				98.17 (g)	1.0	0.83				2.5	0.6	142	205	167
27	Reclaim	97.9				0.7	1.4				1.8	0.7	163	341	195
	New		97.9			0.7	1.4				1.9	0.7	185	376	190
28	Reclaim	97.7				0.9	1.4				2.4	0.7	102	219	150
	New		73.3	24.4		0.9	1.4				2.3	0.8	119	234	175
29	Reclaim	98.94				0.66	0.4				2.0	0.3	178	287	150
	New		98.94			0.66	0.4				2.1	0.35	185	300	125
30	Reclaim														
	New														
Malleable Foundries															
31	Reclaim	97.44				1.25	1.03	0.196		0.084 (m)	1.9	1.6	138	226	150
	New		97.44			1.25	1.03	0.196		0.084 (m)	1.9	2.1	93	134	160
32	Reclaim	97.09				1.57	1.25	0.09			1.6	0.6	138	300	260
	New		97.09			1.57	1.25	0.09			1.5	0.7	151	326	258

(a) Ohio Portage Sand
(b) Kingston No. 70 Sand
(c) New Jersey Silica Sand
(d) Heaton Sand
(e) Bay City Sand
(f) Berlin Sand
(g) Wisconsin Portage Sand
(h) Fireclay
(i) Dextrine
(j) Truline Binder
(k) Kor-Rite Dry Binder
(l) Lion Binder
(m) J. S. McCormick Dryer

TABLE 2—SCREEN ANALYSES OF TEST SANDS

Foundry No.		Sieve No. 6	12	20	30	40	50	70	100	140	200	270	Pan
Steel Foundries													
1	Reclaim	—	—	0.6	2.4	13.0	46.0	27.4	3.4	7.0	0.2	—	—
	Blended Ottawa	—	—	0.1	1.5	7.3	34.7	34.4	16.1	5.0	1.3	0.2	0.2
2	Reclaim	—	—	0.1	0.1	2.8	34.0	35.2	21.8	5.2	0.4	—	—
	Blended Ottawa	—	—	—	—	3.4	32.2	31.2	24.8	7.8	0.2	0.2	0.2
3	Reclaim	—	—	—	1.6	24.0	30.8	21.4	14.6	6.4	1.2	—	—
	Ottawa Crude	—	—	0.2	4.8	35.2	27.6	12.2	9.4	6.8	2.4	0.8	0.2
4	Reclaim	—	—	—	0.8	8.4	33.8	33.4	17.8	4.6	1.2	—	—
	Ohio Portage	—	—	—	0.8	6.2	26.0	29.4	21.2	9.8	5.0	1.0	0.6
5	Reclaim	—	—	—	1.4	26.4	36.2	18.6	11.4	4.6	1.0	—	—
	Ottawa Crude—Wisconsin Portage 1:1	—	—	—	1.4	15.7	30.4	25.4	17.7	6.5	1.5	0.7	0.5
6	Reclaim	—	—	—	1.2	22.4	33.2	23.8	14.0	5.0	0.4	—	—
	Wisconsin Portage	—	—	—	1.2	11.2	24.4	30.5	22.4	7.9	1.8	—	0.5
7	Reclaim	—	0.2	0.6	1.4	5.8	23.8	32.2	17.6	15.4	2.0	0.2	0.2
	Ohio Portage	—	—	1.2	3.6	10.0	25.2	28.2	15.0	12.4	2.6	0.6	0.6
8	Reclaim	—	—	0.2	1.2	8.4	31.8	38.2	11.0	8.4	0.6	0.2	—
	Blended Ottawa	—	—	1.2	3.8	14.0	35.6	31.0	8.6	5.0	0.4	0.2	—
9	Reclaim	—	—	0.2	2.4	6.4	45.0	21.0	17.8	6.0	1.0	—	—
	Blended Ottawa	—	—	—	0.8	19.7	16.1	18.1	23.5	15.2	4.8	1.1	0.3
10	Reclaim	—	—	0.4	3.4	15.0	28.2	27.8	7.6	17.0	0.4	—	—
	Wisconsin Portage	—	—	—	2.8	12.0	27.6	29.2	8.8	17.4	1.8	0.2	0.2
11	Reclaim	—	—	0.6	4.2	36.6	35.0	14.0	6.6	2.4	0.4	—	—
	Ottawa Belrose	—	—	0.2	1.0	44.4	29.0	13.4	10.6	2.5	0.1	0.1	0.1
12	Reclaim	—	—	0.2	1.8	27.4	30.4	18.4	12.6	7.2	1.6	0.2	0.2
	Kingston-Ottawa Crude 1:1	—	—	0.2	3.6	27.9	29.9	17.1	9.9	6.6	2.1	0.6	0.3
13	Reclaim	—	0.2	0.8	4.0	12.4	53.2	16.0	5.6	7.0	0.4	—	—
	Ottawa Crude	—	—	0.4	5.2	38.4	34.8	13.8	0.6	6.6	0.1	0.1	—
14	Reclaim	—	—	—	0.2	4.6	23.0	29.6	28.0	12.0	2.2	0.2	—
	Wisconsin Portage-Ottawa W&D 2:1	—	—	—	0.2	4.4	24.2	33.8	24.4	10.4	2.0	—	—
15	Reclaim	—	—	0.2	1.8	6.9	18.8	35.9	27.9	6.8	0.8	0.2	0.1
	New Jersey Silica	—	—	0.3	1.7	8.2	21.6	33.0	24.5	8.2	1.6	0.3	—
16	Reclaim	—	—	—	1.2	27.8	38.2	18.2	9.6	3.8	1.0	—	—
	Ottawa W&D	—	—	—	2.8	32.6	35.6	12.4	8.6	4.6	1.6	0.6	0.4
Gray Iron Foundries													
17	Reclaim	—	—	0.2	2.8	16.4	25.4	26.6	23.0	5.0	0.2	—	—
	Bay City-Heaton 1:1	—	—	—	2.6	28.2	26.8	19.4	16.6	5.2	0.6	0.2	—
18	Reclaim	—	—	—	0.2	1.2	38.0	44.6	13.4	2.6	—	—	—
	Lake	—	—	—	0.2	5.0	50.0	37.6	5.8	1.0	0.2	—	—
19	Reclaim	—	—	—	0.2	8.2	47.2	32.4	9.4	2.4	0.2	—	—
	Lake	—	—	—	0.2	6.8	42.8	39.8	8.4	1.6	0.2	—	—
20	Reclaim	—	—	0.2	0.2	3.6	34.8	44.6	13.8	2.6	0.2	—	—
	Lake	—	—	—	—	2.2	31.8	53.2	12.6	0.2	—	—	—
21	Reclaim	—	—	—	0.2	4.4	33.4	42.2	15.0	4.0	0.6	—	—
	Lake	—	—	—	—	2.0	7.4	76.4	13.8	0.4	—	—	—
22	Reclaim	—	—	—	0.6	9.0	51.2	34.2	4.6	0.4	—	—	—
	Lake	—	—	—	0.6	8.4	52.2	33.4	4.6	0.4	0.2	—	—
23	Reclaim	—	—	0.2	0.8	13.8	24.4	27.6	14.8	15.4	2.0	0.2	0.2
	Berlin	—	—	—	0.3	3.2	9.3	54.7	23.3	8.2	0.5	0.2	—
24	Reclaim	—	—	0.2	1.6	11.8	40.8	32.6	10.6	2.2	0.2	—	—
	Lake	—	—	0.2	2.4	17.0	43.0	30.0	6.4	0.6	—	—	—
25	Reclaim	—	—	0.2	2.2	5.8	17.8	45.4	26.8	1.6	0.2	—	—
	Lake	—	—	—	0.6	3.4	18.4	44.6	31.2	1.4	0.2	—	—
26	Reclaim	—	—	0.4	5.6	21.0	28.8	22.2	15.0	5.8	1.2	—	—
	Wisconsin Portage	—	—	0.4	5.1	16.6	26.9	22.5	16.7	7.9	1.4	0.6	0.4
27	Reclaim	—	—	—	1.6	9.6	37.8	40.8	9.6	0.6	—	—	—
	Lake	—	—	—	0.6	4.4	31.4	53.4	8.2	1.0	1.0	—	—
28	Reclaim	—	—	—	0.2	2.8	26.6	42.6	19.2	6.4	2.2	—	—
	Lake-Bank 3:1	—	—	—	0.1	1.3	13.8	43.2	23.9	9.7	5.6	0.6	0.1
29	Reclaim	—	—	0.2	0.4	2.4	18.8	69.4	7.8	1.0	—	—	—
	Blended Lake	—	—	0.1	0.6	2.7	21.5	53.1	20.1	1.1	—	—	—
30	Reclaim	—	—	—	0.2	3.8	30.0	39.2	22.4	4.0	0.2	—	—
	Italian Sand	—	—	—	0.2	3.8	31.2	40.4	20.4	3.6	0.4	—	—
Malleable Iron Foundries													
31	Reclaim	—	—	0.2	0.4	3.4	19.0	36.6	28.4	10.6	1.4	—	—
	Lake	—	—	—	0.4	3.6	17.4	30.0	19.8	21.2	6.4	0.6	0.2
31	Reclaim	—	—	0.2	0.8	4.0	15.6	45.4	31.2	2.8	—	—	—
	Lake	—	—	0.2	0.4	3.0	17.6	50.4	27.4	1.0	1.2	—	—



Waste Sand—as Received | Reclaim Sand | New Sand

Fig. 4—Typical photomicrographs of waste sand "as received," reclaim sand, and new sand. Mag. 15x.

screw-type conveyor which feeds the dryer at a uniform rate of flow. This dryer is a typical rotary kiln-type unit which dries the sand to bone-dryness. Upon leaving the dryer, the sand is screened through whatever mesh screen is necessary to remove undesirable oversize material.

Some years ago the authors' company offered the facilities of the pilot plant and its technical staff to the foundry industry for the purpose of processing, testing and evaluating their waste sands for reclamation. This work, done without charge, was performed for academic rather than economic reasons.

Tables 1 and 2 show the results obtained in the pilot unit from the processing of 32 different samples of waste foundry sand from iron and steel foundries. These samples varied from 5 to 50 tons.

The sands produced by this wet reclamation system were compared on a laboratory basis against new sands of the respective foundries using mixtures selected by the foundry involved. Usually additive materials such as core oil, cereals, clays and other binders were shipped from the current stocks of the respective foundries for use in the evaluation. The reclaimed sand was then shipped to the foundry and applied in production mixes. Castings were poured and examined in comparison with those made with their standard new sand cores and facings. This was done in order to eliminate as many variables as possible. In every case the sands returned to the foundry compared favorably with the new sands.

Certain general observations can be made from the tables:

1. The reclaimed sand shows a slightly lower green strength than new sand.
2. The baked tensile strength is about equal.
3. The permeability, both green and baked, is slightly higher. Loss on ignition of reclaimed sand is slightly higher. This is attributed to the surface imperfections of the sand grains in the natural state which are filled with carbonaceous materials which it is not practical to remove mechanically by the scrubbing operation. It has been found in some cases that having these depressions filled is advantageous because the reduced total surface area requires less binder to coat the grains, without impairing the physical properties.

The photomicrographs in Fig. 4 illustrate that the gray color of the reclaimed sand is a result of the carbon deposits in the surface imperfections of some of the grains rather than a complete coating of the grains.

Results, benefits and characteristics that might reasonably be expected of a wet sand recovery system include:

1. Casting finish equal to new sand.
2. Baked tensile strengths equal to an equivalent new sand with no increase in binder consumption. This is considered a practical measure of sand cleanliness.
3. Sand cost per ton substantially lower than that of delivered new sand, usually low enough to permit amortization of equipment investment cost in 1 to 3 years depending on the volume of sand used and cost thereof. Often the operating costs of a wet method sand reclamation unit are no greater than the cost of waste sand disposal alone.
4. Grain distribution can be controlled more closely than that of new sand.
5. Color will not match that of new sand. The gray color will not produce any undesirable effects on the physical characteristics of the reclaimed sand.
6. Sintering point is not lowered.

This paper has not attempted to show the effect of a wet sand reclamation system on dust control. It certainly offers advantages in this direction. The dust producing grains are largely removed in the waste from the classifiers as slimes.

There are at present 26 wet method sand reclamation units in operation producing upwards of 1,000,000 tons of sand annually for the production of castings equal in quality to those made with the new sand formerly used by these respective foundries. Generally these foundries have had refuse sand samples processed in the authors' pilot reclaimer and have run exhaustive laboratory and production tests on the reclaimed product in order to predetermine the results obtainable before purchasing a reclamation system.

SAND RECLAMATION IN A STEEL FOUNDRY

By

H. H. Johnson, R. Y. McCleery and G. A. Fisher*

One of the main problems that confronts every foundry is that of processing an adequate supply of sand which has the characteristics to meet the many requirements imposed upon it in the foundry and the core room. The volume of sand required alone presents a major handling problem since one to two tons of new sand are normally required per ton of castings produced. This means that in a foundry such as the one with which the authors are connected, sand requirements may be in the order of 1200 tons per week when production is at a normal level.

In addition, the sand must consistently possess certain characteristics of uniformity such as grain shape, distribution of grain size, and thermal characteristics to mention only the very obvious requirements. These are necessary to secure the uniformity of properties expected in the sand mixes from mill to mill and from day to day. These requirements further complicate the problem because the use of sand from a particular geographical location is frequently indicated, such as the familiar use of Ottawa sand from near Ottawa, Illinois, because of its round grain characteristics. While the cost of the sand itself may be comparable with that of many other types of sand, the freight cost on a long haul can be considerable and therefore the initial sand cost becomes an item of major consideration. When sand is transported for a considerable distance, the foundry also must allow for delays by maintaining a considerable stock pile which further presents problems of providing storage space, unloading of cars and loading into bins or onto belts as needed, in all kinds of weather.

Disposition of Waste Sand

An additional factor—and not the least in importance—is that of the disposition of the waste sand, since an amount of sand must be removed from the system equal to the amount that has been introduced as new sand. In some situations there is a sale for much of this waste sand at a price that pays for the handling of the sand. In other situations there is the additional cost of hauling this sand away.

Because of these considerations, it was decided to install a sand reclamation unit of the "wet" type at Sharon Works and operation of the unit was begun on May 5, 1949. It was recognized that such a unit would not be panacea for all of the problems involved; indeed it has injected other problems into the picture such as (a) that of securing moisture control of the reclaimed sand to an extent that would allow a uniform level to be reached through the drying operation, or (b) that of sludge disposal, to mention only two of the problems. It was fairly obvious, however, that if the reclaimed sand could be used interchangeably with new sand with no detrimental effects in casting quality; and if the sand could be reclaimed for approximately $2.00 per ton, then economic and operating benefits would be very attractive.

Our experience has been in general quite satisfactory. With normal operation the unit has consistently reclaimed about 1000 tons of sand per week, of which perhaps 700 tons were used in the foundry and 300 tons in the core room. New sand in the amount of perhaps 15-20 pct of these amounts is required as make-up to replace sand lost in (a) the core lumps that may not be broken up in the barrel, (b) as sludge, and (c) at miscellaneous places, as in sweepings, etc.

In our practice, the core sand is bonded with synthetic resin (of the phenolformaldehyde type) but this binder does not seem to exert any influence in the efficiency of the washing operation and the reclaimed sand can be used interchangeably with new sand in the core room. It is perhaps also of importance to note that we have standardized on one base sand which is suitable both for core production as well as on one green facing sand that is used at all of our molding units. This represents a considerable accomplishment over the period of the last ten years for during that period we have cut down from 6 or 7 core mixes, requiring two types of new sand to three principal mixes made from one sand. Similarly we were using two types of sand in our facing and supplying three or four kinds of facing sands to the various units and this is now standardized on one facing

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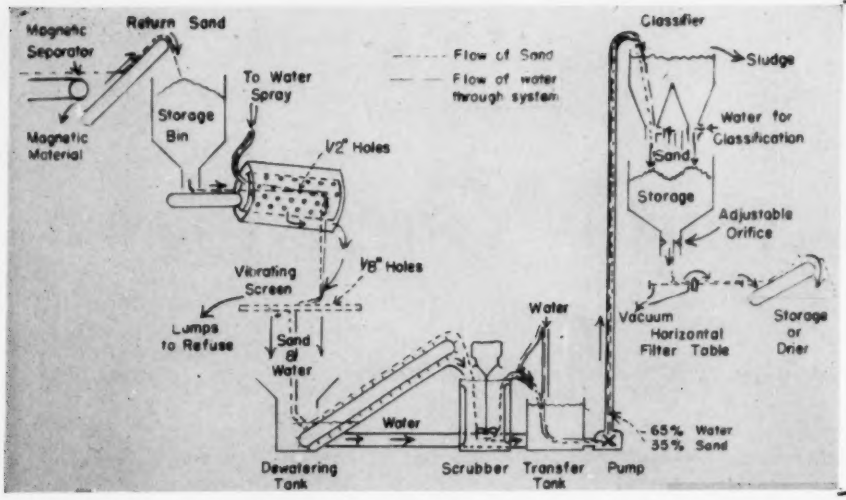


Fig. 1—Schematic diagram of the sand reclamation system.

and one backing sand. Such standardization is almost mandatory if the reclaimed sand is to be used most effectively.

The reclamation unit used at Sharon Works is essentially a standard unit rated at 15 tons per hour, such as has been described by both the manufacturer[1,2] and by users.[3] In fact another paper in this Symposium describes the pilot plant used by this manufacturer and therefore the authors shall only broadly outline the equipment involved in their installation. A schematic diagram (Fig. 1) shows the principal pieces of equipment that are involved and the flow of sand and water through the system. A photograph of the installation is shown in Fig. 2. As noted, about one-third of the sand prepared goes into cores while two-thirds is used as green sand and hence the reclaimed sand is used in this proportion.

Operation of Sand Reclamation Unit

For reclamation, the waste sand is put through a lump breaker which is merely a revolving barrel into which a stream of water is directed. This water stream and the rotating action of the barrel breaks down the lumps and prepares most of the sand for further treatment. The center portion of the barrel is perforated so that the sand and water mixture can discharge through this perforated section. It then passes over a vibrating screen which removes all the lumps that have not been broken up in this treatment and the sand and water mixture then goes into a dewatering tank. Oversize material is rejected to a waste box and tramp metal retained in the barrel also goes into this waste box. From the dewatering tank, the sand *can* go directly to a classifier and from this unit go over the vacuum dewatering table and be ready for use.

However, in our experience so much clay and other inert material remained on the sand grains after this treatment only, that a considerable amount of extra synthetic resin was required for bonding the core sand. Because of this difficulty, the installation of the scrubber was felt to be economically justified. Such a scrubber is nothing more than a big barrel with a paddle arrangement to keep the sand and water mixture vigorously agitated. The scrubbing operation is accomplished with a mixture of about 25 pct water and 75 pct sand (by weight). This allows grain to grain contact and the abrasive action scrubs off much of the adhering fine material on the grains. The sand now goes into a transfer tank or sump from which the mixture of sand and water is pumped to the classifier.

In the type of classification used in this unit, acceptable sand settles downward against a rising current of water, the velocity of which is controllable. The rising current of water is the means by which the reject material is removed and this operation pretty much controls the grain distribution of the

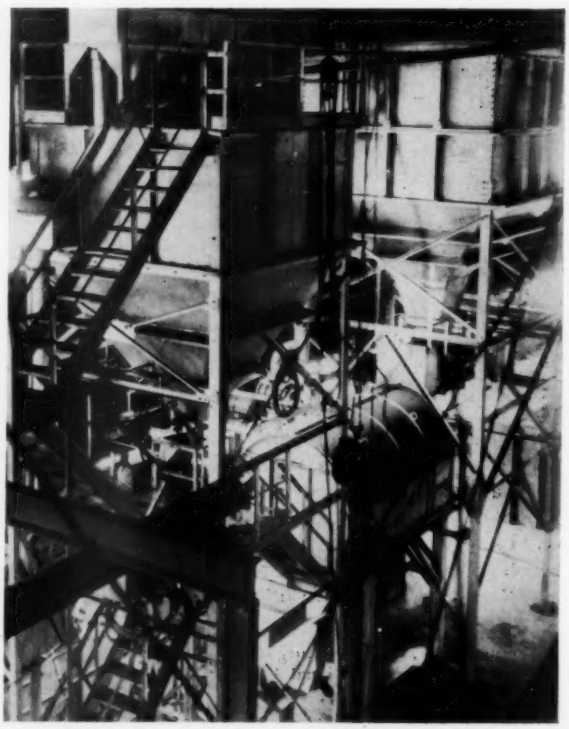


Fig. 2—Photo of sand reclamation system.

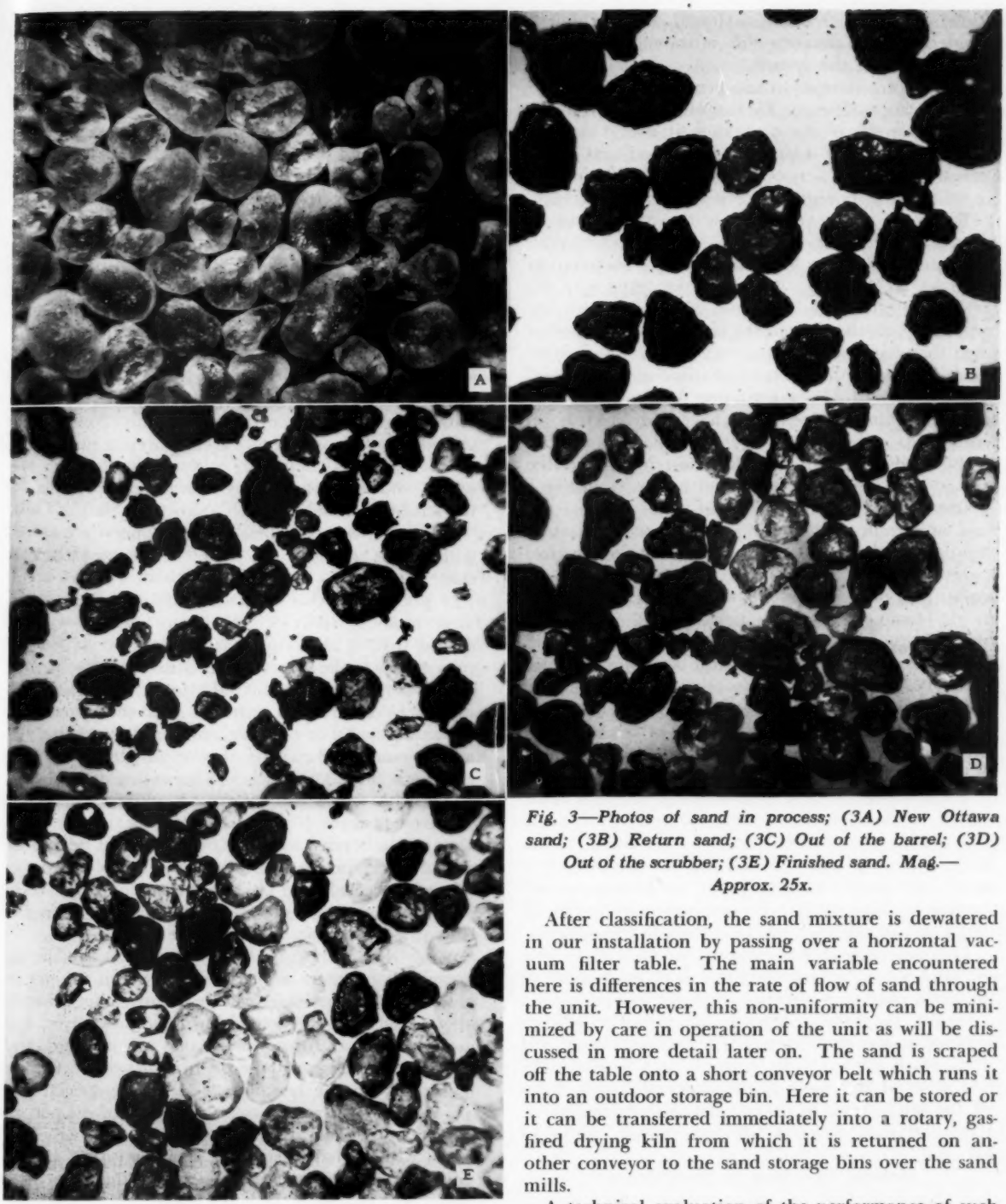


Fig. 3—Photos of sand in process; (3A) New Ottawa sand; (3B) Return sand; (3C) Out of the barrel; (3D) Out of the scrubber; (3E) Finished sand. Mag.— Approx. 25x.

end product. As the counterflow water is increased, the fineness number is decreased to a certain extent and it is claimed that it is possible to materially change the grain distribution but we have never tried to accomplish much along that line. It is important to maintain a uniform flow of the sand to the unit and to keep the velocity of the incoming water to the classifier adjusted to compensate for changes in the volume of sand to be classified.

After classification, the sand mixture is dewatered in our installation by passing over a horizontal vacuum filter table. The main variable encountered here is differences in the rate of flow of sand through the unit. However, this non-uniformity can be minimized by care in operation of the unit as will be discussed in more detail later on. The sand is scraped off the table onto a short conveyor belt which runs it into an outdoor storage bin. Here it can be stored or it can be transferred immediately into a rotary, gas-fired drying kiln from which it is returned on another conveyor to the sand storage bins over the sand mills.

A technical evaluation of the performance of such a unit must consider (a) the efficiency of the unit in removing the adhering fine material on the sand grains, thus restoring them to their original condition; (b) the grain distribution of the reclaimed sand, as compared with that of the new sand; (c) the physical properties of the sand mixes made from this sand, as compared with the specifications that have been set up for them; and (d) the appearance of the castings made from the reclaimed sand, with particular reference to defective castings produced under these con-

ditions. Such an evaluation should also include a consideration of operating difficulties encountered in the operation of the system.

A series of photographs are presented in Fig. 3 to show the appearance at 25 magnifications of representative samples of the sand taken at several steps in the operation. The base sand is washed and dried Ottawa Sand with a nominal A.F.S. Grain Fineness No. of 50 and its appearance is shown in Fig. 3a.

Figures 3b, c, d, and e show the appearance of samples taken at several steps in the reclamation. The used sand as it is being returned for reclamation is shown in Fig. 3b. As would be expected, the grains, while basically the same, are coated with the by-products resulting from the reaction of the binders when they have been heated in the mold. Decomposition, distillation, and condensation of the several binders involved plus the coating action of the clay and other binders used that have not reacted, all combine to materially change the surface conditions of the sand grains and would result in undesirable properties if the sand was re-used in this condition.

Howard and Jenni[4] point out that "One of the most important effects resulting from the contact of the compounded mold or core sand with molten steel is the coating of the sand grains with a firmly adhering film of clay binder and carbonaceous matter. At the temperature of molten steel, the water of crystallization of the clay compounds is driven off and the clay or bentonite film on the grains has distinctly different properties from the orignal binder. The carbonaceous matter results from the decomposition of organic compounds present in the mix." In order for the sand to be used again this coating must be removed and the obvious measure of the efficiency of the reclamation unit is how nearly the sand is returned to its original surface condition.

In Fig. 3c is shown the effect of the washing action while the sand is in the first step of the unit (the revolving barrel) where the lumps are broken up and the first washing occurs. By comparison with Fig. 3b the beneficial effects of this first washing are evident.

The next step in the reclamation cycle is to pass the sand through a scrubber and here again more of the grain coating is removed as shown in Fig. 3d.

Finally the sand is passed into a sump tank, is pumped to the classifier unit and passes through this unit, which steps remove more of the clay and other fine material and more of the loosened coating from off the sand grains so that as the sand leaves the reclamation unit it looks like the sample shown in Fig. 3e.

Material Remaining on Sand Grains

Much of the grain coating that we are concerned with removing is an inert, clay material but some of it is combustible. In order to get an approximation as to the amount of this clay-like material and of the combustibles present after each of these several steps, representative samples were analyzed for carbon and A.F.S. clay determinations were also made, with the results shown in Table 1.

Here we have a somewhat more quantitative measure of what has happened than is obtained by merely looking at the samples taken at the several steps in the process. A comparison of Figs. 3a (new sand) and 3e (the finished reclaimed sand) indicates that the grains maintain the same shape and size but that some of the grains are still entirely or partially coated, while many of them are very clean, except for a few tiny spots on them. (These spots may be of importance and we shall refer to them later in the discussion). This observation is confirmed by the fact that analysis shows but 0.2 pct combustible material and 0.4 pct clay remaining in this sand. It is generally accepted that less than 0.35 pct combustible matter will give a satisfactory sand. It is also of interest that while only a part of such matter is actually removed in the scrubber itself, it apparently has been loosened until it can readily be removed in the classifier.

Mention was made previously of the fact that the phenol-formaldehyde type of resin is used as a core binder and one of the problems was that of determining how well the polymerized coating could be removed from the sand grains. Apparently the grains can be cleaned satisfactorily as shown by the data presented and as confirmed by operating experience, if the scrubber is included in the unit.

When the unit was first put into operation, it was without the scrubber and therefore the apparent clay content was being reduced from about 6 pct to about 1.25 pct. When this sand was bonded for the green sand molding, only slightly more binder was required than if new sand was used. However, in the core mixtures the clay content is undesirable because a considerable amount of additional binders are required to absorb this material and these materials are expensive. For example, we found it necessary to add as much as 3 gallons of core oil per ton of sand to absorb this clay, which was an expensive addition. Along with the expense of the oil, additional cost was involved because of the observation that when cores were made bonded with any oil, they must be washed in order to produce a casting finish equivalent to that of an unwashed core bonded with a phenolic resin. In general, a poorer casting surface is produced if any oil is used than if resin alone is used.

After the scrubber was put into operation, the apparent clay content was reduced to about 0.4 pct, so that no additional binders are required for most of the production. For some blown cores, however, 6 lb western bentonite are added per ton of sand and, therefore, 1 gallon of core oil is used per ton of sand to react with the bentonite. For a few special purpose cores, such as cup cores, 3 gallons of oil per ton are added to the resin mix to produce extremely hard cores with high hot strength. This mixture also requires 10 pct silica flour and again the core oil is added to react with the silica flour.

TABLE 1—MATERIAL REMAINING ON SAND GRAINS

Sand Condition	Combustibles (As Carbon), %	Apparent Clay, %
As returned from foundry	0.56	5.6
Through revolving barrel	0.37	5.2
Through scrubber	0.33	2.0
Through classifier (into yard)	0.20	0.4
Sludge (waste)	0.66	
New Ottawa sand	0.05	0.2

Grain size distribution of the reclaimed sand gives another interesting criterion as to the performance of the system. Typical distribution charts both for new sand and for the reclaimed sand are superimposed in Fig. 4 and these show nearly the same distribution.

As a check on the uniformity of the grain distribution of the reclaimed sand, samples were taken at 1½-hr intervals during the course of a 16-hr run, as the sand was being discharged into the storage bin. The range of the grain distribution is shown in the charts of Fig. 5 and the variation in the amount of fines and of clay content is shown in Fig. 6. The amount of variation is within the expected range and reflects differences to be expected in sampling and analysis as well as any real differences in the sand itself. Certainly the amount of fines (through the 100-mesh screen) and the level of clay content reached are unexpectedly uniform over the period under observation.

Green Properties of Reclaimed Sand

The green properties of the sands prepared from the reclaimed sand should also afford a good measure of its ability to replace new sand and this has been found to be the case.

In Figs. 7a, b, and c are presented quality control charts, taken from the authors' files, showing moisture, permeability, and green bond control for the period of February-May, 1952. The charts are of the conventional $\overline{X}$ and R type, using fixed control limits. Each individual point represents the average value for the 8-hr turn and may be the average of 20 or 30 determinations from that turn. These points are plotted as groups of four values and the average for each group is shown as a circle. As long as these average points stay within their control limits, and fluctuate about the average value, and as long as the process is operating at the desired levels we are satisfied that it is in control. Our experience has been that the use of reclaimed sand lent itself to very good control, of which these charts give a typical picture.

The same is true for the results on core sand, when uniform conditions are maintained, as shown in Figs. 8a, b and c. The period of time represented is essentially the same as for the facing sand charts. Here, again, the data are presented as $\overline{X}$ and R charts, using fixed limits. In this case, however, we have introduced a new variable during the period in the form of using new sand for a couple of weeks because the supply of reclaimed sand ran very low. The effect of this new sand on all the properties is quite evident and interesting. When the reclaimed sand was used again, the moisture and bond settled down to the level and range that they had previously held. The permeability level stayed slightly higher after the reclaimed sand was resumed, but it finally drifted back inside the control limits, as shown.

It will also be noted that near the end of March the moisture level was purposely changed, with the attendant increase in green bond as shown. The degree of control of the physical properties using reclaimed sand is entirely satisfactory and the molding characteristics are also acceptable.

An interesting example of variations that can be encountered in the operation of the reclamation system, was that of the difference in the moisture level of the sand leaving the system when the scrubber was being used and when it was out of the cycle for repairs. Typical data are shown in Fig. 9. Here are plotted, as daily average values, (and not as quality control charts) moisture values from three locations: (a) out of the reclaim unit; (b) out of the storage bin and into the drier; and (c) out of the drier and into the muller.

The arrows indicate the dates on which some sand from the scrubber was put back into the cycle and that on which 100 pct of the scrubbed sand was again being used. No other known changes were made, yet it is quite evident that the moisture level, out of the reclamation unit, of the scrubbed sand is holding quite uniform at a level of about 8 pct, while without the scrubber, it was fluctuating considerably at a level of about 9 pct. It would seem as if the scrubbed sand, being a somewhat "cleaner" sand, drains a little better than the washed sand. This effect is further accentuated in noting the amount of water lost during storage, as shown on the chart, although the moisture loss in the bins probably depends to a greater extent on weather conditions and temperatures.

The data which the authors have gathered seem to indicate that the moisture loss in the drier itself is approximately 70 to 75 pct of the incoming moisture regardless of the initial level. A more uniformly dry and a drier sand is produced from the scrubbed sand and this is of great importance in setting up the moisture control procedure for the mill men. It has resulted in considerably improved moisture control in both the facing and core sands.

It also was observed that when scrubbed sand was used instead of merely washed sand, the moisture level of the core mixes could be lowered perhaps 0.5 pct as shown in Fig. 10a and still produce satisfactorily hard cores. The attendant drop in green bond with moisture, is shown in Fig. 10b, which drop was corrected by the addition of more cereal to bring the bond up to its former level. The introduction of the scrubbed sand also changed the permeability, as shown in Fig. 10c. No attempt was made to control the permeability, but it was allowed to assume its level and new control limits were then established at this level.

Surface of Reclaimed Sand

These examples have been cited as typical operating situations that have been encountered in the application of the reclamation unit. When proper controls are applied, advantage can be taken of the properties of the washed sand to produce excellent sand mixes. In fact, the operating men prefer this sand to new sand from the viewpoint of flowability and ramming. Apparently the little spots on the sand grains, mentioned earlier in this discussion, act to "key" the bonded grains together more firmly than do the smooth surfaces of the new grains. This is a characteristic that it is hard to measure but one which is very real to the men who are molding with the sand.

The use of the reclaimed sand has also been reflected to some extent in the baked properties of the

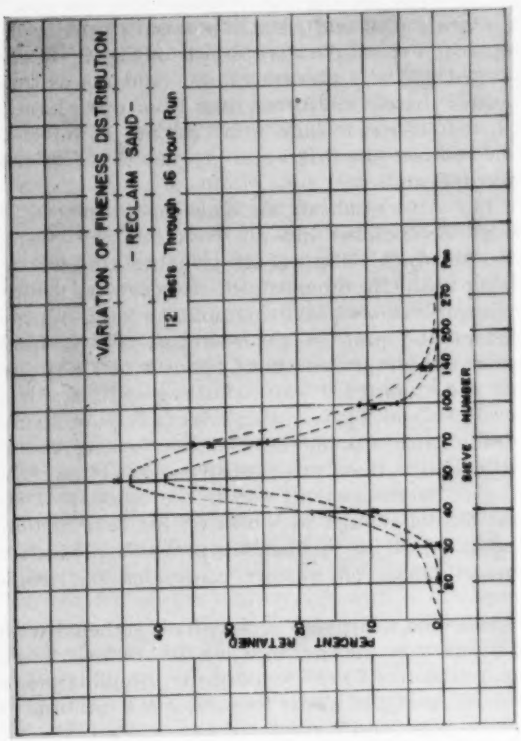


Fig. 5

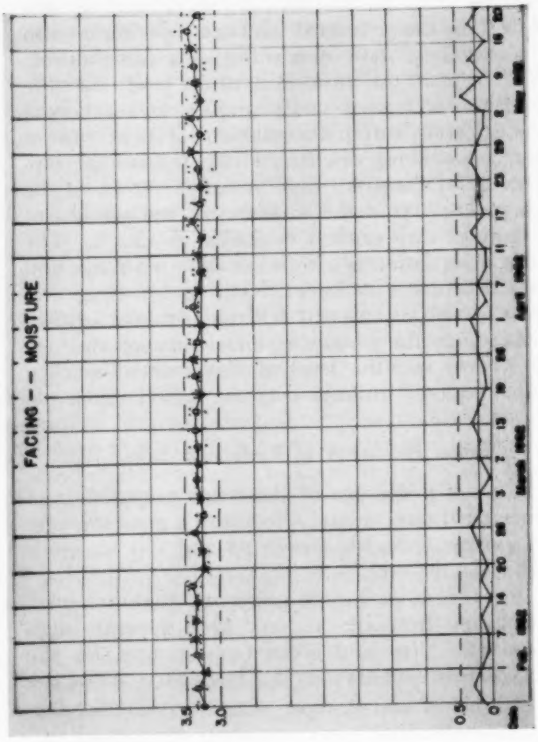


Fig. 7A

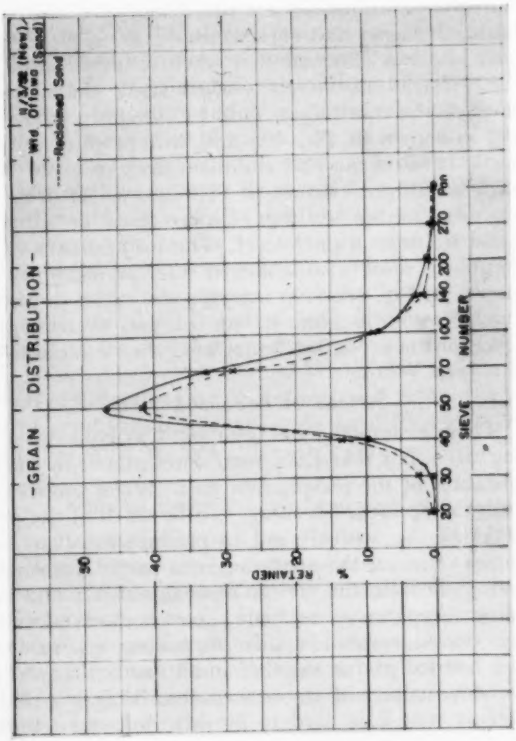


Fig. 4

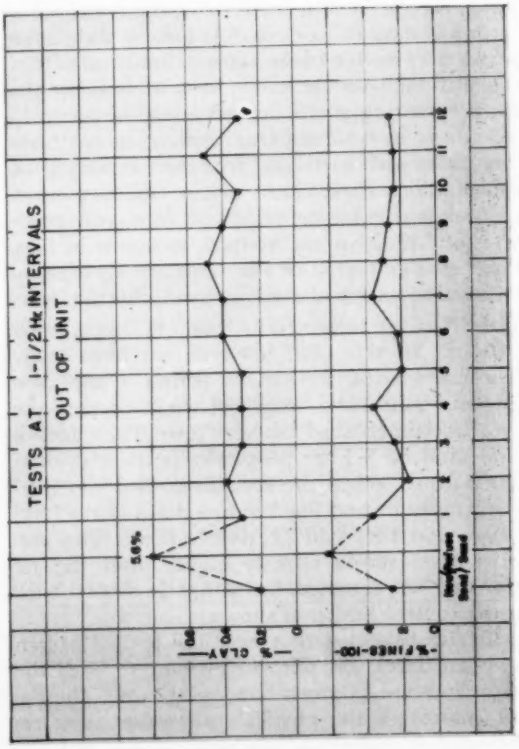


Fig. 6

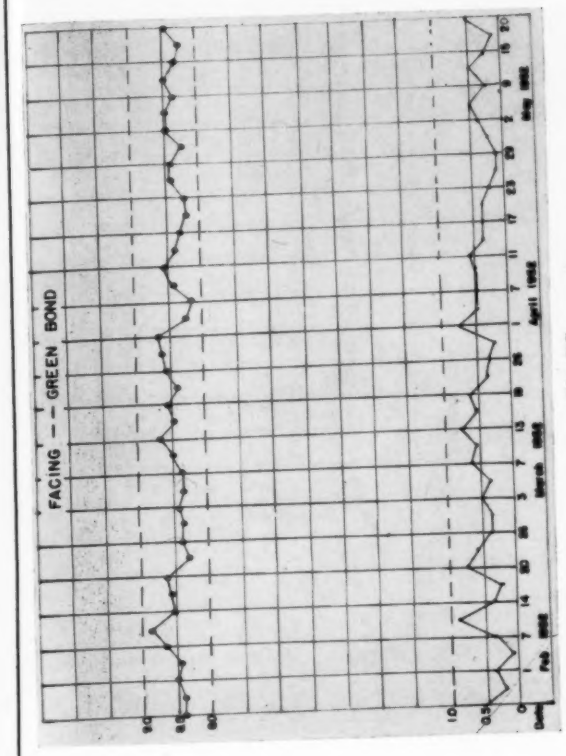


Fig. 7C

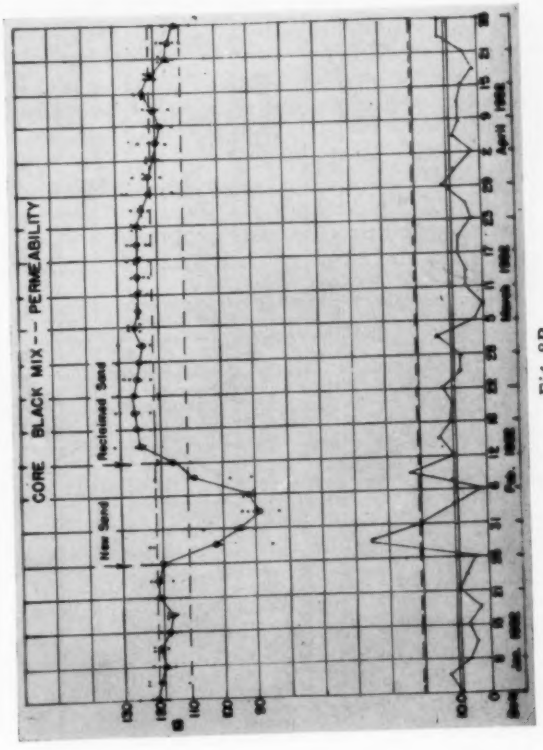


Fig. 8B

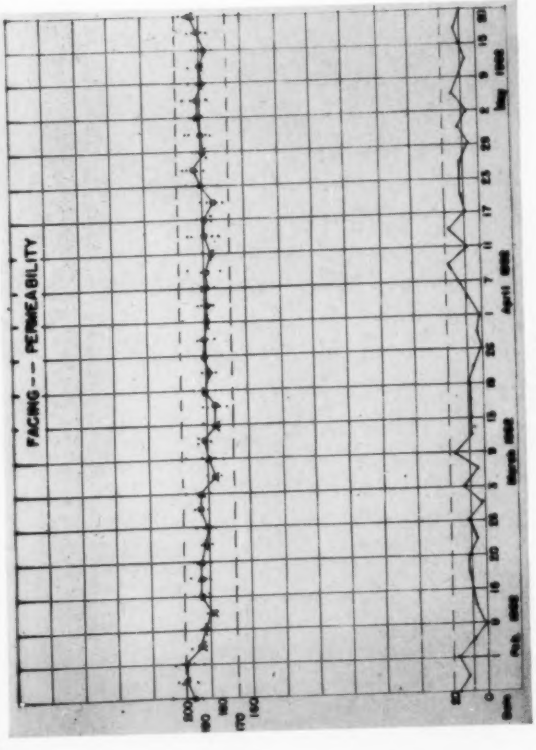


Fig. 7B

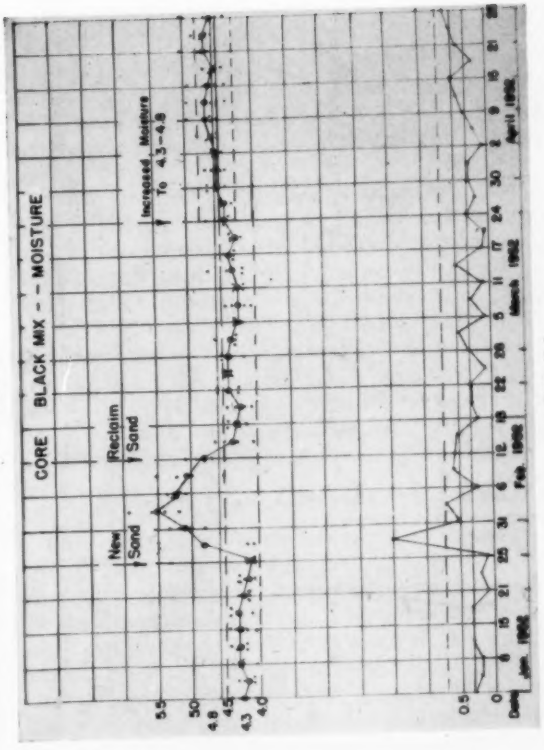


Fig. 8A

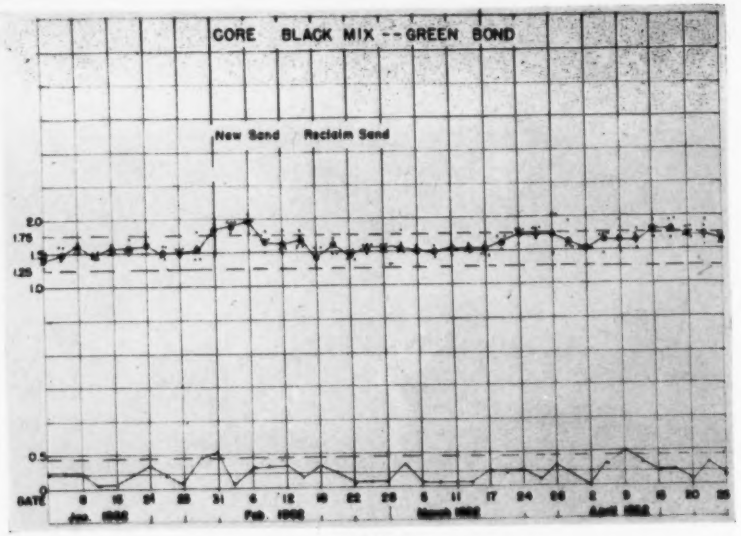


Fig. 8C

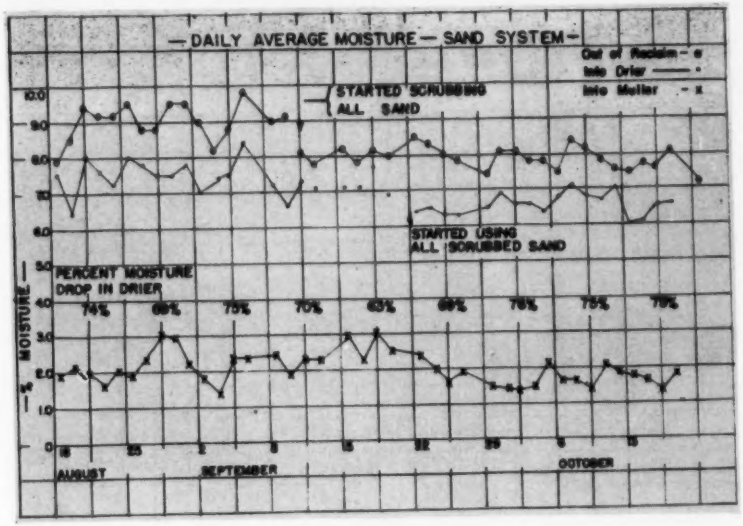


Fig. 9

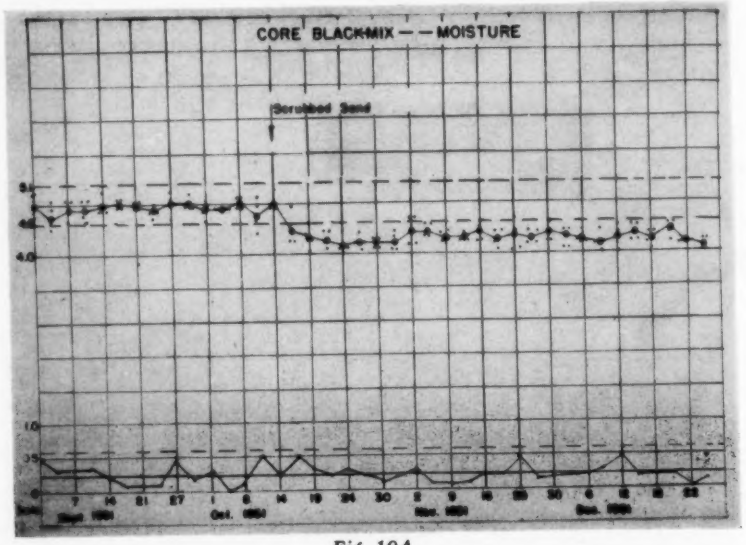


Fig. 10A

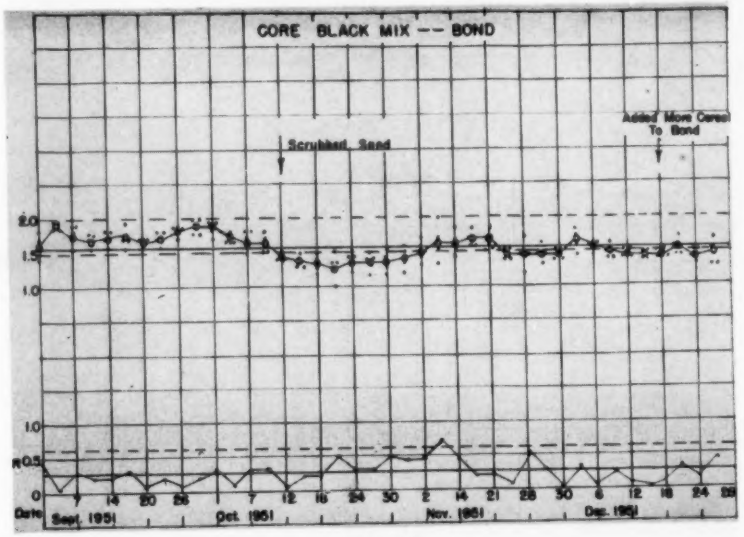


Fig. 10B

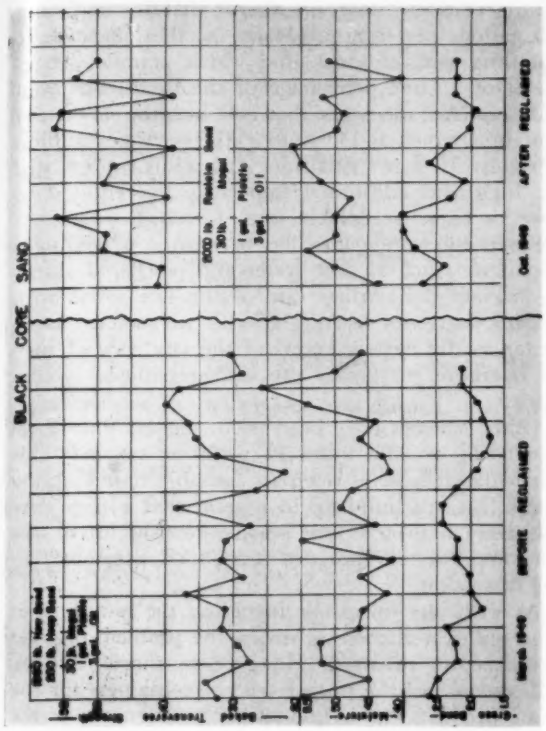


Fig. 11 (Right)

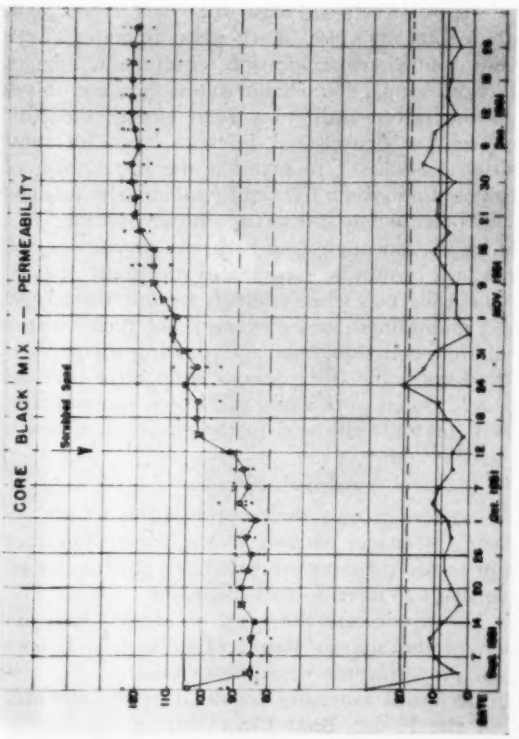


Fig. 10C (Left)

cores produced. The authors use the transverse test (instead of the more commonly used tensile test) for measuring the baked strength of the cores, averaging the results of three test specimens from each mill of sand so tested.

In Fig. 11 are shown data representing a period before the reclamation unit went into operation as compared with data from similar period after the unit went into use. The results are not exactly comparable because the sand mix was also changed when the reclaimed sand became available as shown in Table 2 and these changes partly account for the change in level in baked strength.

Table 2—Core Sand Mixes Used Before and After Installation of Reclamation Unit

Before		After	
New (washed) Sand, lb	1800	Reclaimed Sand, lb	2000
Heap Sand, lb	200	Cereal Binder, lb	30
Cereal Binder, lb	30	Synthetic Resin, gal	1
Synthetic Resin, gal	1	Core Oil, gal	3
Core Oil, gal	3		

The charts show that the green bond level and the moisture level remain fairly constant for the two types of sand but the baked strength of the reclaimed sand shows a marked increase. Part of the reason undoubtedly is the effect of the use of 200 lb of heap sand which was added to the new sand to give green strength but which acted as an inert, fine material so far as baked strength is concerned. Sufficient green strength was developed in the use of washed sand without the addition of this heap sand and when the scrubbed sand was used later on there was no need for the core oil addition to combine with the inert material.

As a general observation, cores made using reclaimed sand are equal to or better than those made using new sand and, as noted above, the reclaimed sand flows better and rams more easily than the new sand.

While the authors have not made any test castings to evaluate scabbing, etc. (such as described by the Steel Founders' Society,[5] inspection of their castings has indicated that there was no marked difference between the castings made before and after the start of the reclamation unit. Neither has there been any indication of a change in scrap level that could be attributed to the change in sand practice. As noted above, the reclaimed sand seems to ram more easily than new sand and it is the opinion of some of our people that the casting surface is improved by its use.

Reclamation Unit Operating Precautions

In the use of any equipment such as this, there are always operating precautions which help to make for better operation. Among such precautions may be mentioned such items as (a) maintaining a reasonably uniform rate of flow of sand to the unit; (b) maintaining a fairly uniform thickness of cake on the vacuum filter table; (c) disposal of the waste water produced in a manner that will be satisfactory; (d) maintaining good housekeeping around the unit.

These are among the factors that obviously require consideration in the efficient operation of the unit,

but there are many others that should be measured and controlled. For example, it is evident that when the water and sand input are set for one rate, any interruption in the sand input will markedly change the sand:water ratio as well as markedly decrease the amount of sand reclaimed by the unit. Changes in the sand:water ratio will be finally reflected in the moisture content of the sand as discharged from the unit and this, in turn, is reflected in the moisture of the sand out of the drier as has been indicated. For example, we had two operators, working alternate turns, one of whom (Operator A) was able to control the moisture level out of the unit fairly consistently under 8 pct, while the other operator (B) fluctuated markedly, as shown in Fig. 12. A study of B's work showed that he was somewhat careless about maintaining a uniform flow of sand into the unit. If a batch of cores were sticking in the feed hopper he would wait an unduly long time before he broke them loose. After some discussion with him and after some operating changes were made, his operation improved until it equalled A's operation, as shown in Fig. 12.

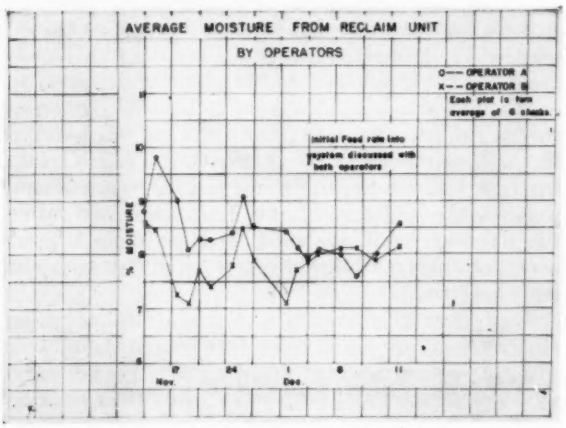


Fig. 12

Similarly such interruptions in sand feed and similar difficulties act to change the thickness of the cake maintained on the rotary pan drier and this in turn affects the operation of the unit. Thus, when the cake thickness is about 2 in., the unit is operating normally. If the thickness drops to 1 in., the amount of sand being reclaimed materially drops and an unusual amount of fines are removed from the sand thus changing its grain fineness and causing an undue waste of such material.

As for any mechanized operation, it is desirable to maintain a system of careful measurements of the various critical steps in the process and to keep these charted preferably in the form of quality control charts. In this way, a constant picture of performance is maintained.

Disposition of the waste water produced is perhaps the problem of most concern to many users because they cannot discharge it into a sufficiently large body of water to minimize the effect of the solids that are in suspension. The volume of water to be handled is also a factor, amounting to perhaps 100 gallons per minute of waste water in a unit that is circulating 300 gallons per minute. Many installations include a settling tank of some kind which permits the removal of a large percentage of the solids, but even with a scheme the water that goes into the sewer may contain as much as 15,000 to 35,000 parts per million of solids. In many situations this solids ratio is still too high and additional steps may be required to lower it to an acceptable level.

For example, the idea has been tried of adding a flocculant (such as lime water and powdered alum) to increase the settling rate of the fine solids with varying degrees of success. There is no good evidence, so far as the writers know, of the efficiency of such an operation so far as the sludge removal is concerned.

Other schemes have been tried, such as that of recirculating all the water in a closed system. This particular scheme is open to the objection that the fine solids will build up to a point that will be prohibitively high with the attendant troubles in the core and foundry practice mentioned previously in the discussion.

As with any operation involving the handling of sand and water, there is always the problem of good housekeeping involved. However, by careful and intelligent use of the proper type of containers for the waste products and by the proper maintenance of the equipment involved, spillage and splashing of the sand and water mixture can be kept to a minimum. On the other hand, there is practically no dust problem involved. Complete mechanization of the handling of the sand in and out of the unit is also a great help in simplifying the problems of housekeeping.

No attempt has been made to present comparative operating costs because it is felt that it would be difficult to set forth cost data that would be meaningful to some one else. Each plant operates under different conditions of location, equipment, type of production, tonnage and labor rates, to name only a few of the factors and, to a great extent, the costs themselves are figured on different bases for most operations. Suffice it to say that the installation of the reclamation system has been economically justified as well as technically satisfactory in our opinion.

We would repeat that, as with any production setup, the unit cannot be expected to run itself. It operates most efficiently when controls are intelligently set up and maintained. Some examples of such controls and of the results obtained by applying them under the conditions prevailing at our shop have been set forth. Additional work along this line is contemplated and will undoubtedly add to the efficiency of operation of this unit.

Acknowledgments

The writers express their appreciation to Mr. W. M. Ewing, Manager, Sharon Works, for his kind permission to use the data presented and for his fine encouragement to develop this discussion.

We also appreciate the work of Messrs. Ellis and Snyder, of the Sharon Works Metallurgical Laboratory for preparing the illustrations used.

The technical assistance provided by the technical staff of the Hydro Blast Corp. during the days of

planning and installation of the unit are also acknowledged and appreciated.

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DISCUSSION

Chairman: T. W. CURRY, Lynchburg Foundry Co., Lynchburg, Va.

Co-Chairman: G. M. ETHERINGTON, American Brake Shoe Co., Mahwah, N. J.

Recorder: R. W. BENNETT, Walter Gerlinger, Inc., Milwaukee.

MEMBER: What is the source of the sand going to the reclaimer?

MR. JOHNSON: All of the used sand (from the several shakeouts) is returned by continuous belts to the sand preparation department. Here it passes over a magnetic pulley to remove any magnetic material that may be left in the sand and then it goes through a rotary screening barrel to remove the lumps. It passes over another magnetic pulley to further insure freedom from magnetic material and then it can go either into storage hoppers or directly to the reclamation unit. The lumps which were removed in the rotary barrel all go to the reclamation unit.

As noted in the paper, only one base sand is used in the shop and, therefore, there is no problem of keeping sand from different systems separated or of furnishing one type of sand to the core department and another type to the foundry.

J. A. RASSENFOSS: [1] How effective is the wet lumpbreaker?

MR. JOHNSON: There is about 5 per cent loss at the lumpbreaker.

D. G. JENCKS: [2] What is the maintenance cost of the reclaiming unit per ton of sand processed?

MR. JOHNSON: About 25 per cent of the cost of operation of this sand reclaiming unit represents maintenance cost.

W. R. JENNINGS: [3] For operating a similar unit we require one operator and one maintenance man to process 300 tons of sand per day.

D. E. MATTHIEU: [4] What is the cost of reclaiming sand per ton?

MR. JOHNSON: The cost of reclaiming sand in this unit is less than the estimated $2.00 per ton, all costs considered.

MEMBER: Is heat from the melting unit used for drying?

MR. JOHNSON: No, we have a rotary dryer for this purpose.

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[1] Assistant Research Director, American Steel Foundries, East Chicago, Ind.
[2] Plant Engineer, Grede Foundries Co., Milwaukee.
[3] Foundry Supt., John Deere Co., Waterloo, Iowa.
[4] Chief Metallurgist, Alabama Pipe Co., Anniston, Ala.

SAND RECLAMATION WITH THE COMBINATION SYSTEM

By

G. H. Curtis*

In recent years foundries have become more and more interested in the possibilities of reduced operating costs by conservation of foundry materials. Foundry sand has come in for its share of developmental work for the purpose of determining the most suitable method of recovering the waste sand and making it repeatedly useful. Although sand is one of the most abundant materials, we all know that industry's demand for quality castings necessitates the use of a sand that is not always locally available; and in fact, it may not be available in nature without some degree of processing. With the trend toward quality control many foundries have insisted on properly classified sand and are prepared to pay premium prices and high transportation costs to make the ideal sand available for their use.

It is not logical, once money and energy have been spent in obtaining the proper sand, that sand should be discarded after one cycle through the casting process. For this reason the reclamation of the waste sand has great value to the foundries' management; first, because it can produce sand at a lower cost than new sand, and second, because excellent control may be maintained over the properties of the sand.

The purpose of this paper is to high-light the important steps of sand reclamation with the combination system of wet-scrubbing and burning, also to indicate the possibilities and limitations of the thermal method alone.

Benefits of Reclamation

Much has been written in literature in regard to the benefits of reclaiming refuse sand and no attempt here will be made to repeat what has already been said. Undoubtedly the two most important factors which are weighed in any consideration of sand reclamation are: (1) the lower cost of a ton of reclaimed sand and, (2) the control of sand properties which the foundries may maintain with a closed sand system.

There are many additional reasons for a foundry to install a reclamation system. Some of these are of a specific nature, arising out of a geographical location, the amount of space available and transportation facilities.

The most tangible benefit is, of course, the dollar saving that can be made by the substitution of reclaimed sand for new sand. There is little doubt, in the face of accumulating evidence, that sand reclamation does not provide substantial savings for the foundry which is not situated strategically to its sand source or which uses large quantities of sand. To the foundryman who wants the most for his dollar, the combination system offers the best investment by reason of the fact that he is getting the highest quality for the invested dollar.

One of the intangible benefits of sand reclaiming is the ability of the foundry to maintain a constant grain



Fig. 1—Photomicrograph of reclaimed sand.

* Sales Engineer, Nichols Engineering & Research Corp., New York.

distribution over a long period of time. Because the grain distribution is constant, and the sand is normally dry when it is conveyed to storage, excellent properties at the mill are possible. This permits of consistent quality of mulled facing and core sand, and improves casting quality, at least in so far as sand contributes to quality.

The sand reclamation system that is properly designed should be capable of producing a sand that is equivalent to new sand, and should produce such a sand at a much lower cost. The degree of cleanliness required in the sand will be dependent upon the ultimate use to which it will be put. For a broad range in size and weight of castings, and for widely different bonding materials, the aim should be the removal of all deleterious substances which the sand has picked up during the casting process. If the foundry has made it a practice to purchase a clay-free, organic-free sand when they are buying new core sand, it would seem logical to expect that similar specifications should be used for a sand produced by themselves. The combination system, with its wet-scrubbing and high temperature burning, will produce such a sand.

Figure 1 is a photomicrograph of a steel foundry reclaimed sand after removal of clay and organic matter by the combination system. Note the brilliance of some of the silica grains. The actual color is tannish white.

Principle of the Combination System

Foundry sand is subjected to a wide range of temperatures and heat shock. The facing sand at the metal-sand interface, comes into contact with the highest temperatures; and, at this point, the organic is partially oxidized and the clay is dehydrated. The organic constituents in the cores are partially burned, and the core disintegrates. At a greater distance from the metal-sand interface, the dehydration of the clay is not as complete, however, sufficient temperatures exist to bake the clay bond. Organics are present in a charred state.

The purpose of a proper reclamation system would be the removal of all these partially dehydrated clays and partially oxidized organics. The manner by which the combination system accomplishes this will be described.

The first phase consists of a swelling and softening of the clay bond in water combined with a scrubbing action. By the abrasive action of sand upon sand, the clay is separated from the grain and made ready for removal in a classification step.

The second phase of the combination system is the removal of the organic matter still present and unaffected by water treatment. The organic, whether it be oil or resin, is nearly impervious to water. Some of this matter is removed in the wet-scrubbing phase chiefly by attrition. This being so, the cleanliness of the sand after the first phase is a function of the grain shape. A round smooth sand of the Ottawa type will be cleaner than an angular sand such as the many lake sands and the silica sands in the East.

In this second phase the carbonaceous matter is merely burned off in an oxidizing atmosphere. The cleanliness of the final product is complete, and in no way dependent on grain shape.

The principle behind the combination system is the removal of clay by water-scrubbing and the oxidation at high temperatures of the organics which are a residuum of the wet-scrubbing phase.

The Combination Process

A flow diagram of the combination system is shown in Fig. 2.

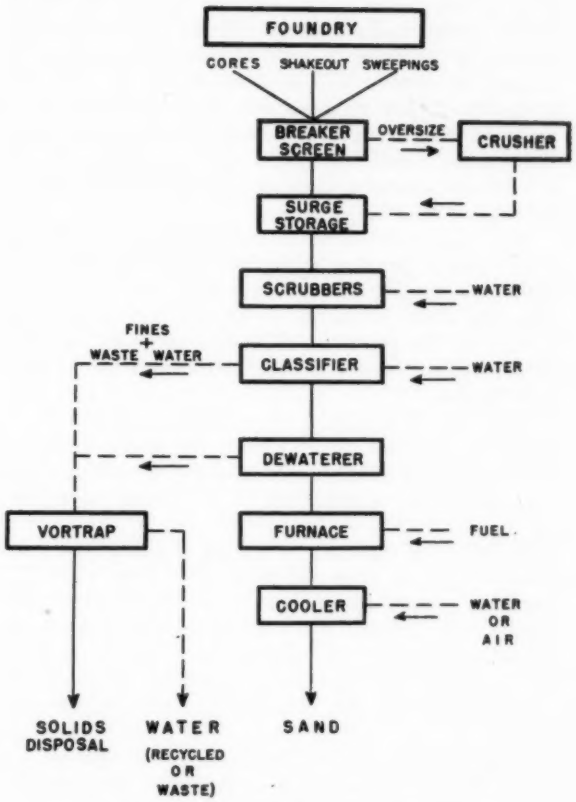


Fig. 2—Flow diagram of reclamation process.

Waste sand has several sources in the foundry. Excess shake-out sand, core butts, core knockout sand and floor sweepings are all combined in many reclamation systems prior to processing for re-use. Generally it is the practice to put this sand over a scalping screen and through a breaker barrel to reduce the lumps to the proper size. Lumps that are not broken up in the first pass through the breaker can be crushed by a hammer-mill or merely recycled through the barrel till they are reduced.

In this preparatory step it is also necessary to remove iron chills, core rods, wires, etc., either on picking belts, magnetic pulleys or screens.

The crushed sand is stored in a hopper from which it is fed at a controlled rate to the reclamation system.

In some cases, the sand may be conveyed hydraulically after it has been crushed. In this instance, the sand would be stored in a wet condition and withdrawn from the wet storage tank at a controlled rate by means of a pump.

The sand is now ready for the reclamation process, the first step of which is the scrubbing.

Scrubbing

The clay binder is loosened from the sand grains by intensive stirring action in water. The scrubbers are cylindrical steel tanks, ranging from 3 ft in diam to 6 ft, in which is mounted a vertical shaft and propellor.

In order to control the circulation within the tank, reduce short-circuiting to a minimum and to permit repeated contact of grains, the scrubbers are equipped with a mechanism which allows a vertical and horizontal rotary motion. Recirculation is controlled by raising or lowering a weir, thereby controlling the amount of slurry which is permitted to pass over the propellor. This recirculation principle provides a relatively lengthy period of contact of sand grain upon sand grain, until eventually the sand finds its way out of the scrubber through an outlet at the top or through a sand relief pipe.

The concentration of the slurry is about 50 per cent solids and 50 per cent water. Variations in concentrations have been tried with very little data to indicate that the ratio of solids to liquid is crucial. The slurry, at the point of discharge contains the clay, some organic matter in suspension and the cleaned sand grains.

The only function of the scrubbing step is to remove by abrasion the clay from the grain, therefore, the cleanliness of the water used for scrubbing is not too important. Reconditioned or process water may be so used if clear water is costly.

Because the difficulty of freeing the clay from sand varies with the type and amount of clay used, and with the temperature to which the sand has been subjected, it is customary practice to provide at least two scrubbers in series. Multiple scrubbers have the effect of increasing the time of residence in the units and insuring that all the sand will get the proper treatment.

The effluent from the last scrubber in the series is pumped to a hydraulic classifier where actual separation of clay and sand is accomplished.

The scrubbers are well designed for the heavy duty which they perform with a consequent low maintenance cost. A relatively slow speed shaft is used, but with a large propellor, high peripheral speeds are possible. The sand, as it passes over or near the propellor, receives a thorough scrubbing action. The propellors are of molded rubber construction which has proved to give longer life than castings. No bearings are submerged; yet despite the length of free shaft there is little "whip" to cause vibration and higher maintenance costs.

Classification

The classifier is of the "hindered settling" principle in which sand, of different particle sizes, is permitted to settle in an upward-moving column of water. In the real sense we are not interested too greatly in "classification." There is no attempt made to divide the whole body of sand into several categories of particle sizes. Rather than true classification, there is a separation here between the very fine fraction, consisting of minus 200 mesh, and the desirable grain sizes of 12 to 200 mesh.

Separation at predetermined particle sizes is possible by controlling of the counter-flow water. With the installation of flow-meters or venturis it is a simple matter for the operator to control the fineness of the end-product.

The fine silica sand, clay and any other substances which are floated off in the over-flow water go to a collecting box for further treatment. The sand still in a slurry is pumped to a mechanical de-watering device.

Before the final burning operation there is one more intermediate step. From the standpoint of operating costs it is oftentimes cheaper to de-water by mechanical means rather than using additional fuel, and for this reason one of several de-watering mechanisms is used. It is possible to reduce moisture in scrubbed and classified sand to approximately 6 to 8 per cent by centrifuging or filtering. By natural drainage, over a short period of time, the moisture is reduced to 20 to 25 per cent. The selection of one method over another is a matter of judgment and capacity of the plant.

Once the moisture in the sand has been thus reduced to the economical minimum the sand is ready for drying and burning.

A word should be said here about possible waste water treatment. If sewer protection is the decisive consideration it is merely necessary to remove all settleable solids. This may be accomplished by collecting

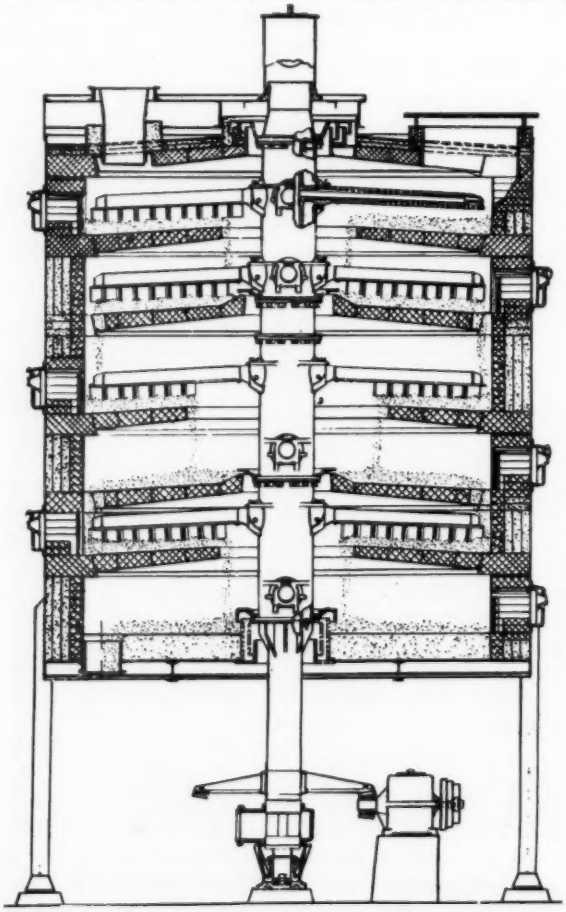


Fig. 2A—Sectional diagram of a multiple hearth furnace.

all the waste water from the classifier and the filtrate or concentrate, and pumping this water through a centrifugal type of classifier. This unit will remove all of the coarser grains of sand, and most of the 140-200 mesh material. The effluent from the centrifugal type classifier will be black in color, due to the suspension of colloidal matter, but any solids which would normally settle will now be discharged from the centrifugal type classifier as a thick mud.

Burning

The final step in the reclamation of sand by this process is the heating up of the sand to a temperature of 1200-1400 F in an oxidizing atmosphere for the purpose of removing all the organic matter that is present. For this operation a multiple hearth furnace is employed. Figure 2A is a sectional view of such a furnace. This furnace consists of a vertical steel shell which is refractory lined. Within this lined shell there are a number of self-supporting, refractory hearths properly spaced to permit combustion of the fuel within the furnace. The top-most hearth is generally an in-hearth, i.e. the material is moved through an annular hole at the center onto the next hearth below. The second hearth is called an out-hearth since it has a number of drop holes located at the periphery of the hearth. On this hearth the material is rabbled from the center to the outside. The rest of the hearths are alternately "in" and "out" hearths. The sand is rabbled across these hearths by means of two or more rabble arms equipped with blades. The arms are attached to the central shaft which rotates slowly. The shaft and the arms are air cooled.

Several burners are situated in the wall of the furnace on alternate hearths. Because it is possible to use a large number of small burners, excellent fuel economy is possible, with the furnace zoned according to the process requirements. For instance, it is possible to burn fuel on the middle and lower hearths and rely on the products of combustion on the upper hearths to evaporate the moisture from the sand being introduced at the top. As the sand progresses through the furnace, the moisture is removed and the sand is heated to approximately 1200 F by which time all the organic matter has been oxidized.

On each hearth the sand takes the form of several ridges which are continuously turned over, thus exposing new areas to the oxidizing gases as the shaft and rabble arms rotate. The result is the final elimination of all the substances which should be removed during the reclamation process.

Upon discharging from the multiple hearth furnace the sand must be cooled to a temperature which will permit proper mixing with binders and tempering water. This cooling operation may be accomplished in a vertical type heat exchanger using water or air. Another means of cooling is with the rotary drum through which is drawn a large amount of cooling air and into which is sprayed a predetermined amount of water.

The cooled sand is now ready for storage and re-use in the core sand mixers. The sand has almost the appearance of new sand. It is free from clay and organic matter; the fines have been removed.

Special Applications

The process as described above would apply to the iron and steel foundry's waste sand in which there is usually an indeterminate amount of clay to be removed. In recent years, however, particularly in the non-ferrous foundries, use has been made of dry sand molds. In such cases there is not likely to be any clay used and reclamation of the sand would involve merely the removal of organic matter. Where this particular situation prevails it is, of course, unnecessary to wet-scrub the sand. The easiest way of reclaiming this type of sand would be merely the burning off of the organics. This has been done in several aluminum foundries and has been reported in the literature. In these cases the fuel consumption is remarkably low and the recoveries are remarkably high. In one case 96 per cent of the sand that was put into the system was recovered.

Another special application for the thermal method is in the new field of shell molding. The general practice has been to use a high grade of sand of very fine particle size. This sand is bonded with resin type binders. Because of the fact that only organic binders are used, it is a simple matter of crushing the unused or incompletely burned shells and burning the sand in an oxidizing atmosphere to remove completely the partially oxidized resins. The resultant product would be exactly the same as the new sand purchased.

Even though the amount of sand used in shell molding installations may be small, the cost of the sand makes it economically feasible to recover. The thermal method would obviously be the logical method of removing a cured-plastic binder which is more impervious to water than to heat. The reclamation system is reduced to merely the single operation of burning the crushed sand. Small furnaces are available to meet the requirements of a shell-molding installation.

To summarize, the combination system of wet-scrubbing and thermal treatment is desirable wherever the possibility exists of clay being present in the waste sand. This would be true of most iron and steel foundries. The thermal method alone has been successfully used at foundries employing dry sand cores and organic binders exclusively.

Advantages of Combination System

Of the many values attached to the reclamation of sand probably the most important one to the metallurgist and the superintendent is the excellent control over sand properties that he can exercise with the reclamation system. Control is important in these days when casting finish and dimensional accuracy are emphasized. Normally new sand is purchased for use in the core room; and when we talk of reclaimed sand properties, we should consider those properties in relation to core room usage.

When new sand specifications are used, these specifications generally cover such characteristics as grain size, organics and clay. A reclaimed sand should have similar characteristics if it is to replace the new sand in the core room. The combination system of which we speak here can provide a sand meeting new sand specifications with certain qualifications as to grain size. This latter characteristic will depend much upon

the number of different sands used in the foundry, consequently the degree of blending that takes place at the waste sand discharge point.

With reference to control over gas-forming properties of sand it is quite obvious that organics are reduced to zero in the burning process. The sand is subjected to temperatures in the range of 1200 F to 1400 F and an oxidizing atmosphere. With a gas-to-solid contact, there is a complete oxidation of carbon. There can be no accretions in the niches of the angular sand grains. The result is the complete removal of organic material on each cycle through the casting process and the reclamation system.

Wet-scrubbing often leaves residual carbon up to 2.0 per cent. The organics remaining in the sand, will of course depend on the nature of the sand, i.e. whether it is round and smooth (Ottawa silica) or angular (lake sand). When one adds core oil and cereal flour to bond the sand, one is adding a determinate amount of organics to an indeterminate amount already coating the grain. In the system employing burning, this organic does not present an indeterminate quality. It is merely removed. To some foundries the matter of residual organic is not one of concern; to others, it is of considerable importance.

There is a second criterion to judge the quality of reclaimed sand, and that is tensile strength. There are some who consider this differentiation of greater importance.

The burning procedure affects the tensile strength in varying degrees. The extent of improvement, if any, due to burning was determined quantitatively in the laboratory. The sands were representative of both steel and iron foundries. Since this phase of the investigation was concerned with the effect of burning on tensile strength, only those sands that were originally wet-scrubbed were investigated.

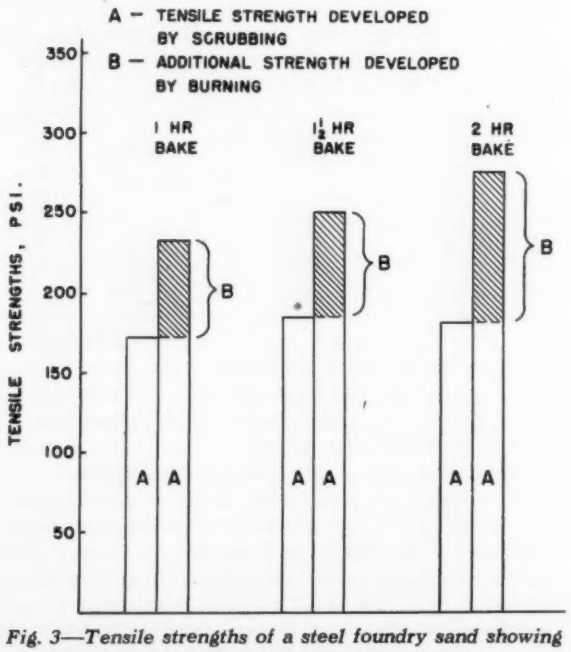


Fig. 3—Tensile strengths of a steel foundry sand showing effect of burning in the reclamation procedure.

The sand, as received, containing variable percentages of carbon up to 2.0 per cent, was burned in a single hearth furnace. Upon heating for 1 hr, the sand was discharged, cooled and screened through a 12-mesh sieve. Wet-scrubbed sands and burned sands were bonded with oil and cereal flour. The tensile briquets were baked at 425 F in a core oven. The tensiles shown in Fig. 3 are averages of five cores, and were confirmed in several tests extending over as many days. The portion of the column identified as "A" in the tensile strength developed by wet-scrubbing and the part marked "B" is the additional strength developed by burning.

Improvement in other sands varied but it was usually the case that this improvement in tensiles was possible.

It is theorized that the removal of the partially oxidized film which imparts the black color permits a better bond between the new oil film and the clean grain itself. It may be possible also that the speed and degree of wetting of the sand is improved when the organic deposit is removed.

The improvement of tensile properties with the burning operation would apply to oil-cereal bonded cores. The improvement may not be so marked when, as is common in steel foundry practice, clay is added to core sand mixtures to develop the necessary green bond. Normally high tensile strengths are then sacrificed for greater green strength. But in either case the tensile strength developed per quart of oil is greater when a completely burned sand is employed.

The control of permeability is another plus value for the foundry using a reclaimed sand. It is commonly known that it is not always possible to get sand at the required time with the required fineness characteristics. With a reclaimed sand in a closed sand system, it is possible to maintain excellent control over fineness, and thus permeability of the sand mixes.

In the author's experience with a sand reclamation system it was found over a period of several years, the permeability of the core sand mixes varied only by a few points. The system was made up of a blend of silica and lake sands in approximately equal proportions. New sand, in the form of the coarser silica or the finer lake sand, was added periodically to replace the sand lost in the casting process. In this specific case the loss was only about 5 per cent, yet with these small additions constant permeabilities could be maintained.

More recently a steel foundry has reported control of fineness which is most unusual. Figure 4 shows the close relationship between the finenesses of new sand of two grades and two screened products of a combination reclamation system.

In this particular system a burning operation follows the wet-scrubbing and classification. The reclaimed sand is passed through an air classifier of the cascade type. The finer fraction is collected on one conveyor belt and the coarse fraction on another belt. Fineness of these two fractions are nearly identical with the two grades of new sand purchased.

In the foundry where such separation is not warranted the blend could be classified to meet the requirements, and new sand of coarser or finer grade

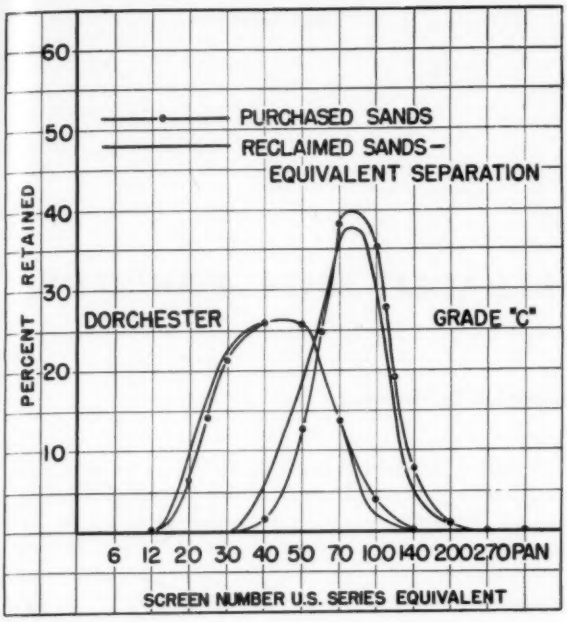


Fig. 4—Grain distribution in new and reclaimed sands.

added to the system to make up losses. The degree of classification will effect the percentage of recovery since the more sand discarded requires more new sand make up. The foundry whose practice is mentioned above is recovering 80 per cent of the sand sent to reclaiming.

Cost of Reclaimed Sand

To the foundry management, the cost per ton of reclaimed sand offers the most important incentive for reclaiming.

The highest proportion of cost in reclaiming is in labor and fuel. Since the labor required is more or less a fixed quantity, the larger the system, the lower this particular cost. Two very large thermal systems reclaimed sand for a cost of approximately 50 cents per ton. In one case the cost of new sand was over $5.00 per ton and in the other the cost was only a little above $1.00 per ton. Needless to say, they both could be amortized over a short period. In both cases two operators were responsible for the system which actually required very little attention. As the system becomes more complex, the labor cost increases slightly. The cost of sand in a steel foundry reclaimer of a capacity of over 4000 tons per month is reported to be approximately $1.39 per ton.

As for fuel costs this is variable according to the fuel used and the distance from good fuel sources. The fuel cost of a wet-scrubbing system in one section is equivalent to the fuel cost of a burning system in another location. In other words one plant can burn sand as cheap as another can dry sand. Fuel consumption is influenced, of course, by the thermal efficiency of the dryer or furnace. The multiple hearth furnace may be direct-fired with small burners situated in the walls of the furnace. Heat may be applied in zones where it can best be utilized, thus higher efficiencies are possible.

Although the theoretical heat requirements are greater for the combination process than they are for a drying system, the selection of equipment and operating procedures can and do equalize the costs. One steel foundry is burning 8.8 gallons of oil per ton of sand while another foundry is using slightly more for its drying operations.

Regardless of the specific costs it is quite evident that costs of reclaiming are well below those of new sand in many cases.

Space Requirements

One other question often asked concerns the space requirements for a reclamation system. Because of the nature of the equipment used in the combination system, it requires little floor space and takes advantage of vertical space. One system has been installed in a 15-ft wide bay of a building and extends about 24 ft along this bay.

By vertical alignment of equipment it is possible to use gravity flow and reduce equipment and power costs. Generally two pumps can take care of all fluid flow. Gravity takes care of the rest of the material flow through the system.

Conclusion

The purpose of this paper has been to outline the various steps in the combination system of reclaiming sand, and to focus upon the quality of the product with the final step of burning at high temperatures. From the standpoint of a quality sand, as measured by tensile strength, fineness and residual substances, it has been indicated what part the burning phase has taken in the cleaning of the sand. From the standpoint of operating costs, it has been indicated by actual commercial installations that the cost of thermally reclaimed sand is much less than new sand and sometimes less than the drying of sand. From the standpoint of real estate, the compactness of a vertical system offers a definite advantage.

Acknowledgments

The author wishes to thank the several foundrymen who were kind enough to submit sand for testing operations, and particularly to Mr. Clyde Jenni, of General Steel Castings Co., for use of some data reported by him in the literature.

GRAPHITIZATION* IN THE MALLEABLE IRON INDUSTRY

By

H. A. Schwartz, W. K. Bock and J. D. Hedberg

ABSTRACT

This paper does not seek causes and effects but only the quantitative relations expressing graphitizing rate in terms of the observable chemical and operating variables in cupola-air furnace duplexed malleable.

Graphitization is studied during freezing (mottling), in "first stage" gamma iron or supercritical heat treating, in the region where the gamma iron solid solutions decompose into alpha iron and graphite, here called "intermediate stage" annealing; and briefly in "second stage," i.e., subcritical temperature annealing.

Silicon is vastly the most important element. Its potency varies with changes in melting conditions which also affect nodule number, cupola carbon and cupola silicon. The effect of these variables in the potency of silicon is further modified by heating rate in annealing. Slow heating tends to make the potency in all materials more nearly alike. Changes in potency are closely paralleled by changes in nodule number.

Although observed relations show no statistical evidence of error greater than the unavoidable observational error, they were not sufficient to give more than qualitative indications of the effect of other elements within the range of compositions encountered in the practice being studied.

Introduction

The importance of graphitization to the malleable foundryman is obvious. A considerable amount of literature on the subject has accumulated. In the 1949 Williams' Lecture for the I.B.F. one of the authors[1] cited 121 titles covering the more pertinent publications. Other papers have been published since.

There are, in general, two avenues of approach to such a problem. Especially if one is interested in the elucidation of principles it is usual to choose the method of the controlled experiment, holding all conditions constant except the one variable whose effects are being studied. The method is admirable for such a purpose, only relatively limited experimentation is required and the resulting data are usually capable of unequivocal interpretation. Most of the publications from this and other laboratories are based on this approach.

The other method is statistical. It does not pretend to control the experimental procedure but takes the results of an operation as it finds them and seeks to establish correlations between as many recognizable operating variables as possible and the resulting characteristics. The advantages of this method other than that it furnishes a rather inclusive picture, are that it does not overlook the fact, as the controlled experiment usually does, that the effect of a given variation in one property on another may not be unique but may depend upon many other environmental conditions. Its disadvantage is the labor and expense incident to a study of numerous samples.

The present somewhat complete study of some 200 or more specimens has required about 12000 hr of laboratory time. This was about ⅔ professional and ⅓ service; i.e., sample preparation, computation, supervision and the like. About three tons of specimens were poured.

It is always well to examine the quantitative conclusions of an experiment by comparison with the observational errors involved. This has been done in the present paper in much greater detail than in any previous investigation in this field of which we know.

The purpose of this rather exhaustive work was to investigate the relation of all recognizable variables to the rate at which graphite would separate during the freezing of the white cast iron or during the several stages of the annealing process. It excludes variations in the annealing conditions, concerning itself only with the ability of the metal to precipitate graphite.

Basic Assumptions

Certain facts are regarded as acceptable without further proof for the purpose of this study.

The eutectic arrest is significant in determining graphitization during freezing.[2]

The expression Carbon + 2.85 log Silicon correctly expresses the relative effects of these two elements on mottling.[3]

Logarithm of the graphite formed in a given time in first stage graphitization increases linearly with silicon concentration.[4] This is probably true of other elements and for other stages.

For properly chosen coordinates the log per cent completion of graphitization-time curve is the same for first stage graphitization for all white irons.[5,6]

Our own unpublished observations lead us to

* Research Dept., National Malleable and Steel Castings Co., Cleveland.

Table 1.—(Plant A) Chemical Composition of Hard Iron

Mark	C, %	Si, %	Mn, %	S, %	Boron (0.001%)	Cr, %
L-3	2.53	1.06	0.50	0.125	4	0.043
L-4	2.64	1.05	0.46	0.120	3	0.033
L-6	2.59	1.05	0.41	0.131	3	0.034
L-7	2.62	1.10	0.39	0.123	4	0.035
L-8	2.60	1.14	0.46	0.125	4	0.030
L-9	2.41	1.14	0.50	0.130	4	0.039
L-10	2.59	1.14	0.41	0.129	4	0.039
L-11	2.55	1.09	0.40	0.123	3	0.048
L-13	2.51	1.05	0.42	0.119	3	0.042
L-14	2.36	1.04	0.42	0.119	3	0.045
L-16	2.48	1.06	0.36	0.129	3	0.044
L-17	2.47	1.07	0.41	0.117	2	0.046
L-18	2.59	1.04	0.39	0.121	4	0.051
L-19	2.56	1.17	0.42	0.121	2	0.043
L-21	2.58	1.06	0.46	0.118	3	0.040
L-23	2.49	1.07	0.51	0.121	6	0.040
L-25	2.75	1.09	0.48	0.121	3	0.045
L-26	2.66	1.07	0.49	0.122	3	0.034
L-27	2.72	1.00	0.47	0.119	6	0.040
L-28	2.50	1.00	0.40	0.109	3	0.035
L-30	2.61	1.08	0.45	0.117	3	0.049
L-32	2.62	1.08	0.47	0.113	3	0.045
L-35	2.59	1.01	0.50	0.115	3	0.052
L-38	2.65	1.06	0.46	0.115	7	0.032
L-39	2.61	1.03	0.48	0.103	4	0.041
L-42	2.54	1.04	0.44	0.110	4	0.039
L-46	2.48	1.08	0.43	0.115	4	0.046
L-47	2.37	1.02	0.41	0.111	2	0.035
L-49	2.38	1.08	0.45	0.114	3	0.030
L-24	2.58	1.08	0.48	0.131	3	0.040
L-29	2.49	1.06	0.48	0.110	4	0.070
L-31	2.64	1.03	0.44	0.114	3	0.041
L-1	2.53	0.95	0.33	0.124	2	0.037
L-2	2.52	0.87	0.33	0.125	2.5	0.028
L-5	2.45	1.00	0.38	0.117	3	0.028
L-12	2.59	1.24	0.45			
L-15	2.55	1.17	0.47	0.116	2	0.048
L-20	2.52	1.18	0.49	0.115	2	0.036
L-22	2.53	0.99	0.43	0.110	3	0.040
L-33	2.45	0.84	0.36	0.116	3	0.038
L-34	2.63	1.08	0.49	0.115	7	0.045
L-36	2.45	0.92	0.42	0.117	7	0.032
L-40	2.59	1.17	0.50	0.110		
L-41	2.53	0.95	0.44	0.110	6	0.045
L-43	2.52	0.95	0.41	0.109	3	0.038
L-44	2.44	0.99	0.40	0.114	2	0.041
L-45	2.58	1.17	0.47	0.114	3	0.039
L-48	2.46	0.95	0.37	0.109	2	0.034
L-49	2.36	1.07	0.45	0.114	3	0.030
L-50	2.42	1.06	0.44	0.115		0.032
L-51	2.32	0.83	0.35	0.113	1	0.032
L-52	2.48	0.95	0.43	0.116	2	0.035

accept as a usable first approximation that in intermediate and second stage graphitization the log per cent combined carbon decreases linearly with time.

The log per cent completion of first stage graphitization should be proportional to the $\frac{2}{3}$ power of the number of nodules per unit volume[7] or directly proportional to the number per unit area.[8,9]

Graphitization is enormously rapid between A_1 stable and A_1 metastable.[10] The metallographic changes in this region have been described.[11]

The metallographic consequences of the cooling rate through this region are of enormous effect on subcritical graphitization.[12]

The laws of propagation of error are developed in many texts[19] and those governing the significance of

Table 2—(Plant B) Chemical Composition of Hard Iron

Mark	C, %	Si, %	Mn, %	S, %	Boron (0.001%)	Cr, %
C-3	2.43	1.22	0.50	0.154	2.8	0.030
C-4	2.52	1.22	0.47	0.155	2.7	0.038
C-6	2.47	1.29	0.53	0.156	1.2	0.040
C-8	2.51	1.17	0.50	0.157	1.1	0.038
C-9	2.47	1.26	0.55	0.151	3.9	0.035
C-10	2.45	1.17	0.45	0.158	2.8	0.030
C-14	2.45	1.24	0.44	0.168	4.1	0.030
C-15	2.42	1.22	0.49	0.169	5.9	0.028
C-18	2.52	1.12	0.43	0.171	2.8	0.037
C-22	2.49	1.12	0.43	0.169	4.0	0.026
C-24	2.43	1.20	0.46	0.168	4.0	0.040
C-25	2.46	1.27	0.45	0.175	2.6	0.035
C-26	2.52	1.25	0.44	0.176	3.8	0.033
C-27	2.44	1.26	0.50	0.172	5.7	0.039
C-28	2.51	1.13	0.47	0.166	2.0	0.030
C-33	2.43	1.28	0.50	0.174	4.8	0.035
C-34	2.49	1.09	0.44	0.163	5.3	0.038
C-35	2.48	1.23	0.48	0.167	4.3	0.028
C-37	2.40	1.22	0.49	0.170	1.8	0.028
C-38	2.45	1.20	0.48	0.169	4.3	0.030
C-40	2.51	1.20	0.42	0.172	1.8	0.050
C-42	2.51	1.17	0.50	0.178	2.2	0.030
C-44	2.49	1.18	0.45	0.174	5.9	0.028
C-45	2.43	1.35	0.45	0.177	2.9	0.030
C-47	2.39	1.21	0.48	0.173	2.6	0.033
C-48	2.49	1.18	0.48	0.169	3.0	0.036
C-49	2.45	1.24	0.48	0.168	2.5	0.028
C-50	2.54	1.16	0.45	0.162	2.2	0.025
C-52	2.46	1.20	0.42	0.164	1.8	0.038
C-56	2.45	1.09	0.45	0.165	3.0	0.025
C-58	2.65	1.09	0.48	0.162	1.7	0.037
C-60	2.59	1.18	0.44	0.161	2.0	0.027
C-61	2.50	1.23	0.52	0.169	1.7	0.040
C-63	2.43	1.04	0.48	0.154	1.7	0.035
C-65	2.48	1.27	0.50	0.168	2.5	0.025
C-70	2.49	1.06	0.41	0.157	1.6	0.028
C-73	2.47	1.26	0.49	0.156	2.1	0.028
C-74	2.49	1.25	0.47	0.158	1.7	0.035
C-75	2.50	1.24	0.47	0.155	1.7	0.035
C-77	2.45	1.18	0.45	0.156	1.6	0.028
C-78	2.47	1.09	0.42	0.158	1.7	0.028
C-1	2.37	1.24	0.52	0.150		
C-2	2.45	1.12	0.47	0.152		
C-5	2.42	1.29	0.52	0.160		
C-11	2.47	1.20	0.50	0.164		
C-12	2.49	1.30	0.52	0.157		
C-13	2.50	1.40	0.57	0.160		
C-16	2.45	1.26	0.49	0.171		
C-17	2.37	1.23	0.52	0.164		
C-19	2.46	1.27	0.46	0.162		
C-20	2.50	1.23	0.42	0.161		
C-21	2.46	1.36	0.51	0.173		
C-23	2.38	1.22	0.50	0.171		
C-29	2.52	1.44	0.50	0.170		
C-30	2.53	1.18	0.45	0.173		
C-31	2.44	1.29	0.48	0.173		
C-32	2.59	1.22	0.49	0.176		
C-36	2.52	1.17	0.42	0.169		
C-39	2.35	1.09	0.41	0.164		
C-41	2.50	1.24	0.48	0.175		
C-43	2.47	1.28	0.50	0.180		
C-46	2.55	1.23	0.47	0.174		
C-51	2.40	1.20	0.44	0.168		
C-53	2.44	1.11	0.48	0.163		
C-54	2.48	1.15	0.46	0.164		
C-55	2.53	1.30	0.51	0.173		
C-57	2.55	1.17	0.48	0.170		
C-59	2.45	1.28	0.50	0.168		
C-62	2.45	1.01	0.40	0.166		
C-64	2.45	1.05	0.43	0.162		
C-66	2.52	1.12	0.47	0.165		
C-67	2.48	1.23	0.50	0.163		
C-69	2.46	1.20	0.48	0.162		
C-71	2.61	1.29	0.51	0.159		
C-72	2.50	1.09	0.46	0.159		
C-76	2.48	1.20	0.46	0.155		
C-80	2.43	1.25	0.50	0.155		

TABLE 3— (PLANT C) CHEMICAL COMPOSITION OF HARD IRON

Mark	C, %	Si, %	Mn, %	S, %	Boron (0.001%)	Cr, %
3-20-2	2.51	1.22	0.47	0.167	1.5	0.02
3-20-4	2.43	1.21	0.46	0.166	1.5	0.03
3-21-2	2.61	1.19	0.47	0.174	1.5	0.03
3-21-4	2.69	1.16	0.46	0.163	1.4	0.03
3-22-2	2.52	1.30	0.50	0.164	1.4	0.03
3-22-4	2.51	1.17	0.47	0.160	1.5	0.03
3-23-2	2.52	1.33	0.48	0.164	1.4	0.03
3-23-4	2.55	1.11	0.44	0.174	1.4	0.03
3-24-2	2.48	1.40	0.52	0.176	1.5	0.04
3-24-4	2.55	1.19	0.50	0.169	1.7	0.04
3-26-2	2.54	1.24	0.50	0.169	1.8	0.03
3-26-4	2.50	1.28	0.49	0.162	1.6	0.03
3-27-2	2.37	1.26	0.49	0.160	1.5	0.03
3-27-4	2.48	1.31	0.49	0.161	1.6	0.03
3-28-2	2.41	1.28	0.49	0.163	1.7	0.03
3-28-4	2.46	1.22	0.48	0.173	1.6	0.03
3-29-2	2.35	1.38	0.50	0.172	1.6	0.03
3-29-4	2.56	1.17	0.46	0.162	1.6	0.03
3-30-2	2.42	1.27	0.53	0.167	1.7	0.02
3-30-4	2.54	1.26	0.50	0.161	1.8	0.03
3-31-2	2.48	1.42	0.48	0.168	2.2	0.03
3-31-4	2.46	1.29	0.49	0.165	2.4	0.03
4-2-2	2.41	1.38	0.49	0.176	2.4	0.04
4-2-4	2.52	1.05	0.41	0.169	2.2	0.03
4-5-2	2.53	1.25	0.48	0.174	2.1	0.04
4-5-4	2.58	1.17	0.51	0.175	2.0	0.04
4-6-2	2.72	1.20	0.52	0.176	2.1	0.02
4-6-4	2.54	1.26	0.51	0.166	2.0	0.02
4-7-2	2.46	1.27	0.51	0.166	2.2	0.03
4-7-4	2.46	1.29	0.54	0.166	2.4	0.03
4-9-2	2.47	1.23	0.53	0.172	2.3	0.03
4-9-4	2.32	1.23	0.51	0.164	2.2	0.03
4-10-2	2.57	1.31	0.58	0.172	3.6	0.03
4-10-4	2.72	1.10	0.46	0.160	3.6	0.03
4-11-2	2.59	1.18	0.46	0.172	3.8	0.02
4-11-4	2.74	1.09	0.44	0.167	3.5	0.02
4-12-2	2.60	1.14	0.56	0.175	3.6	0.02
4-12-4	2.62	1.17	0.58	0.163	3.6	0.02
4-13-2	2.59	1.22	0.51	0.177	2.1	0.02
4-13-4	2.50	1.03	0.43	0.165	2.1	0.02
4-16-2	2.64	1.15	0.47	0.180	2.5	0.03
4-16-4	2.46	1.11	0.46	0.185	2.3	0.03
4-17-4	2.60	1.20	0.56	0.176	2.4	0.03
4-19-2	2.57	1.27	0.51	0.183	2.7	0.03
4-19-4	2.55	1.07	0.49	0.175	2.3	0.04
4-20-2	2.40	1.21	0.49	0.170	3.6	0.03
4-20-4	2.42	1.12	0.46	0.175	3.4	0.03
4-23-2	2.50	1.21	0.47	0.185	2.4	0.03
4-23-4	2.49	1.21	0.47	0.175	2.2	0.04
4-24-2	2.47	1.13	0.52	0.177	2.1	0.03
4-24-4	2.47	1.22	0.58	0.165	2.2	0.03
4-25-2	2.58	0.99	0.43	0.161	2.2	0.03
4-25-4	2.45	1.25	0.51	0.169	2.1	0.03
4-26-2	2.54	1.19	0.50	0.165	2.0	0.02
4-26-4	2.56	1.08	0.54	0.164	2.1	0.02
4-27-2	2.54	1.27	0.55	0.157		
4-27-4	2.47	1.18	0.58	0.160		

regression co-efficients have also been adequately treated.[20] Both are too involved for restatement here.

Material

Our investigation is confined almost exclusively to iron made by cupola-air furnace duplexing. It was derived from three different plants here designated as A, B and C. It later proved desirable, when studying solid state graphitization, to obtain another lot from plant B, designated as B′.

The composition of the several lots of metal is given in Tables 1 to 4.

TABLE 4— (PLANT B′) CHEMICAL COMPOSITION OF HARD IRON

Mark	C, %	Si, %	Mn, %	S, %	Boron (0.001%)	Cr, %
39-X	2.34	1.13	0.45	0.177		
40-X	2.46	1.21	0.46	0.180		
41-X	2.38	1.26	0.50	0.181		
42-X	2.41	1.27	0.49	0.191		
43-X	2.42	1.18	0.51	0.180		
44-X	2.36	1.15	0.48	0.168		
45-X	2.42	1.22	0.50	0.171		
46-X	2.49	1.20	0.50	0.176		
47-X	2.41	1.13	0.49	0.169		
48-X	2.44	1.21	0.50	0.187		
49-X	2.53	1.18	0.45	0.184		
50-X	2.44	1.22	0.46	0.185		
51-X	2.42	1.14	0.42	0.175		
52-X	2.44	1.24	0.39	0.181		
53-X	2.43	1.22	0.41	0.180		
54-X	2.43	1.24	0.45	0.171		
55-X	2.40	1.09	0.41	0.166		
56-X	2.48	1.26	0.44	0.180		
57-X	2.44	1.24	0.45	0.188		
58-X	2.42	1.29	0.46	0.169		
59-X	2.41	1.21	0.45	0.174		
60-X	2.44	1.14	0.47	0.178		
61-X	2.37	1.19	0.41	0.171		
62-X	2.31	1.23	0.41	0.185		
63-X	2.52	1.21	0.44	0.176		

Plants A and C were operating with Northern coke; plant B with Southern coke. Air furnaces were coal fired.

In Table 5 are other details of the melting operation. These are taken from operating records and may not be literally applicable to the specimens studied. We know only, for example, the average rate of thruput for a given day and not that at the moment of pouring. This does not seem too important. Apparently what happens is the effect of a sort of integration of what has gone before during a considerable period. A few attempts were made to correlate observations with existing conditions as the iron left the furnace but these were all unsuccessful as compared with correlation with average conditions and were therefore abandoned.

Specimens were cast of five sizes:—½ in. × 3 in. × 4 in., ¾ in. × 3 in. × 4 in., 1 in. × 3 in. × 6 in., 1½ in. × 3 in. × 6 in. and 2 in. × 3 in. × 6 in. In the laboratory and at Plants B and C a number of specimens of each size were cast and cooling curves at the center taken with a platinum-rhodium pyrometer and a portable potentiometer. The laboratory tests indicated that the length of the isothermal arrest was nearly independent of pouring temperature for the ½ in. thick section but decreased as the pouring temperature increased for thicker sections. Roughly the rate of decrease is more pronounced the thicker the section.

The operating temperatures at the two plants were not so very different for the various experiments and the time of "eutectic hold" was pretty constant for each section. For purposes of description and not for purposes of heat transfer calculations, the time of this isothermal hold is listed as follows:

Section Size, in.	Hold, min
2 x 3	2.5
1½ x 3	2.3
1 x 3	1.7
¾ x 3	0.8
½ x 3	0.5

It is possible, if the relation is not linear, that the first and fifth figures are a little too low and the others a little too high.

The various experimental heat treatments will be described in their proper places. Here we wish only to describe the as-cast metal. Further description has been published elsewhere.[13]

Experimental Procedure

The observations made on these several irons were as follows:—For the study of primary graphitization, or mottling, the blocks were broken at mid-length and rated as to whether they were mottled or clear. If even a single mottle could be found visually, on critical examination in good light, the fracture was rated as mottled. Obviously many specimens were much more strongly mottled, in various degrees up to almost completely gray fractures in the thickest section. It was, however, found impossible to base any quantitative discussion on severity of mottling and only the criterion of complete absence became amenable to this treatment. The melting conditions at the several plants are reported in Table 5.

Table 5—Melting Characteristics

	Plant A	Plant B	Plant C	Plant B′
Coefficient	3.2545	5.2361	1.9237	4.5566
Cupola Diam, in.	60	82	72	85
Hearth, Ft²	307	280	282	280
Cupola, lb/hr	38971	56928	30288	55940
Air Furnace, lb/hr	44549	63522	32378	62107
Cupola, lb/hr/sq ft	1985	1552	1071	1420
Air Furnace, lb/hr/sq ft	145	227	115	222
Tons/sq ft	0.06	0.11	0.11	0.13
Iron/Coke	10.7	9.6	7.4	10.3
Iron/Coal	22.4	25.5	16.6	25.4
% Pig	11.5	10.2	11.0	10.1
% Steel	35.7	26.4	28.6	22.2
Hours in Air Furnace	0.88	0.95	1.92	1.04
H_2O, in. Hg	0.44	0.53	0.23	0.23
Cupola Carbon	2.77	2.64	2.82	2.69
% Si Loss in Cupola	0.27	0.43	0.06	0.32
% Si Loss in Air Furnace	0.01	0.00	0.04	0.01
% C Loss in Air Furnace	0.27	0.15	Not available.	0.15

The original plan was to include the effects of oxidation in this study. To that end a considerable number of typical specimens were examined for oxides by residue analysis after vaporizing the iron as ferric chloride. Also liquid metal was treated with excess aluminum and its oxygen calculated from the recovered Al_2O_3. As a check, a few of these specimens were analyzed by vacuum-fusion methods at Battelle Memorial Institute.

The several methods do not agree among themselves nor with the history of the sample and correlation with primary or secondary graphitization was hopeless. Our best conclusion, after discussion with C. E. Sims of Battelle is that the oxygen content of all these (and many other) cast irons is of the order of 0.003 pct, which corresponds approximately to the oxygen content of the SiO_2 of the sonims when these are not contaminated by extraneous siliceous matter. Battelle also found about 0.0001 pct hydrogen and about 0.01 pct nitrogen.

The observational data will be found in the section on Primary Graphitization.

For the study of "first stage" or super-critical graphitization, specimens were cut from unmottled ½ in. thick plates. A number of these were packed, in white iron crushings, in a 3 in. × 7 in. pipe nipple capped at both ends. All specimens in a given pipe were at the same distance from the pipe surface and occupied no more than the middle 4 in. of its length.

The nipple was introduced into a "hump" furnace at 900 C (1650 F). About ½ hr was required to heat

Table 6—(Plant A), % Completion of Graphitization in 2½ Hr at 900 C

Mark	Per cent Completion	Mark	Per cent Completion	Mark	Per cent Completion
L-2	2	L-21	45	L-38	35
L-3	30	L-23	29	L-39	31
L-4	16	L-24	24	L-40	61
L-6	10	L-25	55	L-41	20
L-7	10	L-26	47	L-42	38
L-8	16	L-27	40	L-43	11
L-9	35	L-28	17	L-44	16
L-10	12	L-29	21	L-45	47
L-11	19	L-30	34	L-46	36
L-13	7	L-31	18	L-47	31
L-14	6	L-32	50	L-48	8
L-15	44	L-33	1.3	L-49	27
L-16	8	L-34	74	L-50	24
L-17	24	L-35	27	L-51	3.5
L-18	4	L-36	16	L-52	10
L-19	51				

Table 7—(Plant B), % Completion of Graphitization in 2½ Hr at 900 C

Mark	Per cent Completion	Mark	Per cent Completion	Mark	Per cent Completion
C-1	44	C-28	11	C-54	7
C-2	8	C-29	81	C-55	35
C-3	56	C-30	12	C-56	1
C-4	39	C-31	31	C-57	9
C-6	30	C-32	20	C-58	7
C-8	18	C-33	31	C-59	22
C-9	44	C-34	2	C-60	4
C-10	27	C-35	26	C-61	39
C-11	25	C-36	10	C-62	1
C-12	42	C-37	6	C-63	1
C-13	62	C-38	5	C-64	3
C-14	22	C-39	1	C-65	10
C-15	23	C-40	3	C-66	8
C-16	47	C-41	17	C-67	22
C-17	42	C-42	30	C-69	18
C-18	9	C-43	34	C-70	2
C-19	28	C-44	7	C-71	71
C-20	6	C-45	55	C-72	3
C-21	62	C-46	18	C-73	43
C-22	5	C-47	18	C-74	24
C-23	10	C-48	10	C-75	25
C-24	7	C-49	23	C-76	7
C-25	11	C-50	6	C-77	5
C-26	13	C-51	6	C-78	1
C-27	26	C-52	2	C-79	6
		C-80	17	C-53	6

TABLE 8— (PLANT C), COMPLETION OF GRAPHITIZATION IN 2½ HR AT 900 C

Mark	% Completion	Adjusted % Completion	Mark	% Completion	Adjusted % Completion
3-20-2	59	66	4-7-4	66	76
3-20-4	59	66	4-9-2	70	84
3-21-2	62	69	4-9-4	51	51
3-21-4	65	74	4-10-2	60	66
3-22-2	74	90	4-10-4	17	17
3-22-4	56	59	4-11-2	35	35
3-23-2	59	66	4-11-4	16	16
3-23-4	43	43	4-12-2	43	43
3-24-2	79	99	4-12-4	51	51
3-24-4	66	76	4-13-2	63	71
3-26-2	67	78	4-13-4	23	23
3-26-4	81	102	4-16-2	34	34
3-27-2	71	85	4-16-4	36	36
3-27-4	72	87	4-17-4	46	46
3-28-2	67	78	4-19-2	53	53
3-28-4	42	42	4-19-4	38	38
3-29-2	87	110	4-20-2	32	32
3-29-4	65	74	4-20-4	24	24
3-30-2	83	105	4-23-2	32	32
3-30-4	54	54	4-23-4	57	61
3-31-2	79	99	4-24-2	27	21
3-31-4	62	69	4-24-4	71	85
4-2-2	66	76	4-25-2	12	12
4-2-4	21	21	4-25-4	68	80
4-5-2	21	21	4-26-2	51	51
4-5-4	48	48	4-26-4	49	49
4-6-2	55	55	4-27-2	62	70
4-6-4	57	61	4-27-4	54	56
4-7-2	72	87			

TABLE 9— (PLANT B'), % COMPLETION OF GRAPHITIZATION IN 2½ HR AT 900 C

Mark	% Completion	Mark	% Completion
39-X	3	52-X	7
40-X	2	53-X	3
41-X	23	54-X	9
42-X	19	55-X	1
43-X	7	56-X	5
44-X	6	57-X	5
45-X	15	58-X	24
46-X	10	59-X	8
47-X	5	60-X	3
48-X	16	61-X	2
49-X	3	62-X	3
50-X	8	63-X	5
51-X	4		

the furnace to its original temperature, which was then maintained for 2½ hr (also 1¼ hr in the case of plant C) and air cooled. From a comparison of the results in 1¼ hr and 2½ hr and the 3/2 power of time rule it was deduced that the small amount of graphitization presumably occurring during heating and cooling was equivalent to an additional 0.19 hr at 900 C.

The pertinent observations are summarized in Tables 6 to 9 in terms of the per cent completion of the reaction. The amount of graphite which could be formed at 900 C is obviously the total carbon less the solubility of graphite at that temperature, about 0.95 pct for the average silicons encountered.

Graphite was determined, in all cases, on a piece of metal representing the entire thickness except the layer 1/16 in. thick at each surface. This was rejected as being of lower carbon due to selective freezing.[14]

TABLE 10— (PLANT A), % COMPLETION OF GRAPHITIZATION IN 2½ HR AT 900 C AFTER PRETEMPER

Mark	Per cent Completion	Mark	Per cent Completion
L-3	4.4	L-32	52
L-4	29	L-33	1.3
L-6	10	L-34	63
L-9	43	L-35	20
L-10	27	L-38	55
L-11	22	L-39	28
L-14	7	L-40	74
L-15	51	L-41	39
L-17	27	L-42	45
L-18	4.9	L-43	11
L-20	63	L-44	16
L-21	46	L-45	39
L-22	20	L-46	44
L-23	32	L-47	33
L-25	58	L-48	21
L-26	41	L-49	27
L-27	37	L-50	28
L-28	16	L-51	1.4
L-29	24	L-52	11
L-30	23	L-24	21
L-31	22		

TABLE 11— (PLANT B), % COMPLETION OF GRAPHITIZATION IN 2½ HR AT 900 C AFTER PRETEMPERING

Mark	Per cent Completion	Mark	Per cent Completion
C-1	74	C-40	20
C-2	40	C-41	27
C-3	78	C-43	66
C-5	69	C-46	48
C-10	54	C-51	35
C-11	56	C-53	34
C-12	51	C-54	14
C-16	73	C-55	63
C-17	65	C-57	46
C-19	62	C-59	65
C-20	43	C-62	9
C-21	77	C-64	9
C-23	45	C-66	25
C-29	86	C-67	56
C-30	59	C-69	46
C-31	72	C-72	18
C-32	52	C-76	16
C-36	30	C-79	25
C-37	37	C-80	51
C-39	14		

Since it is known[15] that pretempering alters nodule number and graphitizing rate, certain specimens from Plants A and B were heated, similarly packed, for 20 hr at 400 C (750 F) and then graphitized at 900 C as previously described. The per cent completion is tabulated in Tables 10 and 11.

Because we shall wish to consider the effect of nodule number on graphitizing rate, and the effect of composition on nodule number, metallographic counts of nodules per square millimeter were made by Reidar Eriksen, using a magnification of 100 diameters and examining five fields per specimen, each covering 1 sq mm of specimen surface. These counts found in Tables 12 to 15 apply specifically to heating to 900 C in ½ hr, followed by a 2½-hr hold.

It was found experimentally that the graphite formed in equal times in a pretempered and the same unpretempered iron, remained in a constant ratio

Table 12— (Plant A), Nodules per mm² Formed on Quick Heating at 900 C and Holding

Mark	Nodules /mm²	Mark	Nodules /mm²
L-3	100	L-25	77
L-4	58	L-26	56
L-6	68	L-27	68
L-7	169	L-28	32
L-8	156	L-30	57
L-9	194	L-32	59
L-10	150	L-35	57
L-11	181	L-38	65
L-13	41	L-39	54
L-14	25	L-42	48
L-16	70	L-46	94
L-17	38	L-47	42
L-18	64	L-49	74
L-19	57	L-24	44
L-21	47	L-29	59
L-23	45	L-31	50

Table 13— (Plant B), Nodules per mm² Formed on Quick Heating to 900 C and Holding

Mark	Nodules/mm²	Mark	Nodules/mm²
C-3	75	C-42	59
C-4	62	C-44	61
C-6	59	C-45	67
C-8	46	C-47	65
C-9	59	C-48	59
C-10	61	C-49	66
C-14	50	C-50	48
C-15	40	C-52	55
C-18	56	C-56	53
C-22	37	C-58	39
C-24	50	C-60	45
C-25	56	C-61	45
C-26	56	C-63	45
C-27	68	C-65	48
C-28	39	C-70	37
C-33	55	C-73	53
C-34	50	C-74	52
C-35	60	C-75	66
C-37	64	C-77	65
C-38	56	C-78	44
C-40	66		

(2:1 as it happened) regardless of the length of these times, so long as the comparisons were made when the 3/2 power of time rule was applicable for the latter. Thus this rule must also apply in the pretempered state.

For intermediate stage graphitization, between the stable and metastable A_1 points, it seemed wise to adopt the technique of cooling through this range at a known rate since this is the condition most commonly encountered in practice.

Specimens were packed as previously described, heated to 900 C as before, and held 70 hr. They were then cooled at 2 C (3.6 F) per hour to 765 C (1410 F). On metallographic evidence the resulting structure represents equilibrium just above A_1 stable. It was found that cooling at 35 C (63 F) per hour produced usable residual combined carbons and this rate was used to bring the specimens to 720 C (1330 F) just under A_1 metastable. The pipe nipple was then air cooled.

Combined carbon was determined by Guiler's modification of Donaldson's* method[18] which, however, still carries a constant blank of 0.10 pct. As before,

Table 14— (Plant C), Nodules per mm² Formed on Quick Heating to 900 C and Holding

Mark	Nodules/mm²	Mark	Nodules/mm²	Mark	Nodules/mm²
3-20-2	68	3-30-4	76	4-13-2	74
3-20-4	101	3-31-2	99	4-13-4	75
3-21-2	75	3-31-4	102	4-16-2	35
3-21-4	96	4-2-2	56	4-16-4	49
3-22-2	122	4-2-4	53	4-17-4	59
3-22-4	88	4-5-2	74	4-19-2	60
3-23-2	98	4-5-4	50	4-19-4	50
3-23-4	64	4-6-2	71	4-20-2	55
3-24-2	114	4-6-4	69	4-20-4	60
3-24-4	84	4-7-2	136	4-23-2	35
3-25-2	81	4-7-4	136	4-23-4	86
3-25-4	95	4-9-2	101	4-24-2	38
3-27-2	103	4-9-4	59	4-24-4	94
3-27-4	106	4-10-2	47	4-25-4	102
3-28-2	142	4-10-4	30	4-26-2	52
3-28-4	52	4-11-2	40	4-26-4	59
3-29-2	114	4-11-4	63	4-27-2	49
3-29-4	101	4-12-2	58	4-27-4	59
3-30-2	108	4-12-4	82		

Table 15— (Plant B′), Nodules per mm² Formed on Quick Heating to 900 C and Holding

Mark	Nodules/mm²	Mark	Nodules/mm²	Mark	Nodules/mm²
39-X	44	47-X	32	55-X	69
40-X	48	48-X	83	56-X	54
41-X	62	49-X	54	57-X	58
42-X	85	50-X	77	58-X	104
43-X	63	51-X	51	59-X	64
44-X	40	52-X	64	60-X	39
45-X	66	53-X	53	61-X	50
46-X	56	54-X	64	62-X	52
				63-X	79

surface metal was excluded from the sample. The resulting data are to be found in Tables 16 to 18.

Our experimentation with second stage (subcritical) graphitization was greatly curtailed. The process must begin with some arbitrarily chosen state, and no state is accurately reproducible in practice. The investigation is offered mainly as being of academic interest and incapable of direct application in practice. A few specimens were brought to equilibrium at 770 C (1420 F) packed in charcoal in a plumbago crucible, and then rapidly air cooled by dumping the crucible on the floor. This produces a practically completely pearlitic matrix. The specimens were then packed as usual, heated 5 hr at 680 C (1260 F) and analyzed for combined carbon with the results shown in Table 19.

In conclusion be it noted that we have deliberately avoided dealing experimentally with the low-carbon surface areas which produce rims in practice. The prevention of pearlitic rims constitutes an entirely separate field for investigation.

In the next section we propose to evaluate the

* When this work was begun in 1950 we regarded this as the preferred method. We still think that it yields reproducible results but that these are of the order of 10 pct low. The results are still comparative but the numerical constants of Equations (14) to (25) inclusive are probably about 0.0414 too low. This does not alter any conclusions as to the effect of alloys, etc. for their coefficients are not affected.

TABLE 16—(PLANT A), % COMBINED CARBON AFTER COOLING 35 C/HR*

Mark	% Combined Carbon	Mark	% Combined Carbon	Mark	% Combined Carbon
L-1	0.41	L-21	0.39	L.39	0.45
L-2	0.38	L-22	0.38	L-40	0.23
L-3	0.25	L-23	0.39	L-41	0.36
L-4	0.36	L-24	0.42	L-42	0.42
L-7	0.19	L-25	0.16	L-43	0.40
L-8	0.37	L-26	0.32	L-44	0.49
L-9	0.32	L-27	0.39	L-45	0.35
L-10	0.26	L-28	0.43	L-46	0.29
L-11	0.40	L-30	0.32	L-47	0.42
L-14	0.42	L-31	0.42	L-48	0.45
L-15	0.42	L-33	0.41	L-49	0.39
L-16	0.31	L-35	0.42	L-50	0.24
L-17	0.42	L-36	0.45	L-51	0.50
L-19	0.24	L-38	0.31	L-52	0.46
L-20	0.27				

* Includes 0.10% for soluble carbon and blank.

TABLE 17—(PLANT B), % COMBINED CARBON AFTER COOLING 35 C/HR*

Mark	% Combined Carbon	Mark	% Combined Carbon	Mark	% Combined Carbon
C-1	0.37	C-29	0.25	C-55	0.25
C-2	0.43	C-30	0.33	C-56	0.40
C-3	0.28	C-31	0.23	C-57	0.39
C-5	0.36	C-32	0.32	C-58	0.37
C-6	0.32	C-33	0.37	C-59	0.32
C-8	0.39	C-34	0.42	C-60	0.41
C-9	0.32	C-35	0.28	C-61	0.36
C-10	0.35	C-36	0.30	C-62	0.39
C-11	0.40	C-37	0.31	C-63	0.31
C-12	0.32	C-38	0.30	C-64	0.41
C-13	0.30	C-39	0.35	C-65	0.27
C-14	0.24	C-40	0.34	C-66	0.36
C-15	0.35	C-41	0.32	C-67	0.33
C-16	0.25	C-42	0.46	C-69	0.36
C-17	0.38	C-43	0.29	C-70	0.40
C-18	0.27	C-44	0.32	C-71	0.19
C-19	0.30	C-45	0.29	C-72	0.41
C-20	0.23	C-46	0.31	C-73	0.29
C-21	0.24	C-47	0.30	C-74	0.32
C-22	0.31	C-48	0.34	C-75	0.26
C-23	0.36	C-49	0.25	C-76	0.39
C-24	0.34	C-50	0.32	C-77	0.32
C-25	0.30	C-51	0.34	C-78	0.41
C-26	0.32	C-52	0.36	C-79	0.43
C-27	0.33	C-53	0.37	C-80	0.48
C-28	0.40	C-54	0.43		

* Includes 0.10% for soluble carbon and blank.

accumulated data by graphic means in the case of primary graphitization and by statistical methods for secondary graphitization separating the latter into "first stage" by the reaction:—

$$Fe_3C = C + Fe\ (\gamma);$$

intermediate stage by the reaction:—

$$C\ (\text{dissolved in } Fe\gamma) = C\ (\text{graphite}) + Fe\ (\alpha)$$

and "second stage" by the reaction:—

$$Fe_3C = C + Fe\ (\alpha)$$

Evaluation of Primary Graphitization Data

Primary graphitization is a process whereby graphite is deposited from liquid cast iron. Its effect manifests itself as "mottling" in the malleable iron industry and it must be avoided.

TABLE 18—(PLANT C), % COMBINED CARBON AFTER COOLING 35 C/HR*

Mark	% Combined Carbon	Mark	% Combined Carbon	Mark	% Combined Carbon
3-20-2	0.34	3-30-2	0.20	4-13-2	0.17
3-20-4	0.17	3-30-4	0.21	4-13-4	0.18
3-21-2	0.35	3-31-2	0.17	4-16-2	0.21
3-21-4	0.24	3-31-4	0.21	4-16-4	0.22
3-22-2	0.17	4-2-2	0.13	4-17-4	0.31
3-22-4	0.40	4-2-4	0.38	4-19-2	0.16
3-23-2	0.13	4-5-2	0.23	4-19-4	0.21
3-23-4	0.22	4-5-4	0.41	4-20-2	0.20
3-24-2	0.19	4-6-2	0.23	4-20-4	0.27
3-24-4	0.36	4-6-4	0.20	4-23-2	0.27
3-26-2	0.15	4-7-2	0.16	4-23-4	0.18
3-26-4	0.13	4-7-4	0.08	4-24-2	0.29
3-27-2	0.25	4-9-2	0.26	4-24-2	0.18
3-27-4	0.26	4-9-4	0.38	4-25-2	0.36
3-28-2	0.26	4-10-2	0.16	4-25-4	0.20
3-28-4	0.28	4-10-4	0.31	4-26-2	0.24
3-29-2	0.14	4-11-2	0.24	4-26-4	0.32
3-29-4	0.27	4-11-4	0.25	4-27-4	0.35
		4-12-2	0.21		
		4-12-4	0.27		

* Includes 0.10% for soluble carbon and blank.

TABLE 19—COMBINED CARBON AFTER 5 HR AT 680 C, FOLLOWING EQUILIBRIUM AT 770 C

Mark	Plant	Combined Carbon, %	Silicon, %
L-20	A	0.46	1.19
L-30	"	0.45	1.08
L-42	"	0.49	1.04
L-43	"	0.52	0.98
L-50	"	0.39	1.07
L-51	"	0.65	0.85
C-29	B	0.34	1.44
C-34	"	0.62	1.09
C-35	"	0.51	1.23
C-48	"	0.56	1.18
C-52	"	0.51	1.20
C-55	"	0.43	1.30
C-62	"	0.55	1.01
C-71	"	0.35	1.30
C-74	"	0.41	1.25
C-80	"	0.54	1.23
3-20-2	C	0.31	1.18
3-26-4	"	0.28	1.28
3-27-4	"	0.28	1.31
3-31-2	"	0.18	1.42
4-2-4	"	0.42	1.05
4-5-4	"	0.41	1.17
4-23-4	"	0.33	1.21
4-24-2	"	0.43	1.13
4-26-2	"	0.40	1.19

The carbon and silicon contents of the A, B and C series are plotted in Fig. 1, which contains a separate plot for each plant and each section size. Circles represent specimens free from visible mottles; dots, specimens showing more or less mottling. Lines are drawn in the diagram showing the boundaries of a zone in which mottling may or may not occur. For each line C + 2.85 log Si is a constant called "b" by Schneidewind and McElwee in their paper.[3] Be it noted that these curves seem well adapted in form to separate the groups, confirming at least the type of relationship these authors selected.

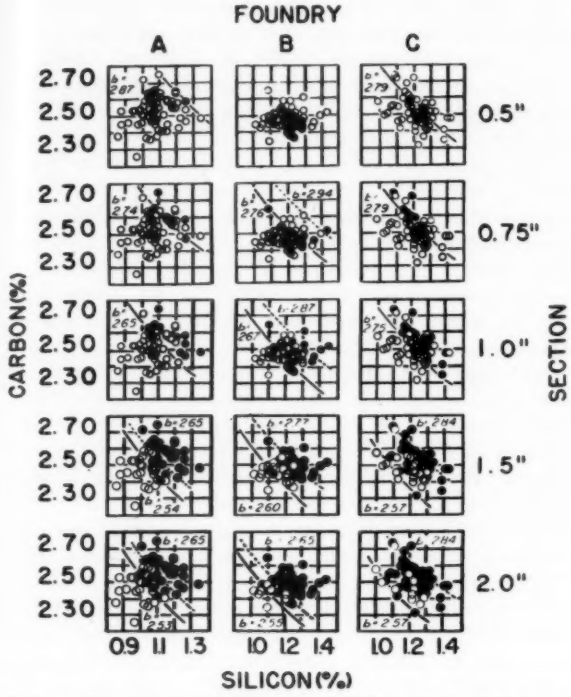


Fig. 1—Occurrence of Mottling as a Function of Carbon and Silicon Content.

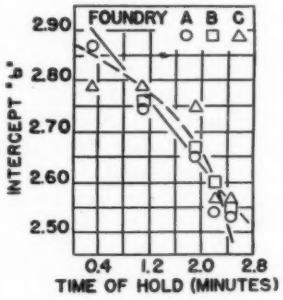


Fig. 3—Safe Values of 'b' for Various Lengths of the Isothermal Arrests.

Figure 2 is a summary of the data of Fig. 1. The specimens are divided into groups according to the smallest section size in which any mottles were ever found. They are further separated according to the plant of origin. The upper and lower groups are of little interest. For the upper group (over 2 in. $\times$ 3 in.) we do not know how much larger a section would have been free from mottles and the lower group contains only 2 points; one from each of two plants. The abscissae are the "b" values calculated from Tables 1 to 3. The B[1] metal described in Table 4 was not studied in the present connection.

Qualitatively it is quite plain how the "b" value below which no mottling occurs, rises with decreasing section size. At the top of the figure the upper and lower limits of "b" between which the iron may or may not mottle, are designated. There appears to be some tendency for the safe "b", i.e., that where mottles cannot occur, to be a little higher at C than at B, where in turn it is higher than at A. Reasons will be cited presently for the belief that these differences are not real. The upper limit of the interdeterminate zone is less interesting and also less well defined. Trends similar to those for the lower limit seem to exist.

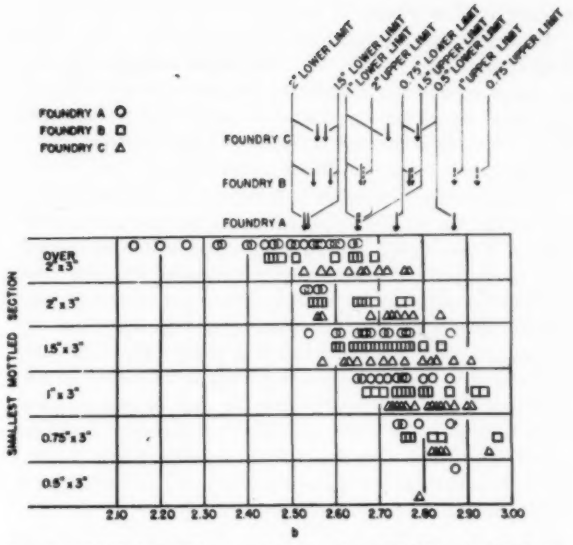


Fig. 2—'b' Values Observed to Produce Mottles in Various Sections.

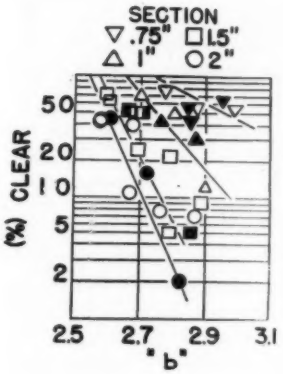


Fig. 4—Probability of Obtaining Freedom from Mottles for Various Sections and 'b' Values.

The safe values of "b" for the several plants are correlated with the time of eutectic hold[13] in Fig.3. The trend line, which perhaps should be convex upward instead of straight, is pretty closely of general application.

The line was selected as representing pretty certainly safe values. If it be accepted as satisfactory then:—

$$\text{safe } b = 2.94 - 0.16\ T$$

where T is the time of the isothermal arrest in minutes. Accepting also the relation of carbon and silicon deduced above, we may write that, to be certainly free from mottles

$$C + 2.85 \log Si = 2.94 - 0.16\ T \quad \ldots\ldots\ldots (1)$$

(1) Carbon and silicon are concentrations in percent.

Some independent observations by Milton Tilley, using a different form of test specimen, furnish a basis for the belief that an increase in pig iron, at the expense of steel, in the mix, would lower the safe value of "b". The data in Table 5 are not suitable for the recognition of this variable.

In Fig. 4 the fraction clear is plotted against "b," using, for convenience, a semi-logarithmic scale. Open

figures are for Plants A and B, black for Plant C.

Whether the logarithmic relation has any theoretical backing or not, the fact that a single family of lines represents the three plants equally well is regarded as evidence that the safe "b" values are alike regardless of such difference of melting practice as are covered by these experiments.

This statement cannot be extrapolated indefinitely. We have also studied certain metal made experimentally at Plant C by transferring duplexed metal, similar to that here studied, to an electric furnace and adding ferrosilicon or a proprietory carburizing compound to make metal of higher "b" values. The experiments were on an operating scale. The safe "b" value was raised to about 2.93 regardless of eutectic freezing time and the width of the indeterminate zone became much less sensitive to changes in thickness than in the experiments on duplexed metal. We do not wish, in the present paper, to discuss metal made by other than duplexing methods.

Figure 4 clearly shows that the probability of avoiding any mottles decreases with increasing values of "b." The rate of decrease grows less as "b" increases. The rate of decrease is also less for fast cooling sections than for slower cooling sections. Thus the width of the band between the "b" value producing freedom from mottling and almost a certainty of mottling is much greater at the smaller sizes.

The existing relationship is not consistent with an assumption that had we included in our "b" the concentration of some other element whose amount scattered symmetrically about a mean value, the range might have been wiped out.

The only elements suggesting themselves are chromium and manganese. A pilot calculation was made on Plant A attempting a correlation by rank between the "b" value at which iron was found free from mottles in the 1½-in. section and in the indeterminate range and the chromium and with the Mn/S ratio. The correlation so calculated had so great a chance of being accidental (90 pct for Cr and 45 pct for Mn/S) that they were entirely unconvincing.

Since boron is often added for other purposes, a similar computation was attempted for that element. There was a 70 pct chance that any correlation was accidental.

We believe it impossible to explain the indeterminate zone by differences of analyses within the existing ranges.

Ten sets of blocks, as previously described, were poured at Plant B from iron of the same general character as the present and 0.0025 pct tellurium was added as the element. The recovered tellurium ranged from 0.0019 pct to 0.0032 pct, perhaps due to inaccuracies in estimating the amount of iron in the ladles to which the element was added. The average tellurium recovery was 0.0025 pct. The "b" value of the metal containing tellurium, and not mottling fell from 0.04 to 0.08 above the safe "b" for the largest clear section. Much more work should be done if the results are to be relied on but there is at least some evidence of tellurium's effect in preventing mottles.

No effect could be noted from the presence of about 0.006 pct bismuth in a similar experiment.

The observations on blocks of different freezing rates are inconsistent with any assumption that the indeterminate zone is due to possible differences in the time of the eutectic hold.

Safe values of "b" having been shown to be similar for the three plants, it is an obvious first conclusion that the systematic differences in melting practice shown in Table 5 are not sufficient to produce detectable differences in mottling tendency.

The question of oxygen's influence on graphitization is often mentioned but no directly conclusive data have been available.

At Plant B no relation of mottling tendency to late ferro-silicon additions, coarseness of pulverized coal, silicon loss in cupola melting or the use of calcium slags could be traced. Also an increase in pig iron in the charge as a substitution for steel and silvery pig suggested a decrease of a few hundredths in safe "b" for an increase of 1 pct in pig. The data were not suited to accurate quantitative conclusions.

At Plant A it was observed that if iron is held in the distributing ladle, mottling decreases. No relation could be traced to the temperature at which the iron left the furnace or the rate of output during the preceding hour. No clear relation to time in the air furnace was substantiated, although it may be that time in the furnace increases rather than decreases "b." No choice was found between malleable scrap and sprue in the cupola charge.

The value of "b" is subject to error since there is an analytical error of some magnitude involved in determining carbon and silicon in the expression

$$\text{"b"} = C + 2.85 \log Si$$

Assuming that the standard error of our determinations is about 0.01 pct for each element and that our silicon values are all near 1.1 pct, the standard error of "b" is[19] approximately 0.06. When the width of the indeterminate band is known in Fig. 1 it varies from 0.11 to 0.20, an amount which is small compared with the possible range corresponding to the standard error of observation. It is thus not impossible that the indeterminate zone represents largely an imperfect knowledge of "b." The form of distribution shown in Fig. 4 is, however, not consistent with a normally distributed error of observation.

Evaluation of Secondary Graphitization Data

Numerical data of the volume here encountered can be grasped only by statistical methods. By least squares J. V. Anthony computed the following equations in terms of concentrations in per cent, applicable specifically to the heat treatments previously described. For first stage graphitization:—

Plant A:—

$$\log \% \text{ completion} = 3.2545 \times \% \text{ Si} - 2.1372 \ldots (2)$$

Standard error of fit = 0.29

Plant B:—

$$\log \% \text{ completion} = 5.2361 \times \% \text{ Si} - 5.2162 \ldots (3)$$

Standard error of fit = 0.28

Plant B′:—

$$\log \% \text{ completion} = 4.5566 \times \% \text{ Si} - 4.7182 \ldots (4)$$

Standard error of fit = 0.27

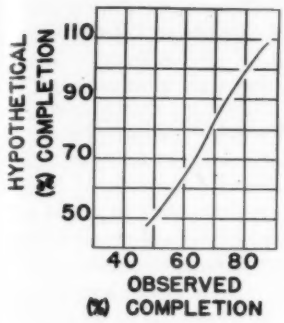


Fig. 5—Correction of Graphitization Rates over 50% Completion.

Plant C:—

log % completion = 1.9580 × % Si — 0.6446.. (5)

Standard error of fit = 0.12

For Plant C it proved expedient to translate the observed per cent completion, when beyond 50 pct, to a hypothetical value which would have been reached if the 3/2 power of time law had continued. These values may be read from Fig. 5 based on Ref. 5. They have no practical meaning but are reliable aids in computation. More completely:—

Plant A:—

log % completion = 2.4299×% Si — 12.04×% B —5.7143×%Cr+0.4892×Mn/S—0.0005×N/$\overline{mm}^2$ —2.7580 ..... (6)

Standard error of fit = 0.22

Plant B:—

log % completion = 6.3498×% Si + 7.660×% B —0.1335×%Cr+0.1450×Mn/S—0.0059×N/$\overline{mm}^2$ —6.66745 ..... (7)

Standard error of fit = 0.27

Plant C:—

log % completion = 1.1792×% Si — 108.17×% B —3.3216×%Cr+0.2027×Mn/S—0.0017×N/$\overline{mm}^2$ —0.0850 ..... (8)

Standard error of fit = 0.07

Or alternatively in terms of excess Mn (%Mnx = % Mn — 1.71 × % S)

Plant A:—

log%completion=1.7104×%Si—0.0004×N/$\overline{mm}^2$ + 5.3074×% Mnx+5.9511×% Cr—39.46×% B —1.3325 ..... (9)

Standard error of fit = 0.20

Plant B:—

log%completion=4.8562×%Si—0.0023×N/$\overline{mm}^2$ —4.6583×%Mnx—0.6019×%Cr+27.1479×%B —5.5669 ..... (10)

Standard error of fit = 0.29

Plant C:—

log%completion=1.2468×%Si+0.0021×N/$\overline{mm}^2$ +0.4488×%Mnx—4.1565×%Cr—96.240×%B +0.2995 ..... (11)

Standard error of fit = 0.08

There is little to choose between the three series of equations except for Plant C where the addition of other variables, whether Mn/S or Mn has significantly improved the fit.

For first stage graphitization of pretempered iron:—

Plant A:—

log%completion = 3.8284×%Si—2.6677 ..... (12)

Standard error of fit = 0.31

Plant B:—

log%completion = 2.5407×%Si—1.4693 .... (13)

Standard error of fit = 0.16

The values of all these standard errors are themselves subject to uncertainties measured by their own respective standard errors. These differ from group to group, being dependent on the number of observations underlying the several equations. They are of the order of magnitude of 10 pct of the standard errors, so that differences between the standard errors of two relations for the same plant are of no significance below 15 pct of their numerical value and a difference of about 30 pct of the value is required to produce reasonable certainty that the fit of one rather than the other is the better. The conditions are also such that the standard errors of observation for Plant C are decidedly lower than for the other two plants, so that the better fit for these equations is due to (accidentally) more favorable experimental conditions. The standard errors of Equations (6), (7) and (8) do not differ significantly from the standard errors of observation, so that these equations express the existing numerical relations as accurately as we are able to conduct the experimentation.

For intermediate stage graphitization:—

For Plant A:—

log % combined carbon=1.6601—2.1428×%Si (14)

Standard error of fit = 0.16

For Plant B:—

log % combined carbon=0.1490—0.6633×%Si (15)

Standard error of fit = 0.08

For Plant C:—

log % combined carbon=0.9503—1.5448×%Si (16)

Standard error of fit = 0.22

Or more completely:—

Plant A:—

log % combined carbon=1.5586—2.0117×% Si —22.652×%B—0.00016×N/$\overline{mm}^2$—4.6240×%Cr +0.0640×Mn/S ..... (17)

Standard error of fit = 0.15%

Plant B:—

log % combined carbon = —0.0308—0.4993×%Si +0.6770×%B—0.00271×N/$\overline{mm}^2$+3.5033×%Cr +0.0055×Mn/S ..... (18)

Standard error of fit = 0.08%

Plant C:—

log % combined carbon=0.7893—1.7146×%Si —69.261×%B—0.00082×N/$\overline{mm}^2$—1.5085×%Cr +0.2153×Mn/S ..... (19)

Standard error of fit = 0.21%

The fit has not been improved by the inclusion of additional variables.

The standard errors of observation turn out to be 0.19, 0.19 and 0.43, showing that the agreement is better than our *a piori* estimate of the precision of our work. This also is confirmation of the satisfactoriness of the form of our equations since they express the relations as accurately as they can be measured.

As has been said before "second stage" annealing is of little practical interest since operations should be, and are frequently (even if unconsciously) conducted to take maximum advantage of "intermediate stage" annealing. This leaves no definable starting point for any first stage which might be intended. As applying to annealing commencing with a graphite pearlite structure not containing free ferrite, Anthony calculated—

For second stage annealing:—

For Plant A:—

log % combined carbon=0.2507—0.6407×%Si ..... (20)

Standard error of fit = 0.05%

For Plant B:—

log % combined carbon=0.5635—0.8132×%Si ..... (21)

Standard error of fit = 0.06%

For Plant C:—

log % combined carbon=1.3812—1.6796×%Si ..... (22)

Standard error of fit = 0.07%

Or more completely:—

For Plant A:—

log % combined carbon=2.2130—0.8954×%Si +0.0793×Mn/S ..... (23)

Standard error of fit = 0.053

For Plant B:—

log % combined carbon=2.5146—0.8785×%Si +0.0448×Mn/S ..... (24)

Standard error of fit = 0.061

For Plant C:—

log % combined carbon = 2.7532—2.0140×%Si +0.3661×Mn/S ..... (25)

Standard error of fit = 0.045

Only for Plant C is there a marked improvement. An insufficient number of samples was studied for the inclusion of other variables.

For any realizable values of combined carbon these numbers compare favorably with standard error of observation of log % combined carbon which is the principal contributor to the standard error of observation of these equations.

Largely as a matter of information the relation of nodule number to chemical compositions was computed for quick heating (i.e. in ½ hr to 900 C).

Plant A:—

$N/\overline{mm}^2$ = —705—376×%C+1558×%Si+21067 ×%B ..... (26)

Standard error of fit = 26

Plant B:—

$N/\overline{mm}^2$ = 73—47×%C+84×%Si+23×%B . (27)

Standard error of fit = 3

Plant C:—

$N/\overline{mm}^2$=229—112×%C+134×%Si—14381× %B ..... (28)

Standard error of fit = 5.

The standard errors of observation of nodule numbers were 5, 3 and 5 and of the equations as a whole, 27, 3.1 and 1 respectively. Plainly our equations are as good as the observations.

All the data are specific to the ½-in plate. Investigations not here reported indicate that the graphite formed under the same circumstances during "first stage" graphitization in the center of 1½-in. plates requiring four times as long to freeze (by actual test) would be about 0.7 times that here recorded.

Discussion of Secondary Graphitization Data

The loci of Equations (2) to (5) intersect, generally near to or above the practical limits of silicon content and the relations are such that at usual silicon values the metal having the highest coefficient of silicon is the one annealing most poorly for a given silicon content. Near 1 pct silicon Plant A produced metal graphitizing about 20 times as fast as B′, the differences are thus of major significance.

It is not necessary to seek separately for explanations of the coefficient of silicon and of the constant in these equations for it can be shown that very closely

Constant = 1.494 × coefficient — 2.4365

so what explains one explains the other.

Perfect correlation by rank exists between the coefficients of silicon in Equations (2) to (5) and the rate of thruput of cupola and air furnace, the rate of thruput per square foot of hearth area, the iron/coal ratio, cupola carbon, silicon loss in the cupola, silicon loss in the air furnace and perhaps carbon loss in the air furnace. (There is about an 8 pct chance of achieving perfect correlation, accidentally).

Many of these variables are obviously inter-correlated and we have regarded cupola oxidation and thruput per unit area of hearth as the significant factors.

Plants A and C, having the lower coefficients of silicon, have the higher pig content in the charge and also somewhat the higher steel. Of the two, A and C at similar pig content, C with the lower steel has the better annealability and the same is true of B and B′.

The two latter differ in moisture more than any other pair and differ but little in annealability, which rather militates against using blast moisture as an important item.

The line of regression of the coefficient on cupola carbon has a standard error of 0.17. Those on thruput and silicon loss (% silicon in the cupola charge — % silicon in cupola melt) have standard errors several times larger. Its equation is

Coefficient = 52.9888 — 18.048 × % C

An attempt to reduce its error by including moisture or rate of thruput proved useless. However, the equation

Coefficient = 52.8854 — 18.048 × % C + 0.3850 × % Si lost ..... (29)

has a standard error of estimate of only 0.12 and is thus an improvement. Let the reader carefully remember that this equation is an empirical statement of an existing numerical relation. It says nothing about the actual mechanism of the process and may be an expression of deeper causes such as pig iron in the charge plus oxidizing action in the cupola.

We have previously found a relation between the coefficients and constants of Equations (2) to (5). Combining that relation with Equation (29) we have:—

$$\log \% \text{ completion} = (52.8854 - 18.048 \times \% \text{ Cupola C} + 0.3850 \times \% \text{ Si loss}) \times (\% \text{ Si} - 1.494 + 2.4365 \quad \ldots\ldots\ldots\

chromium coefficient without disproof by the statistics. Thus we can lay little weight on any (small positive) number. The negative numbers are probably unreal.

The coefficients of silicon in Equations (20), (21) and (22) are all significant. Their mean value is 1.0445. The coefficients for Plants A and B do not differ significantly from this value but Plant C has a 6 pct chance of being due to accident, which is almost up to our 5 pct fiduciary limit.

Our data do not permit valid comparisons involving variables other than silicon. Comparing the coefficients of silicon in Equations (2), (3), (5), (12), (13), (14), (15), (16), (20), (21) and (22) it will be seen that the potency of that element does not fall in the same order (or reverse order) for the three stages of anneal.

Prompted by the parallelism of $N/\overline{mm^2}$ and silicon coefficient (see page 12), which persists also if the data for pretempered iron are inserted, and by the suggestion of Milton Tilley that the changes in melting conditions go hand in hand with changes of nodule number, we find that:—

$$\text{Coefficient of Si} = 483.98\ A - 3.7739 \quad \ldots\ldots \quad (31)$$

Here A is the area per nodule, i.e. $\overline{mm^2}/N$, which is found more nearly linearly related to the coefficient than the latter value.

Combinations of Equations (31) with (29) or with the unnumbered equation correlating the coefficient and constant lead to no usable conclusion. The equations are derived from, and apply to, the averages for the several plants and are apparently not applicable within individual plants.

It is interesting that in first stage anneal an increase in Mn/S consistently favors graphitization and in intermediate stage the reverse is true. This must be read as applicable within the specific composition range of the present data. Presumably an excess of either manganese or sulphur is harmful to graphitization and there should be an optimum relationship of the two elements. The present observations point to different optima for first stage and intermediate stage graphitization. Quite plausible explanation of this is possible but inappropriate in a paper dealing with observed relationships and not with principles.

Having found that the graphitizability of this metal can be studied with considerable satisfaction without consideration of elements other than silicon, we proceed to a brief graphic comparison of the effect of that element on annealing time at the several plants.

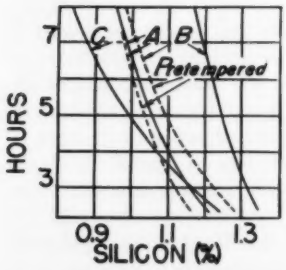


Fig. 6—Time for ½ Completion of First Stage Graphitization as a Function of Silicon.

In Fig. 6 the time required to complete ½ the reaction possible at 900 C (1650 F) has been plotted as a function of silicon for the three plants and for the pretempered metal at two of them. (Actual completion is unattainable but 90 pct completion can be had in about 3½ times the time for 50 pct completion and 95 pct completion in about 6 times that for 50 pct). Plant B differs greatly from A and C but after pretempering A and B come close to the unpretempered A. C maintains an advantage at low silicon.

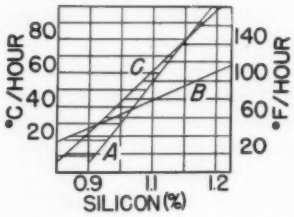


Fig. 7—Cooling Rates to Secure ½ Completion of Intermediate Stage Annealing as a Function of Silicon.

Figure 7 correlates the cooling rate required to produce 50 pct completion during intermediate stage annealing with silicon content. (Again completion is theoretically impossible and 95 pct completion would require rates about 0.3 times those for 50 pct completion). The greater similarity of A and C as compared with B is again evident.

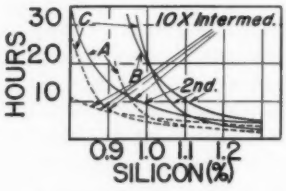


Fig. 8—Times for ½ Completion of Intermediate Stage and Second Stage Annealing as a Function of Silicon.

Figure 8 contains curves correlating time for breaking up at 680 C (1260 F), 50 pct of the pearlite remaining after completion of the supercritical reaction. (For 95 pct completion these times would have to be multiplied by about 3.3). The enormous handicap imposed by low silicon is evident. The dotted lines show *10 times* the time required to complete the decomposition of pearlite to the same degree (50 pct) in the intermediate stage. The difference is most marked for B, then C and lastly A. The differences increase with decreasing silicon.

Conclusions

The authors offer no conclusion as to causes or mechanisms; only as to relationships.

Mottling tendency, for a given time of eutectic hold in freezing, is a function of carbon and silicon. No other variables could be traced as having a determinable effect.

First stage annealing rate is predominantly determined by silicon.

The potency of silicon, i.e. the change produced in graphitizing rate by a given change in silicon concentration depends very greatly on melting conditions, which are also accompanied by a change in nodule number. The differences in potency accompanying changes in melting conditions are minimized if heating to graphitizing temperature is slow.

Intermediate stage annealing by the reaction converting austenite to ferrite and graphite is the fast-

est method for destroying pearlite. Its rate again is primarily a function of silicon content. As before, the silicon potency is variable but its changes have not been correlated with other variables. As for first stage, the potency at various plants varies but is rather nearly alike for A and C and decidedly different from B in the same direction.

The effect of Mn/S ratio is in the opposite direction for first stage and intermediate stage graphitization.

Our equations fit the facts within the observational error incident to good analytical work. Nevertheless we cannot speak positively regarding the quantitative effects of other variables. Their concentration and potencies produce variations whose results are largely obscured by experimental errors and silicon effects. Some of them at least may have potencies varying with process changes.

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DISCUSSION

Chairman: C. F. SEMRAU, Illinois Malleable Iron Co., Chicago.
Co-Chairman: F. A. CZAPSKI, Chicago Malleable Castings Co., Chicago.
Recorder: F. A. CZAPSKI.

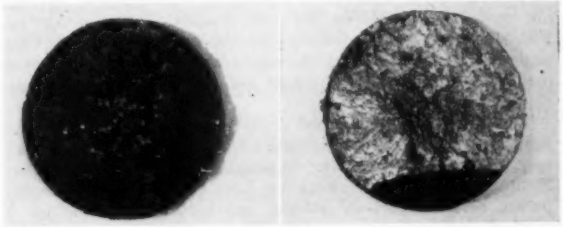
R. W. HEINE *(Written Discussion):*[1] The authors of this paper should be complimented on their fine contribution to malleable iron metallurgy. The information provided by this paper, based on a statistical analysis of extensive data, is of great practical value.

The writer has certain questions which arise from comparing the authors' conclusions with those of the writer's, based on laboratory experiments performed in connection with the AFS malleable iron division research project.

MOTTLING

Considering the graphs, Fig. 1, the total spread of carbon content for mottling in a particular section size and silicon range appears to be about 0.30-0.50 per cent C. A number of possibilities were studied by the authors, apparently without satisfaction. The writer wishes to offer melting furnace atmosphere effects as a possible source of this carbon range variation. In the AFS malleable project work, it has been found (See report, pp. 107-130) that the carbon content for mottling in an iron of about 1.30 per cent Si may be altered from about 2.35 per cent C to over 2.85 per cent C, depending on the melting furnace atmosphere. Minimum carbon content for mottling is promoted by CO or CO-rich atmospheres. Higher carbon contents for mottling are caused by N, air, H_2O, and H_2 melting furnaces atmosphere. An example of this effect is illustrated in Fig. A. Shown in Fig. A is the sprue test fracture, 2 in. in diameter by 9 in. long, taken from 100-lb heats.

The metal in Fig. A was held for 1 hr at 2800 F under CO and N atmospheres, respectively. While the iron in Fig. A is not completely white, the effect of the atmosphere is well illustrated. In other experiments, it was found that the mottle point was about 2.35-2.40 per cent C under CO atmospheres and 2.55-260 per cent C under N atmospheres. Extensive data of this type are given on pages 107-130. Thus, it appears that the "b" value below which no mottling occurs may be markedly altered by melting furnace atmosphere. It is our hope to eventually extend our studies to furnace atmospheres of a practical combustion nature to further explore this point.



CO	Atmosphere	N
2.60	% C	2.67
1.25	% Si	1.24
2800	Pouring Temp.	2800

Fig. A.

FIRST STAGE GRAPHITIZATION

In your consideration of first stage graphitization you bring out the point that Plant A produced metal graphitizing about 20 times as fast as Plant B prime, with metal at 1 per cent silicon; and, again, you state that the difference in anneability correlates with melting practices. In our experimental work we have found that the melting furnace atmosphere has a marked effect on the ability to eliminate carbides during first stage graphitization. Carbon monoxide-rich atmospheres were found to cause an extension of the time required for first stage graphitization, as compared with nitrogen and air atmospheres. I wonder if the difference in the metal from Plant A and Plant B prime may in part be related to the differences in melting conditions, especially the combustion conditions and resultant gases.

Another conclusion of interest in your paper is the statement that moisture in the blast appears to have little effect on first

[1] Associate Professor of Metallurgical Engineering, Dept. of Mining and Metallurgy, University of Wisconsin, Madison.

stage graphitization. This was also a conclusion which we came to in our work on the malleable project, although not as positively as in your work.

In your conclusions the statement is made, "The potency of silicon, i.e., the change produced in graphitizing rate by a given change in silicon concentration depends very greatly on melting conditions, which are also accompanied by a change in nodule number." I am certainly in agreement with this conclusion since we have found great differences in nucleating tendency and graphitizing rates, depending on melting furnace atmospheres. I believe that it is quite satisfying to note that similar conclusions have been obtained using two distinctly different methods of approach.

With regard to first stage graphitization, there appears to be marked agreement between the statistical analyses of plant operating data and the laboratory experiments performed for the AFS malleable iron research project when the same points are under consideration.

DR. SCHWARTZ *(Reply to Prof. Heine):* It is gratifying to note that Prof. Heine finds points of agreement between his work and ours. Since we have no observational knowledge of any aspect of the melting furnace atmosphere composition, we cannot intelligently discuss our data from that viewpoint. It has been an object of our work to discuss only those variables which we could actually measure.

It may not be amiss, however, to make some comment on the general subject of furnace atmospheres insofar as these have been known to us in other connections.

In a duplex operation we might be concerned with two different atmospheres; that in the cupola and that in the air furnace. The silicon loss in the cupola at Plant B is consistently higher than that at either Plant A or Plant C. If this is related to the composition of the cupola flue gas, then one might suspect a difference in flue gas composition. As between Plant B on the one hand and A and C on the other, we do not note any difference in mottling tendency. Since we do not know, but only imagine, that the difference in silicon loss is related to a difference in atmosphere, this negative result does not disprove a relation between cupola atmosphere and mottling rate but may merely prove that our speculation that the atmospheres were different, is wrong.

It is quite possible that if we had flue gas analyses of the duplex furnace corresponding to all of the 200 odd test results, something might have been learned on this subject. We did not make these analyses for two reasons. First, we do not have in mind that anything very useful would result, and second, the keeping of a Research Analyst at each plant to make continuous analyses on so many days would have been prohibitive anyway.

The reason we would not *a priori* have thought the labor well spent is that we have never found any enormous difference of composition in the atmospheres of commercial reverberatory furnaces. We think we have proved that the equilibrium between carbon, oxygen and hydrogen in the gas is reached with greater precision than an analysis can be made, and we think that the carbon monoxide-carbon dioxide equilibrium approaches measurably close to the equilibrium conditions for metal of a given carbon content. We may be in error on this point and disclaim specific knowledge pertinent to the present investigation. It is possible, but in our opinion unlikely, that the air furnace gases in the three plants differ systematically.

All this sums up to the original statement that we are without observational knowledge to confirm or reject Prof. Heine's suggestion that the compositions of the melting furnace atmosphere explain any of the matters which we have recorded. In any event we are glad that he does not feel they contradict his observations.

CO-CHAIRMAN CZAPSKI: Dr. Schwartz I note in some of the tables containing the chemical composition of the hard iron representing the three plants there is a wide range in the boron content. I am thinking of boron contents ranging from 0.001 to 0.007 per cet being used with hard irons containing an average chrome of 0.03 to 0.04 per cent. Is this not unusual?

DR. SCHWARTZ: This may be attributed to the boron addition having been made to the air-furnace bath in place of ladle additions.

W. G. WILSON:[2] My question also relates to the effect of boron in the equations which you have presented. In the equations in which you give a number of factors affecting the annealability of malleable iron, you have given a quantity representing the effect of boron. In the equations representing the annealing times required at plant A and C, the signs in front of the factor representing the effect of boron are negative and in the case of plant B, the sign is positive. As I understand these equations, the difference in the signs means that in two cases the effect of boron is negative, or has a retarding effect on annealability, and in the other case, the effect is positive, or promotes annealability. Would you care to comment on this question?

Also, Dr. Schwartz, you have been one of the strong proponents of the use of ferroboron in malleable iron. Therefore, I wonder if, as a result of this work, you are reversing your position on the effect of boron in malleable iron.

DR. SCHWARTZ: Since most malleable graphitization problems relate to intermediate stage graphitization, equations 17, 18 and 19 are the more pertinent ones. In these equations the coefficient of boron is observed to be quite variable and also the changed sign at Plant B. At that plant, however, the coefficient is very small.

In the discussion of secondary graphitization data (see p. 239) it is stated "None of the boron coefficients differ significantly from their mean value," which is about —30. It is thus shown that an increase in boron decreases the logarithm of the combined carbon for a given heat treating cycle, which confirms the idea that boron acts favorably in intermediate state graphitization.

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PERFORMANCE OF CAST AND ROLLED STEELS IN RELATION TO THE PROBLEM OF BRITTLE FRACTURE

By

W. S. Pellini†, F. A. Brandt* and E. E. Layne*

ABSTRACT

The inherent characteristics of cast steels in relation to the problem of brittle fracture are established and compared to the characteristics of rolled steels of equivalent metallurgical type. It is demonstrated that conventional Charpy V and Keyhole notch toughness properties of cast and rolled steels are similar; also, that the resistance to brittle fracture is exactly the same. The significance of notch toughness data is deduced by means of tests designed to develop brittle fracture under the same conditions that brittle fracture is developed in service. The theoretical and engineering aspects of the development of brittle fracture are discussed for the purpose of presenting comprehensive analysis of the overall problem.

Introduction

The problem of the brittle fracture of apparently ductile steels is probably as old as the use of the metal itself. Crude nick-bend tests of various types have been used for many centuries to evaluate the tendency of steels to brittle fracture. Present day Charpy and Izod notch-bend tests which were first developed approximately 50 years ago, represent refinements of these older methods. Despite the widespread use of notch-bend tests for specifications and despite advances in our knowledge background, the problem of brittle fracture is presently more serious than ever before. This is the result of the increased design complexity, higher stressing and lower temperatures of services which are required of engineering structures and machinery components. The premium which is placed on lightness and design efficiency does not permit over-design, which was the common procedure of the past. For many applications safety must be ensured by the use of metals which are inherently resistant to brittle fracture.

The inherent quality of cast steels in relation to brittle fracture is of considerable engineering and economic significance. Unfortunately, the general opinion of engineering circles regarding cast steels is based on old comparisons which now should be reconsidered. In great part these comparisons concerned the relative performance of similar types of mild steels used either as castings or as rolled plate members of riveted structures. The rolled steels generally performed satisfactorily while the cast steels were noted to be susceptible to brittle fracture. Accordingly, it was concluded that cast steels were inherently brittle in comparison to rolled steels.

With the advent of welding fabrication the formerly reliable rolled steels also demonstrated susceptibility to brittle fracture. The dramatic failures of welded bridges, pressure vessels and merchant ships entailed the brittle fracture of the same steels which had formerly performed satisfactorily in riveted structures. The causes of this change in the performance of rolled steels were gradually clarified during the past few years as being due to a combination of circumstances including: (1) The presence of sharp cracks resulting from welding and (2) the rigidity and continuity of the all-welded structure, described as monolithic design.

Riveted structures in general do not contain severe defects such as cracks; moreover, the lack of rigidity permits small movements at highly stressed points with consequent redistribution of loads. Castings, like weldments, are monolithic and subject to the presence of defects at critical design positions. Inasmuch as the rigidity of such structures does not permit shifting of overloads, regions of high stresses or localized deformation are developed at critical positions.

Brittle cracks which may start from flaws at these positions may then progress to the complete collapse of the structure, because of the continuity afforded by welded or cast design. Riveted design provides a protection in this respect since cracks, if developed, are generally stopped at riveted laps. Welded ships, for example, are now protected from complete failure by the use of riveted joints termed "crack arrestors"; these are placed at strategic points on the ship.

It may be concluded that the unfavorable showing of cast steels as compared to rolled steels observed before the advent of extensive welding fabrication was due, at least in part, to *structural* differences rather than to differences in the inherent resistance to brittle cracking. While both rolled and cast structural mild steels have demonstrated tendencies to brittle fracture at normal service temperatures the

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question of the relative tendencies in this respect has remained controversial.

It is now considered that the overall problem of brittle fracture may be separated into two general aspects:

(1) The aspect of design and fabrication. This aspect concerns the factors of structural continuity, rigidity and probability of flaws; i.e., mechanical factors which relate to conditions which favor the initiation and propagation of brittle cracks.

(2) The aspect of the inherent notch toughness, i.e., the intrinsic properties of the material with respect to brittle fracture. This aspect concerns material properties and primarily relates to metallurgical factors.

The separation of the brittle fracture problem into mechanical and material aspects is realistic in that the level of notch toughness required of the metal is related to the design and methods of fabrication. In general, notch toughness specifications are aimed at obtaining materials of sufficient notch toughness to insure satisfactory performance for specific conditions of service. The problem cannot be resolved by requiring excessively high levels of notch toughness since the metallurgist can provide this only by expensive alloying. Castings for general service, for example, cannot be made using alloy steels because of economic limitations. The problem therefore entails establishing notch toughness requirements which are a realistic compromise between service and cost factors. In this respect the question of the relative notch toughness of cast and rolled steels assumes importance equal to questions of relative production costs in design decisions.

In order to assess the relative position of cast and rolled steels in relation to the problem of brittle fracture it is essential to consider not only the relative notch toughness as indicated by conventional laboratory tests but also the significance of these tests. For purposes of presenting a broad analysis of this problem the present report includes an initial section entailing a general discussion of the basic factors which determine the development of brittle fracture.

Mechanical Aspects of Brittle Fracture

It is generally recognized that the conditions which are conducive to brittle fracture entail:

(1) Unfavorable stress systems such as developed by notches or sharp cracks.

(2) Low temperatures.

(3) High strain rates as represented by rapid loading.

Of these three factors the stress system and temperature are by far the most important. The use of the term "impact tests" in regard to Charpy and Izod type tests is misleading for it connotes that the principal feature of the tests is the impact blow. The notch and the use of low temperatures are the principal features of these tests, the feature of impact loading is of secondary importance.

The nature of the mechanism of brittle fracture was practically unknown 30 years ago. However, considerable progress has been made, particularly during the last ten years, so that today a fairly comprehensive understanding has been evolved. As a first consideration it should be recognized that fracture may occur by a process of shear or by a process of cleavage. The shearing process entails plastic deformation with consequent high absorption of energy; such fractures commonly show a fibrous texture representing a multitude of small tensile ruptures of single or groups of grains. The cleavage process entails the separation of individual crystals in the absence of deformation with consequent low energy absorption; such fractures commonly show a grain texture representing the facets of cleavage planes of the individual grains.

Figure 1 illustrates the significant differences in the mode of propagation of the two types of fracture. Fibrous fracture proceeds by a relatively slow tearing process which requires continued application of load and high input of energy. Brittle fracture proceeds in a rapid and spontaneous fashion, i.e., the elastic strain energy (elastic "spring" action) developed by the initial load serves to provide all of the small amount of energy required for propagation. In this sense brittle fractures may be termed "free-running" in that propagation is not dependent on continued loading.

Fracture Types

Most metals prefer to fracture only in one manner, i.e., either by shearing or by cleavage. A few metals, of which iron in the form of steel is the outstanding case, may be made to change relatively easily from one mode of fracture to another. It should be noted that these statements pertain to changes in the mode of *fracture* and not merely to decreases in *ductility* by concentration of strain to small regions. For example, most metals when notched show a decrease in ductility as judged by the extent of deformation developed prior to fracture; however, only certain metals show the familiar transition from ductile to brittle fracture due to increased notch sharpness and decreased temperature.

Figure 2 illustrates that metals having a body-centered cubic crystal structure (B.CC.) show temperature transitions while face-centered cubic (F.C.C.) metals do not. The energy curve presented for the F.C.C. metal may be lowered by a sharper notch, however, the important fact to note is that a temperature transition is not developed in any case; actually, the energy curve rises with decreased temperature indicating increased resistance to shearing.

In considering the mechanism of brittle fracture it is necessary to differentiate between the nature of the initial notch or crack (flaws) which precipitate failure and the nature of the fracture which then propagates through the material. The nature of the original notch or crack is all-important in determining the extent of deformation which precedes the development of the fracture. The nature of the fracture *per se* is established primarily by the inherent properties of the metal at the temperature in question. In other words, the original "notch" provides the inducement to brittle failure while the metal itself determines if the inducement is acceptable by

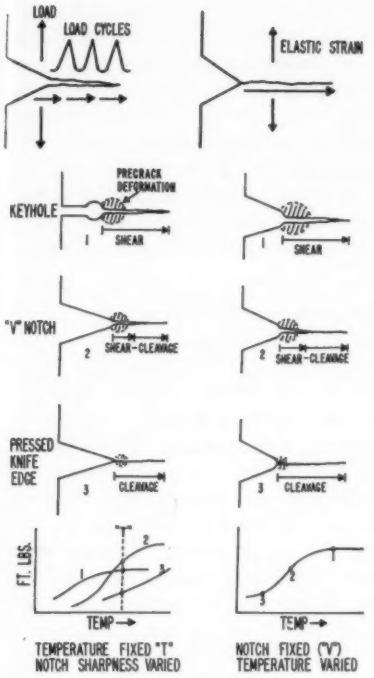


Fig. 1 — Characteristics of fracture propagation. Left — Fibrous Fracture-Crack moves forward no faster than speed of loading. Right—Brittle Fracture-Crack movement nearly continuous; stored elastic strain sufficient to propagate crack at speeds which may be 100-1000 times faster than speed of loading.

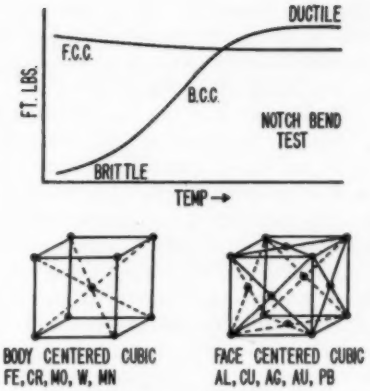


Fig. 2—Temperature transition specific to metal types.

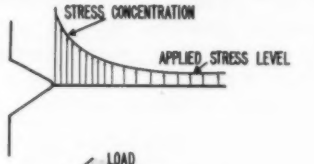


Fig. 3—Notch and temperature effects.

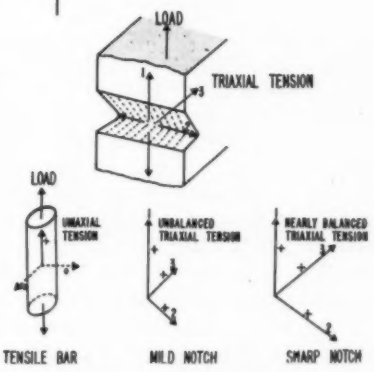


Fig. 4—Notch stress conditions.

permitting, or not, development of a brittle fracture.

The energy developed in the fracture of Charpy-type specimens represents the sum of two specific energies: (1) the energy required for pre-crack deformation, and (2) the energy required for crack propagation. Figure 3 presents a simplified illustration of the effect of notch sharpness and lowered temperature on pre-crack and post-crack deformation; high energy absorption is indicated schematically by the volume of metal deformed prior to cracking and by the development of fibrous type cracks. It should be noted that in the case of fractures of intermediate energy absorptions characteristics, the crack which is initially of a fibrous nature gradually gravitates to cleavage type as the crack front is sharpened during the process of crack propagation.

It is observed from the schematic drawings that the effects of increasing notch sharpness entail decreasing pre-crack deformation and producing a sharper initial crack. The conditions represented are specific for the fracture mechanics of the three specimens at a temperature "T", as related to the energy-temperature transition curves shown. It is observed also that the effects of decreased temperature entail similar mechanics in the temperature range of *possible* brittleness. At a certain limiting high temperature brittle fracture becomes impossible regardless of notch sharpness.

The effects of notches in limiting the amount of pre-crack deformation and promoting cleavage arise from several related factors:

(1) Concentration of strain to small volumes at the tip of the notch.

(2) Increase of stress level at the notch tip above that of the applied nominal stress.

(3) Increase of the strain rate at the point of localized deformation.

(4) Development of triaxial stress conditions.

These factors may be summarized to a simple statement—notches restrict deformation to small regions and at the same time act to prevent the small amount of flow which tends to develop. Under such contrary conditions the metal, if susceptible to cleavage, has no choice but to do so.

The mechanics by which notches act to prevent flow require discussion. This question relates primarily to the stress state developed at notch fronts. Figure 4 illustrates the stress state factors schematically. The stress concentration signifies that plastic flow starts at the notch root at nominal elastic loads. The triaxial stress condition signifies that plastic flow is made difficult by restraint in all possible directions. It is observed that the principal stress "1" acting normal to the notch is opposed by a lateral stress "2" acting along the base of the notch and by a thickness direction stress "3" acting in the direction of the notch. In simple or uniaxial tension as developed in a tensile bar the opposing stresses "2" and "3" are absent; accordingly, elongation in the direction of loading, with consequent reduction in thickness, is not opposed by lateral restraint stresses. Increasing the sharpness of the notch causes stresses "1", "2" and "3" to approach balance; as the opposition to flow in the various directions approaches balance, it becomes impossible to develop flow in any

direction whatever. This situation is similar to the case of an object for which the action of forces tending the cause movement in one direction is countered by balanced opposing forces.

Restricting flow has the serious effect of permitting a rise in the level of the principal stress "1" to a point at which cleavage occurs (cleavage stress). It is thus possible to restrain flow to a point such that brittle fracture occurs before appreciable plastic deformation is developed at the root of the notch. The amount of deformation (hence ductility) developed is directly proportional to the degree of flow restraint. Thus, sharp notches which feature high triaxiality limit deformation to small amounts while notches of lesser sharpness which feature lower triaxiality permit greater deformation. Figure 5 illustrates graphically that the extension (plastic flow) resulting from a given stress level (principal stress refers to stress "1" in the case of notches) depends on the nature of the stress system as described above.

Temperature and Strain Rate

The effects of lowered temperature and increased strain rate basically involve increasing the apparent flow resistance (viscosity) of the metal. Tar shows similar effects, low temperature and fast loading results in fracture without flow while high temperatures and slow loading are conducive to plastic flow. Figure 6 illustrates graphically the flow restricting effects of lowered temperature. The effects of increased strain rate are in the same direction but smaller. It is observed that the changes developed in tensile specimens (simple tension) are of non-critical nature while for notched specimens (triaxial tension) the changes may be sufficient to completely eliminate flow prior to fracture.

It is obvious that these various factors operate equally for specimens or structures. A simple structure without corners or sharp flaws operates essentially as a tensile specimen. Complex structures with sharp corners, or simple structures with crack-like imperfections operate as notched specimens. Thus, the structure may deform drastically before a fracture is started or it may fail under elastic loads. In this respect it should be recognized that a sharp corner in a structure featuring a stress concentration factor of say 2, plus a notch-like defect of stress concentration 4 at this position effectively operates under the disadvantage of a total stress concentration factor of 8 times the applied nominal stress. If the steel of such a structure permits the development of a brittle crack at low levels of notch deformation, the structure may fail at loads considerably below the nominal elastic limit. The importance of notch toughness in this respect is clearly evident.

It is also evident that the designer faces the difficult task of determining the expected degrce of stress concentration in his structure. Stress concentration factors pertaining to design (corners, etc.) are readily determined; however, the great factor of uncertainty is the *chance flaw*. With this uncertainty it is necessary to either specify steels which are insensitive to the presence of severe defects (high notch toughness) or to apply conservatively high factors of safety often referred to as "factors of ignorance." The ignorance in this case may not involve a lack of design knowledge but uncertainty as to the nature of the defects which may accidentally be developed during fabrication or casting. The design difficulty is compounded by the need to consider factors of cost, design efficiency, critical nature of the service, etc., in arriving at a decision. It is not economically feasible, for example, to design on a 100-1 chance of having a severe defect for a structure entailing non-critical service.

Metallurgical Aspects of Brittle Fracture

The previous section concerned the various mechanical factors which affect the ability of the metal to deform in preference to brittle failure. Mechanical factors affect performance primarily by restricting flow and promoting the build-up of stresses to values of the critical cleavage stress required for brittle fracture. The value of the critical cleavage stress is not significantly affected by mechanical variables. Metallurgical factors determine the inherent ability of the metal to develop deformation despite the flow restricting effects of high stress triaxiality. The primary effect of metallurgical factors is on the level of the "effective" critical cleavage stress. To understand the action of metallurgical variables it is necessary to consider the mechanism of fracture on a micro-scale; for simplicity only the case for steels is considered.

Iron is a relatively low-strength, highly ductile metal which is strengthened and somewhat reduced in ductility by the addition of carbon which results in the formation of a carbide phase. The carbide phase is *per se* a relatively hard and brittle constituent not capable of plastic flow and easily subject to cleavage. The distribution of the carbide phase is of major importance in determining the properties of the steel. In general it is desirable to effect a fine distribution of the carbide phase (1) since this results in a strong keying action which strengthens the ferrite matrix and at the same time provides a minimum path for preferential cracking through the brittle phase.

Figure 7 illustrates schematically that the path of fracture in a pearlitic steel is preferential through the brittle pearlitic patches; this results in lowering the effective cleavage stress hence in low resistance to brittle cracking. The fine spheroidal structures developed in quenched and tempered alloy steels serve to strengthen the matrix without providing preferential carbide fracture paths. In addition to promoting the formation of spheroidal structures certain alloy elements have a secondary effect which serves to increase the strength and ductility of the ferrite by solid solution. These combined effects serve to raise the effective cleavage stress of the aggregate hence result in increased resistance to brittle cracking.

The worst metallurgical case is presented by carbide structures in the form of a semi-continuous network which provide a preferential path for cleavage. In such a case the effective fracture stress is lowered markedly resulting in extremely poor crack resistance.

With the exception of the case of the brittle net-

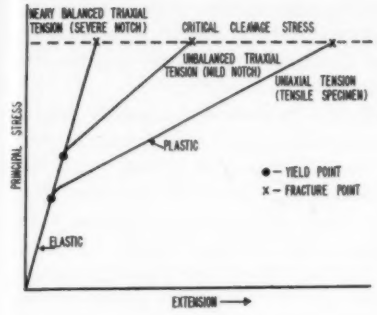


Fig. 5—Effect of triaxial tension in restricting development of deformation.

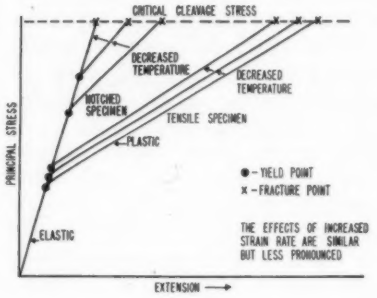


Fig. 6—Effect of decreased temperature and increased strain rate in restricting development of deformation.

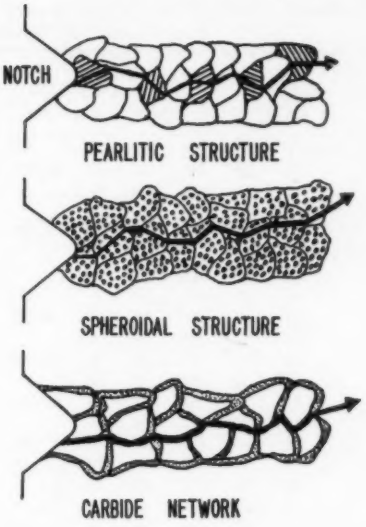


Fig. 7—Micro-features of brittle fracture.

work, the path of brittle fracture is intragranular, i.e., fracture proceeds by the cleavage of individual ferrite grains. Since cleavage of ferrite grains can occur only in definite crystallographic directions the path of fracture is ordinarily changed from grain to grain. Inasmuch as a fraction of the grains are poorly aligned to develop a continuation of cleavage, the path of fracture becomes occasionally difficult. Finer grain size increases the probability of this difficulty hence the fracture stress becomes effectively higher thus providing the necessary conditions for increased resistance to the brittle fracture. It is evident from the above that the intrinsic properties of the steel determines its sensitivity to the unfavorable mechanical factors discussed previously.

Relative Notch Toughness of Cast and Rolled Steels

A reasonably significant comparison of the relative notch toughness of cast and rolled steels cannot be obtained from the available literature. Most of the cast steel data pertain to miscellaneous alloy steels of poorly defined heat treatment; section sizes are rarely stated and test values are given only for a few arbitrary temperatures which do not permit the determination of reliable transition curves. For a valid comparison it was deemed necessary to evolve the required data directly by tests of a sufficient number of steels of comparable type. Such an approach based on direct comparison of cast and forged splits of individual heats posed a severe economic problem; also, the question of laboratory *vs* commercial heats had to be considered in any realistic evaluation. The availability of a large body of data developed by the Naval Research Laboratory[3] and The National Bureau of Standards[4] entailing complete Charpy V transition curves for mild steel ship plates and representing a broad sampling of commercial production over a period of several years, provided a practical start for the desired comparison. These steels for the most part represented the tonnage production variety of rolled structural steels of ¾-in. and 1-in. thickness. There remained the problem of obtaining a similar broad sampling for the tonnage variety of commercial cast steels of equivalent type. This was solved by cooperation of members of the Steel Founders Society of America which provided samples taken from 14 × 14 × 1½-in. plates and 6-in. keel blocks poured from commercial production heats of Class B steels, including all common types of melting practice. In addition, similar plates cast from basic induction heats were made by the Naval Research Laboratory, Table 1.

The evaluations of these steels were based on comparisons of the spread of notch toughness transition curves for the different types, in order to eliminate the uncertainties related to variations in quality within groups. The Charpy V notch toughness of the steels were compared in the following groups:

Class B Cast Steels	Rolled Structural Steels
(1) Normalized	(1) Rimmed, as-rolled
(2) Annealed	(2) Semi-killed, as-rolled
	(3) Fully-killed, as-rolled

It should be noted that the comparisons are specific to the various commercial types *in the common condition of service use.* The Class B steels were fully killed with silicon and aluminum which is the normal practice required for reasons of castability. The fully-killed rolled steels represent a premium product, however.

Any consideration relative to the notch toughness transition curves of rolled steels is complicated by the effect of orientation of the specimen relative to the principal rolling direction. Figure 8 illustrates that a significant effect of orientation is shown only at Charpy "V" transition temperatures which develop more than 10 ft-lb fracture energies. Thus, notch toughness evaluations based on transition criterion of 10 ft-lb are not dependent on orientation effects. It is interesting to note also, that the temperatures range of transition from fully ductile to completely brittle behavior (fracture transition range)

TABLE 1—CHEMICAL ANALYSIS AND TENSILE PROPERTIES CLASS "B" STEELS

	Steel No.	C, %	Mn, %	Si, %	Heat Treatment	TS., psi	Y.S., psi	% EL	% RA
B. IND.	1	.16	.52	.30	N				
	2	.20	.59	.31	N	63,700	27,150	30.0	61.0
	3	.20	.50	.30	N				
	4	.20	.59	.35	A				
	5	.27	.68	.32	N	75,500	30,000	27.5	45.0
	6	.27	.67	.37	A				
	7	.27	.67	.37	N				
	8	.20	.50	.30	A				
	9	.27	.68	.32	A	70,275	41,820	28.3	58.5
	10	.32	.60	.34	A				
	11	.26	.57	.36	N	61,700	25,400	27.9	42.0
	12	.32	.60	.32	N				
	13	.26	.64	.44	N	65,500	31,500	10.8	26.0
	14	.26	.57	.36	A				
	15	.25	.56	.41	A	67,000	35,000	29.0	50.0
	16	.19	.49	.44	N	66,000	29,500	12.0	27.0
	17	.31	.47	.30	N				
	18	.26	.64	.44	A	66,500	35,000	26.5	48.0
	19	.25	.56	.41	N	65,000	31,475	15.0	27.0
	20	.35	.52	.43	N	68,000	37,500	9.8	21.8
	21	.33	.53	.43	N	75,750	31,400	22.0	35.0
	22	.33	.53	.43	A	74,000	32,500	25.0	36.0
	23	.31	.47	.30	A				
	24	.35	.52	.43	A				
B.O.H.	1	.28	.70	.38	A	67,000	36,000	25.0	47.0
	2	.33	.78	.37	A	65,000	35,000	24.0	46.0
	3	.26	.72	.36	A	68,000	35,000	24.0	48.0
	4	.29	.72	.37	A	70,000	37,000	24.0	46.0
	5	.28	.73	.38	N	76,000	43,500	26.0	46.6
	6	.25	.70	.34	N	75,000	40,000	24.0	50.0
	7	.28	.69	.36	N	80,000	40,000	24.0	50.0
	8	.29	.70	.36	N	75,000	40,000	23.0	45.0
A.O.H.	1	.25	.65	.42	N	72,000	38,500	26.0	42.0
	2	.28	.65	.43	N	71,000	38,250	26.0	41.5
	3	.25	.55	.43	A	71,550	37,200	27.3	42.8
	4	.29	.70	.29	N	73,000	36,000	26.0	40.5
	5	.23	.71	.43	N				
	6	.25	.72	.44	N	74,100	36,500	27.5	43.4
	7	.29	.51	.41	N	68,700	38,000	29.0	49.7
	8	.25	.65	.41	A	70,000	34,750	26.5	39.0
	9	.28	.60	.37	A	72,000	36,000	26.0	39.0
B.E.	1	.26	.65	.46	A	73,750	46,500	30.0	53.9
	2	.23	.70	.43	A	72,000	40,500	30.0	53.0
	3	.27	.66	.44	A	75,000	42,500	26.0	39.0
	4	.23	.74	.44	A	70,500	41,500	34.5	51.9
	5	.26	.64	.40	A	77,000	45,400	30.0	50.8
	6	.25	.67	.36	N	73,000	48,000	31.5	51.7
	7	.28	.63	.44	N	77,000	46,500	25.5	49.0
	8	.27	.66	.44	N	77,000	47,500	26.5	45.0
	9	.23	.62	.50	N	76,000	47,000	26.0	46.3
	10	.23	.74	.44	N	72,200	44,750	26.0	37.3
A.E.	1	.22	.55	.36	A	66,700	33,150	30.0	52.3
	2	.27	.59	.43	A				
	3	.21	.54	.40	A				
	4	.25	.60	.43	A				
	5	.24	.70	.40	A	72,250	40,250	30.5	50.8
	6	.22	.51	.40	N				
	7	.18	.47	.35	N				
	8	.25	.60	.43	N	71,500	43,050	30.0	52.3
	9	.25	.73	.34	N	75,800	51,500	31.0	58.5

remains unchanged by orientation changes. The importance of these observations lies in the fact that the 10 ft-lb range which represents the last traces of ductile behavior of "V" specimens has been established[3,4] to be the criterion of significance to the brittle fracture of common engineering structures. By considering the spread of transition temperatures based on 10 ft-lb criterion the average quality of a specific group of rolled steels may be defined without consideration of orientation effects. For a more detailed discussion of orientation effects as related to brittle fracture and Charpy V transition criteria the reader is directed to reference (3).

It is now conventional to present Charpy V transition curves of rolled structural steels in terms of specimens oriented in the direction of rolling with the notch in the thickness direction (L.H. specimen Fig. 8). All rolled steel data presented pertain to L.H. specimens in keeping with this convention. The individual curves have been determined using approximately 20 specimens and may thus be considered to be reliable. In general the reader's attention is directed to the distribution of the groups of curves rather than single curves. Particular attention should also be directed to the temperature spread of the 10 ft-lb energy level in view of the significance of this level to the problem of brittle fracture.

Figures 9, 10 and 11 present group curves for fully killed, semi-killed and rimmed structural steels. The 10 ft-lb and 70 F lines on the various charts provide a ready reference of band position. The relative quality of these various types is illustrated more clearly by the summary band curves of Fig. 12. It is noted that there is considerably overlapping of the bands, particularly for the semi-killed and rimmed grades. The effect of carbon content which is considered an important variable in specification of structural steels is illustrated in the summary band curves of Fig. 13. Despite the overlapping due to grain size and other incidental variables it is observed that higher notch toughness is favored by lower carbon contents.

Figures 14, 15, 16, 17 and 18 present group curves for normalized and annealed Class B cast steels, separated with respect to melting practice. It is observed that no significant differences are developed due to heat treatment; Fig. 19 illustrates that the bands developed by the two heat treatments essentially overlap. Figure 20 presents summary bands illustrating the effect of melting practice. It is apparent from the overlap of these bands that melting practice has relatively little effect on notch toughness properties.

Figure 21 provides a comparison of the summary bands for all of the rolled and cast steels. The bands are essentially superimposed indicating that the range of quality is the same. Comparison on the basis of the frequency distribution of the 10 ft-lb transition temperature, Fig. 22, indicated that the average quality of the Class B steels is slightly better than that of the rolled mild steels. This is probably due to the higher manganese content used for the Class B steels. However, the comparison is valid inasmuch as it is based on the properties of the two materials as used in service. In fact, the distribution curve of the rolled steels would be shifted to higher temperatures if the premium-grade, fully-killed steels were eliminated from the grouping.

Figures 23 and 24 present data for various types of alloy cast steels (Table 2) representing cast plates and keel blocks poured, for the most part, from production heats and heat treated in the same batch as the castings poured from these heats. The wide variety of compositions, heat treatment and other fac-

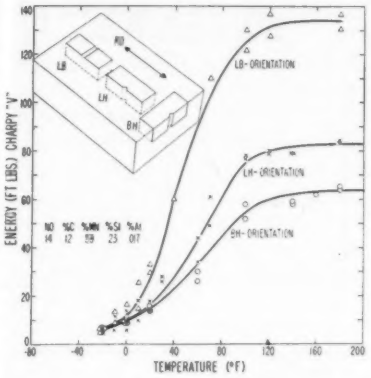


Fig. 8—Effect of Charpy V transition curves of specimen orientation relative to rolling direction.

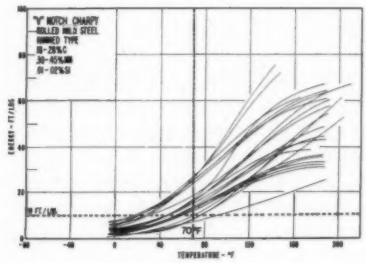


Fig. 9

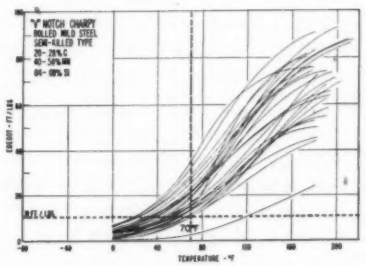


Fig. 10

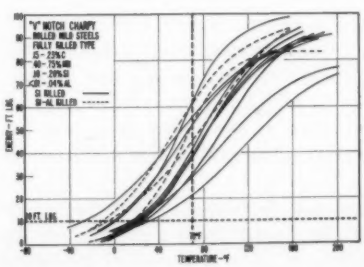


Fig. 11

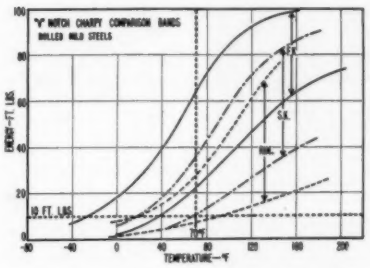


Fig. 12—Quality range of rolled mild steels of various deoxidation practices.

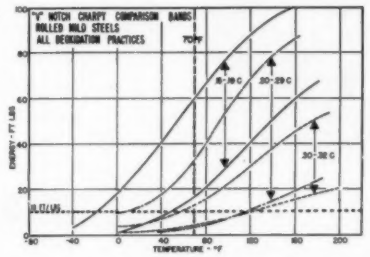


Fig. 13—Quality range of rolled mild steels of various levels of carbon content.

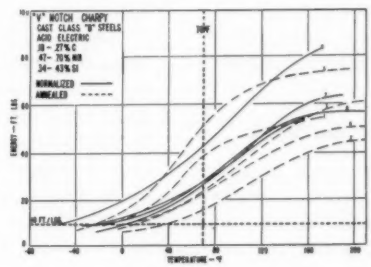


Fig. 14

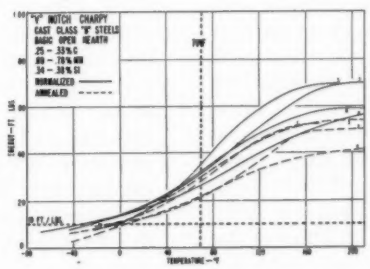


Fig. 15

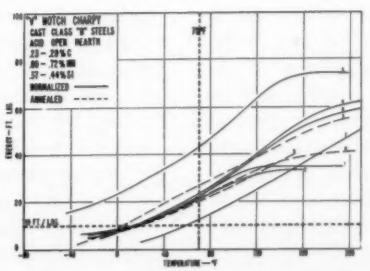


Fig. 16

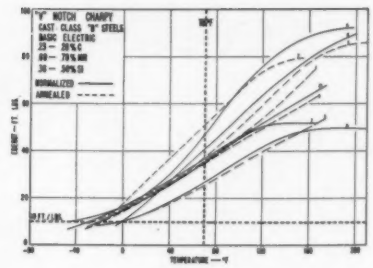


Fig. 17

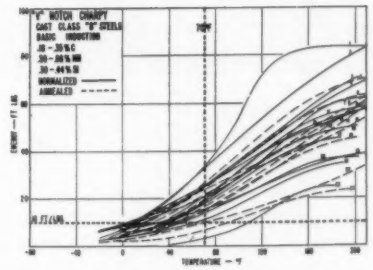


Fig. 18

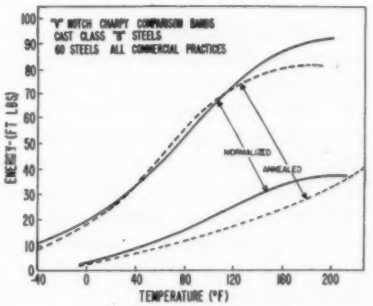


Fig. 19 — Quality range of heat treated Class B cast steels.

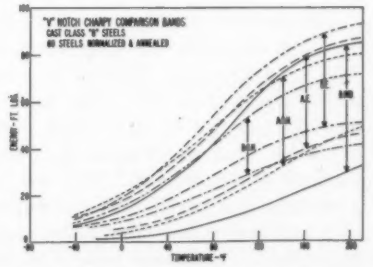


Fig. 20—Quality range of Class B cast steels of various melting practices.

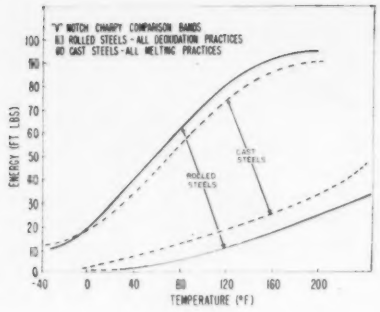


Fig. 21—Quality range of Class B cast steels compared to rolled mild steels.

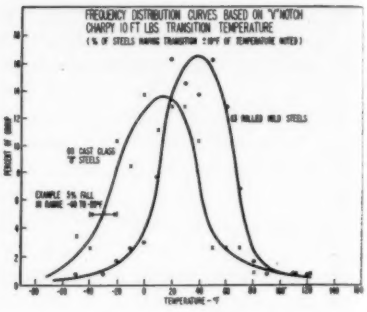


Fig. 22—Comparison of quality distribution of Class B cast steels and rolled mild steels.

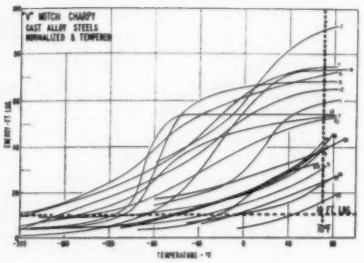


Fig. 23

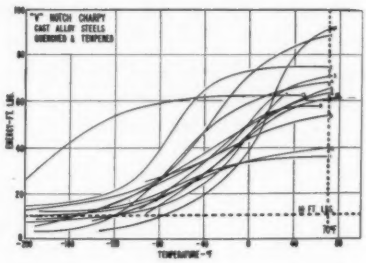


Fig. 24

tors prevent direct comparison with rolled steels. These data are presented primarily to illustrate the wide variety of notch toughness properties which may be expected of alloy cast steels. The wide variations of notch toughness transition present a self-evident case for the use of notch toughness tests to evaluate the properties of the final product.

Performance Evaluation of Cast and Rolled Steels

As described in the introductory sections of this paper the conditions of stress developed in service vary from the favorable extreme of simple tension to the unfavorable extreme of triaxial tension. For purposes of this report the condition of simple tension may be considered to apply to structures entailing only smooth surfaces and well-rounded corners; conditions of triaxial tension are considered to exist if the structure contains sharp notches or cracks. It was stated that structures which entail smooth surfaces operate essentially as tensile specimens while structures which contain sharp defects operate as notched specimens. It is appreciated that a wide range of conditions varying from simple tension to extreme triaxial tension may be present in structures. As a practical approach to the problem of the performance evaluation of cast and rolled steels, only the two limiting conditions were investigated.

The method used for this evaluation was developed initially to study the performance of weldments.[3,5] The method entails bulging $14 \times 14 \times \frac{3}{4}$-in. test plates to the point of failure. The high energy required to develop bulging in such thick plates is obtained by explosive loading. Figure 25 illustrates the general features of the method. The test plate is placed over a circular die cavity and the explosive is positioned at a preset distance above the plate by means of an expendable cardboard box. The intense gas pressure produced by the explosion serves the dual purpose of clamping the plate to the shoulders of the die and forcing the development of a bulge in the portion of the plate which is not supported by the die. Tests are conducted at any desired temperature by heating or refrigerating the plates in controlled temperature boxes prior to positioning on the die.

The performance of the test steels under simple stress conditions is evaluated by bulging plates having smooth machined surfaces. If necessary, repeat shots are used to develop failure; the temperature is re-established between shots by returning to the controlled temperature boxes. The performance of the steels under triaxial stress conditions is evaluated by bulging plates which contain a sharp crack. For this test a single, low energy shot is used. These two types of tests are termed "Bulge Test" and "Crack Starter Test" respectively—the significance of these terms will become evident with data presentation.

Bulge Tests—The "smooth surface" or Bulge Test develops essentially a simple tension condition with the tension acting equally in all directions (commonly termed "biaxial tension"). In keeping with this type of stress condition it should be expected that extensive deformation should precede fracture, as predicted by the tensile specimens. The deep bulge developed by a representative Class B steel, shown in Fig. 26, illustrates that such is the case. It is observed, however, that the fracture which results is of a brittle type despite the high ductility demonstrated by the extensive deformation which preceded fracture. This behavior is not anomalous, it merely indicates high pre-crack ductility in the presence of simple stress systems and low resistance to brittle fracture following the development of triaxial stress conditions produced by the initial crack at the point of failure. It will be shown that equivalent rolled steels behave in a similar manner.

The level of pre-crack deformation developed in the bulge tests may be represented conveniently in terms of the reduction of thickness which preceded failure. Figure 27 illustrates the amount of bulging that corresponds to a wide range of thickness reduc-

tions. Figure 28 presents representative plots of reduction of thickness at fracture for several Class B steels. In general, high deformations are obtained in the range of common service temperatures, demonstrating the high ductility which is inherent to cast steels performing under favorable stress conditions. The various curves shown in Fig. 28 were selected to illustrate the tendencies of certain heats to show a gradual and consistent decrease in deformation level with lowered temperatures while other heats show extensive scatter; all test points plotted on these curves represent radiographically sound plates. The scatter which is characteristic of certain heats is ascribed to microscopic faults not defined by radiographic methods. This particular question requires further detailed investigation.

Figure 29 presents a summary plot of all bulge tests conducted on Class B steels. A number of the test points represent plates which contained various types of common casting defects. These included: center-line shrinkage, small pockets of isolated shrinkage exposed to the surface by machining, sand pockets, small scabs and pinholes. In general, these defects provided the starting point of final failure, however, the effect on the level of deformation developed prior to failure was not sufficiently great to overshadow the chance scatter shown by radiographically sound plates. Figure 30 provides examples which are related to specific points on the summary plot of Fig. 29. It may be concluded that the effective sharpness of common casting defects (with the exceptions of hot tears which were not investigated) is not sufficiently severe to reduce pre-fracture deformation to low values. The significance of this conclusion will be more apparent following the presentation of the crack-starter test data.

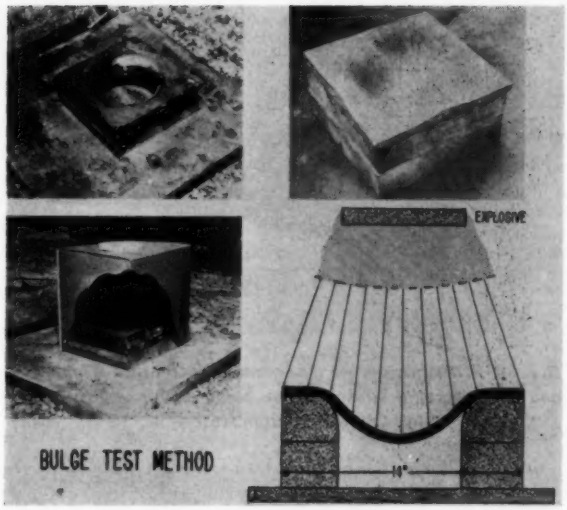


Fig. 25

Figure 31 presents representative Bulge Test data for a cast alloy steel in a quenched and tempered condition. The performance band of the Class B steels

Table 2—Chemical Analysis of Cast Alloy Steels (Normalized)

Steel No.	C, %	Mn, %	Si, %	Cr, %	Ni, %	Mo, %	Heat Treatment		
1	.22	.71	.54	.13	2.22		1650 (2) AC		
2	.18	.78	.37	.30	3.36	.14	1750 (2) AC	1550 (2) AC	1100 (4) AC
4	.11+	.65	.35		2.85		1650 AC	1250-T	
5	.11+	.65	.35		2.85		1650 AC	1275-T	
6	.11+	.65	.35		2.85		N — N	1200-T	
7	.11+	.65	.36		3.45		N — N	1200-T	
8	.12+	.65	.35		1.00		N — N	1200-T	
9	.14+	.65	.35		2.00		N — N	1200-T	
10	.15+	.58	.30		2.90		N — N	1200-T	
11	.13+	.65	.35		3.50		N — N	1200-T	
12	.10+	.65	.35		4.00		N — N	1200-T	
13	.11+	.64	.34		.50		N — N	1200-T	
14	.30	.71	.29	.82		.22	1650 AC	1550 AC	1275 AC
15	.16	.66	.33	.95		.53	1950 (8) AC	1200 (10) FC	
16	.25	1.34	.36			.14	1650 (2) AC	500 (4) AC	
17	.33	1.32	.37			.17	1650 (2) AC	1125 (4) AC	
18	.25	1.42	.42	.29				1200 W.Q.	
19	.30+	1.18				.30	1650 AC	1200-T	
20	.34+	1.10				.18	1700 AC	1200 T	
21	.25	1.50	.40			.30	1700 (2) AC	1200 (4) WC	
			Chemical Analysis of Cast Alloy Steels (Quenched & Tempered)						
1	.25	1.42	.42				1650 WQ	1200 WQ	
2	.30+	.71	.29	.82		.22	1650 AC	1550 WQ	1275-T
3	.34+	1.10				.18	1700 AC	1600 WQ	1200-T
4	.12+				1.03		1600 WQ		1200-T
5	.25	1.42	.42				1650 AC	1550 WQ	1275-T
6	.35	1.16	.41	.24	.25	.15	1650 WQ	1300 (2) AC	
7	.29	1.44	.38	.13	.12	.34	1675 (2) WQ	1200 (2) AC	
8	.30+	1.16					1700 AC	1600 WQ	1200 AC
9	.13+				3.50		1525 WQ		1200-T
10	.26+	.73	.43	.69	1.10		1650 AC	1550 WQ	1275-T
11	.26+	.62	.31	.10	3.36		1650 AC	1550 WQ	1250-T
12	.25	1.50	.40			.30	1700 (2) WQ	1100 (4) WQ	
13	.25	.25	.19	1.50	3.00	.30	1650 (2) WQ	1200 (4) WQ	
14	.16	.52	.28	1.06		.38	1700 (2) WQ	1200 (4) WQ	

(+) Obtained from various literature sources.



Fig. 26—Illustrating the extensive deformation which precedes fracture of Class B cast steel explosion bulged at ambient temperatures.

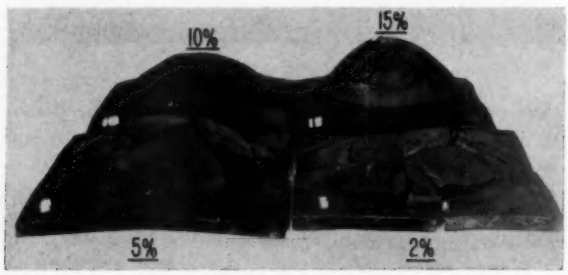


Fig. 27—Relation of thickness reduction to bulge size.

and the respective tensile properties and Charpy V transition curves are also indicated. Figure 32 illustrates that the fracture developed by the alloy steel at normal service temperatures is of a ductile shear type rather than brittle cleavage shown by the Class B steel at the same temperatures, Fig. 26. The higher notch toughness of the alloy steel explains the change in the mode of fracture while the higher tensile ductility explains the increased level of deformation which is developed.

This last conclusion is somewhat oversimplified for notch toughness enters into tensile ductility tests (bulge and tensile specimens) by virtue of the relative sensitivity of the steels to micro-scale imperfections which are associated with cast structures. With higher notch toughness these imperfections are less critical in controlling tensile ductility. The effect is particularly evident at low temperatures for which the notch toughness differential of the two steels is greater. The high ductility shown at low temperatures by the alloy steel thus reflects, in part, a lesser sensitivity to the presence of imperfections.

On the basis of these considerations it should be expected that a rolled steel should develop greater bulge deformation prior to fracture than as a cast steel of equivalent notch toughness, inasmuch as the hot working operation consolidates many of the small imperfections associated with cast structures. That such is the case is illustrated by the upper portion of Fig. 33 which compares the level of deformation developed by a typical rolled mild steel prior to fracture to that of Class B steels. It should be noted that, as predicted by the notch toughness data, the fracture appearance of the two steels is the same, i.e., both show brittle fracture at temperatures below 80 F to 100 F following the development of a separation.

The above comparison based on deformation limits, however, is not completely realistic to the problem of the performance of castings *vs* weldments, the lat-

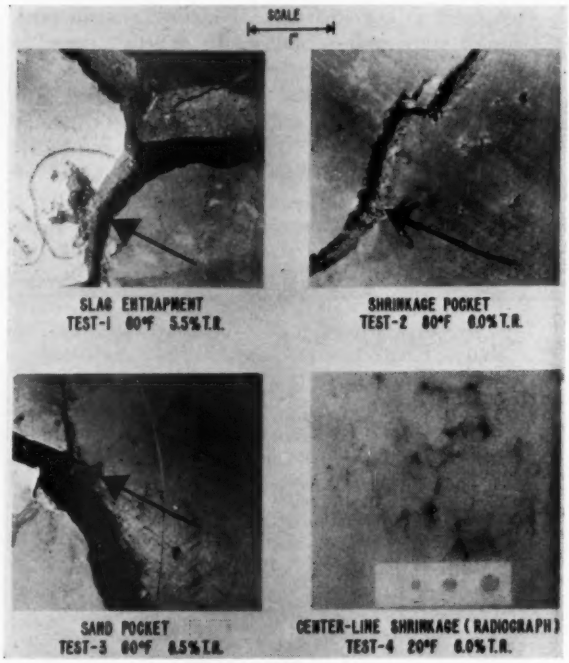


Fig. 30—Representative defects which did not result in a significant decrease of bulge deformation (Test numbers refer to coded points of Fig. 29).

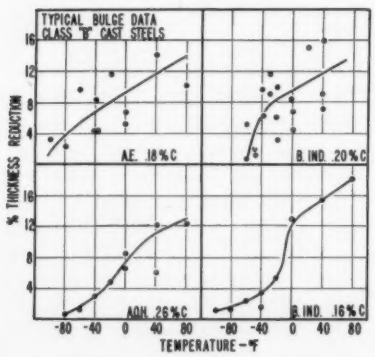


Fig. 28—Effect of test temperature on extent of bulge deformation developed prior to fracture, illustrating characteristics of various Class B cast steels.

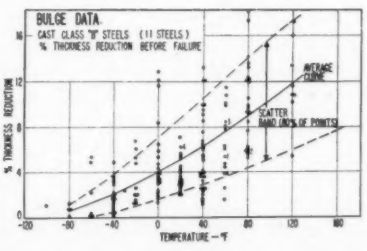
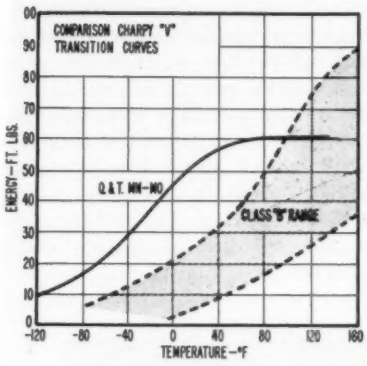


Fig. 29—Summary plot of all explosion bulge tests of Class B cast steels (each point represents a separate test plate; the coded points represent plates with the casting flaws shown in Fig. 30).



COMPARATIVE TENSILE DATA

	T.S.	Y.S.	EL.%	R.A.%
CLASS "B" (AVERAGE) -	75,000	42,000	24	45
Q&T MN-MO -	95,000	75,000	26	60

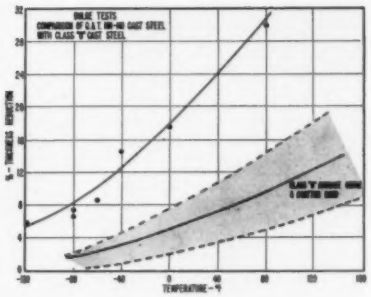


Fig. 31—Illustrating the greater bulge deformation developed prior to fracture of quenched and tempered cast alloy steels in comparison to Class B cast steels; also, comparison of notch toughness and tensile properties.

ter being the usual form of rolled steel structural components. The lower portion of Fig. 33 illustrates that welding impairs the performance of the rolled steel but has no effect on the performance of the cast steel. In order to understand the reason for this behavior it should be noted that all fractures of the rolled steel weldments initiated in the weld itself, while all fractures of cast steel weldments initiated in the casting.

The cause for the detrimental effect of welding on the performance of rolled steels has been reported previously by the authors.[6] Briefly, the weld, which is actually a form of casting, is subject to the same imperfections which limit the amount of deformation developed by cast steels. Accordingly, the imperfection system of the weld serves to decrease the ability of the rolled steel plate weldment to develop extensive deformation. The start of all fractures in the weld is indirect proof of this contention. The E6010 weld, however, has a higher notch toughness than the cast steel[6] and therefore refuses to start failure when deforming in conjunction with cast steel having equal imperfections. All failures are therefore restricted to the cast steel with the result that the performance of

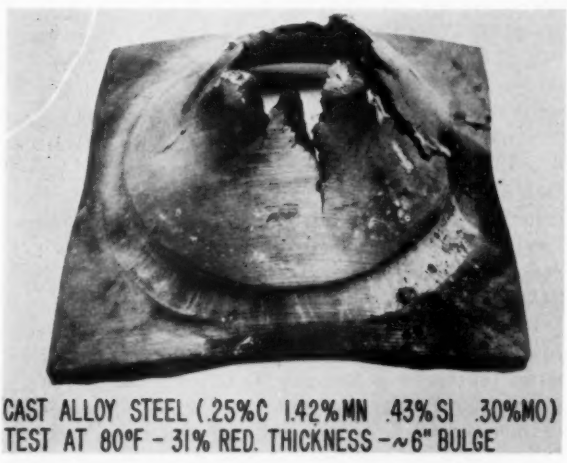


Fig. 32—Illustrating ductile shear fracture of quench and tempered cast alloy steel. Compare with Class B cast steel showing brittle fracture at same test temperature, Fig. 26.

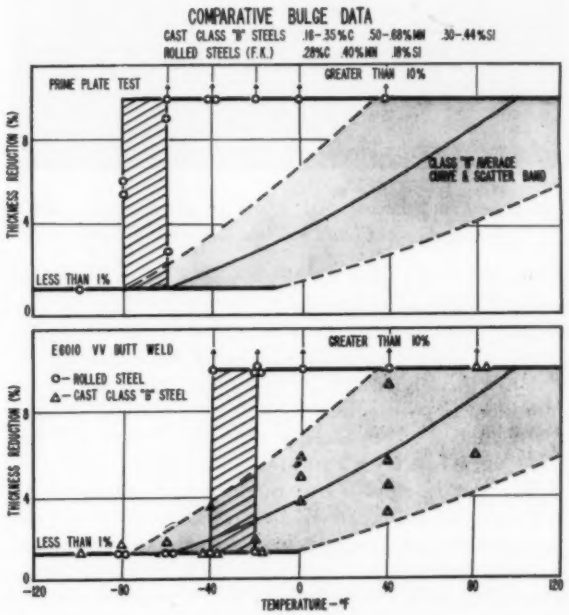


Fig. 33—Comparison of cast and rolled steel bulge deformation transitions (upper graph) and also of E6010 weldments of rolled and cast steel plates (lower graph).

cast Class B steels is not affected by welding.

It may be concluded that for service in structures which are designed such that appreciable plastic deformation is not developed, both rolled and cast steels are capable of performing satisfactorily at the lowest service temperatures. Unless the designer is concerned with service entailing extensive plastic deformation (specific only to certain military applications) it is of relatively little importance whether 5 or 10 per cent deformation is required to develop failure. The important fact is that as long as the steels are capable at least of moderate deformation, their use in structures which are designed to operate under essentially elastic loading and are not subject to triaxial stress conditions, is perfectly safe. In this sense the differences in bulge deformation observed between rolled and cast steels are not significant to service conditions of common engineering structures.

Crack Propagation Tests — The relative resistance of rolled and cast steels to the *propagation of brittle cracks* is of great significance to the performance of common engineering structures. If the predictions of the Charpy specimen data presented in the previous sections are correct, it should be concluded that rolled



Fig. 34—Crack-starter test plate showing brittle, hard-surfacing weld bead used to initiate cracking.

structural steels offer no advantage over Class B cast steels insofar as resistance to brittle fracture is concerned. This conclusion is based on the fact that the range of notch-toughness quality of the two commercial products is essentially the same. Such a conclusion may not be accepted, however, without formal proof that a given Charpy transition curve has the same significance for cast and rolled steels.

In order to resolve this question it is necessary to test cast and rolled steels under triaxial stress conditions which would cause brittle failure if the metal is susceptible to such behavior. It is generally recognized that the most severe triaxial stress conditions to be met in service are developed by sharp cleavage cracks. If a metal resists brittle fracture to the lowest service temperature in the presence of such cracks, it may be deemed to be immune to brittle fracture. The crack starter test was developed to investigate the relative resistance to brittle fracure of various types of rolled structural steels in the presence of a severe crack. The method has been applied to investigations related to the problem of the brittle fracture of ship steels and demonstrated to predict correctly the temperature range and conditions of ship fractures.[3]

The method entails placing a brittle (high carbon steel) weld, *of the type which is ordinarily used for hard surfacing,* on the surface of the test plate, Fig. 34. On loading to the yield point of the plate the brittle weld develops a cleavage crack and thus presents the test plate with a triaxial stress condition of the utmost severity. The extent and nature of the crack which propagates into the test piece depends on the inherent resistance of the steel to brittle fracture. The test is conducted in the manner of the Bulge Tests described previously except that only a single explosion is used and the energy delivered is adjusted to develop a minimum of deformation (approximately 1 in. deep bulge if fracture does not result).

It should be recognized that the high rate of loading produced by the explosion does not result in the development of brittle cracks for steels which are not so disposed at normal rates of loading. As discussed previously brittle fractures propagate at high speeds by the release of elastic strain energy. For example, a brittle fracture developed in a Charpy specimen does not propagate at the speed of movement of the hammer (18 ft sec) but at speeds which may approach 8000 ft/sec. These high speeds are developed only in completely brittle fractures; fractures which show minute, but important, amounts of shearing at surfaces (such as the small shear lips developed along the sides of otherwise brittle Charpy specimens breaks)

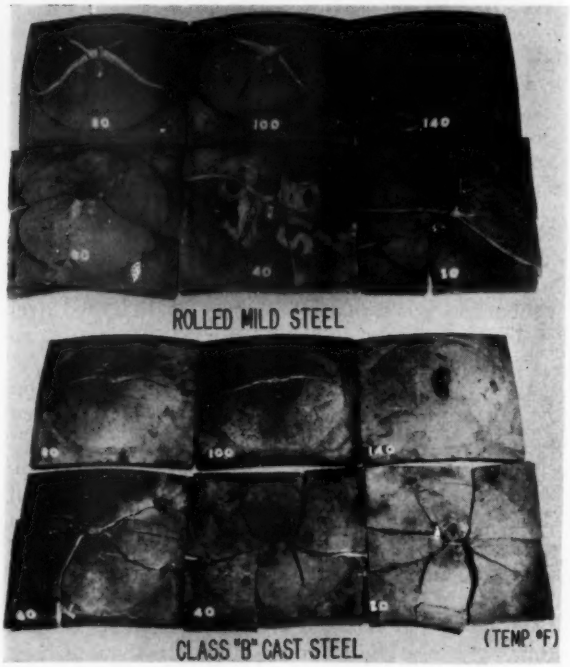


Fig. 35—Crack starter tests of rolled and cast steels of equal Charpy V notch toughness illustrating similarity of fracture characteristics (Numbers refer to test temperature, F).

may travel at considerably lower speeds, possibly as "low" as 1000 ft/sec.

The importance of these various considerations lies in the fact that if sufficiently high elastic stress is built up in the structure prior to the start of cracking the crack will move forward at its own characteristic speed. In this sense brittle cracks may be termed "self-propagating," i.e., not dependent on rates of load application, as illustrated for the case of Charpy specimens. For a detailed discussion of the theory of propagation of brittle cracks in relation to variations in rates of loading consult reference (3).

Figure 35 illustrates the result of crack starter tests conducted over a series of temperatures. The upper series represents a commercial rolled structural steel of average quality and the lower series represents a commercial Class B cast steel also of average quality. It is apparent from the nature and extent of cracking at the various temperatures that the performance of the two steels is closely similar.

It may also be observed from Fig. 35 that the test

plates remained essentially flat when tested at 20 F. At higher temperatures definite bulging was developed prior to fracturing. This behavior is significant in that it indicates that at 20 F the steels fractured in a brittle manner immediately on the development of the sharp cleavage crack. At temperatures above 20 F the presence of the sharp flaw developed by the brittle weld was not sufficiently critical to prevent continued deformation prior to cracking. Accordingly, it is indicated that at service temperatures of 20 F and lower, stress conditions of high triaxiality such as developed by sharp cleavage cracks are sufficiently critical to result in brittle failure even though the steel is used in a structure designed not to develop deformation.

Brittle Fracture at High Temperature

The results of tests at higher temperatures are of significance primarily in indicating the highest temperatures at which brittle fracture is still possible provided deformation is developed in the structure containing such a severe flaw. Deformation resulting from poor design or from accidental overloads therefore may be expected to lead to brittle fractures at temperatures which would ordinarily ensure safe performance if stresses were kept to within elastic limits. There is, however, an upper temperature above which it is not possible to develop brittle fracture irrespective of presence of severe flaws or of the development of extensive deformation. By proper alloying and heat treatment it is possible to place this temperature of absolute safety below the lowest operating temperature. This will be demonstrated in the case of crack propagation tests of alloy steels.

The relation of a typical Class B steel fracture series to the Charpy V transition curve is illustrated in Fig. 36. It is observed that brittle fracture is not developed at temperatures which are related to the upper *shelf* portion of the Charpy curve. Fractures which require extensive deformation for initiation and propagate with difficulty, as observed from the limited extent of fracture, are developed at intermediate and high portions of the curve. Fractures which are highly "brittle" as denoted by the complete break-up of the test plate and flatness of the broken pieces, are developed at temperatures related to the lower portion of the curve. The latter type represent the highly brittle fractures which are developed in failure of common engineering structures.

Figure 37 illustrates the result of direct comparison of cast and rolled steels obtained by welding the two steels together so as to permit the development of the sharp cleavage crack simultaneously in the two steels. The joining was accomplished by means of an E6010 butt weld and the crack-starting weld was deposited so as to overlap the joining weld, thus ensuring transmittal of the crack directly to the test materials. It may be observed that the cracking characteristics are in agreement with the predictions of the Charpy V specimen (equal 10 ft-lb Charpy V temperature signifying equal resistance to brittle fracture). This series also demonstrates that the energy absorbed in the fracture of the Charpy specimens at temperatures of ductile behavior (the upper

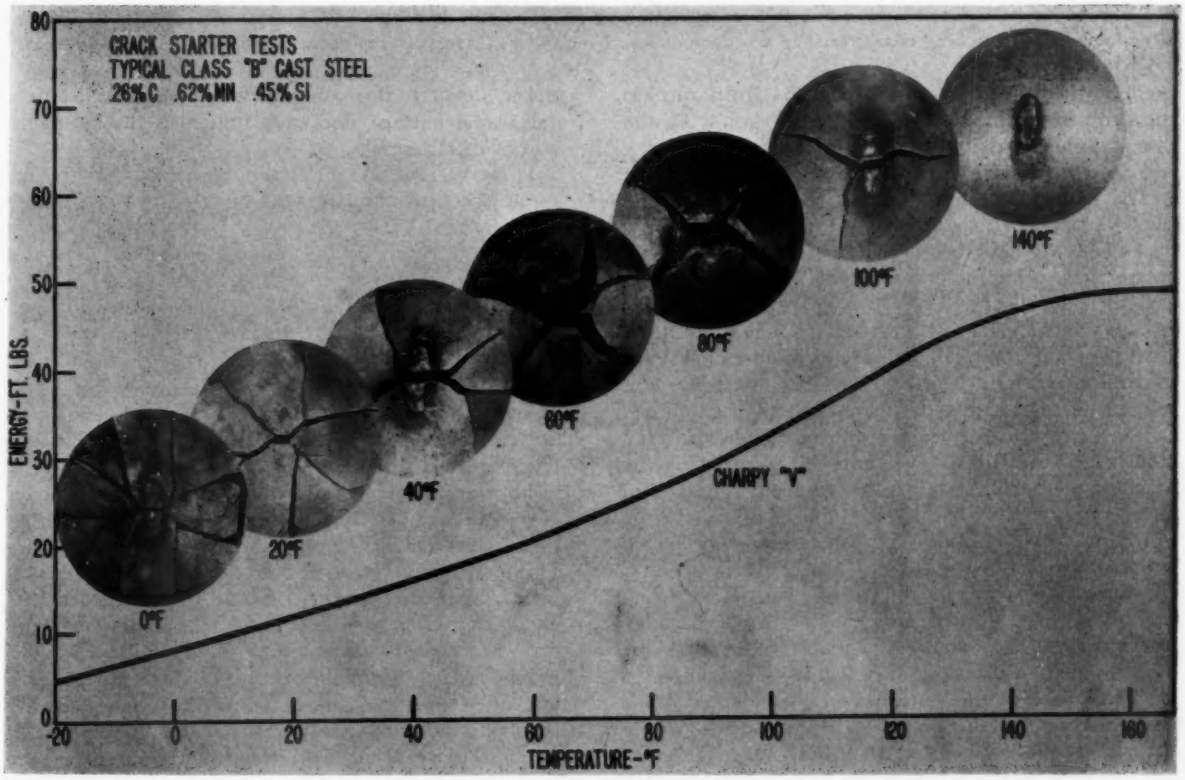


Fig. 36—Correlation of fracture characteristics of Class B cast steel with Charpy V transition data.

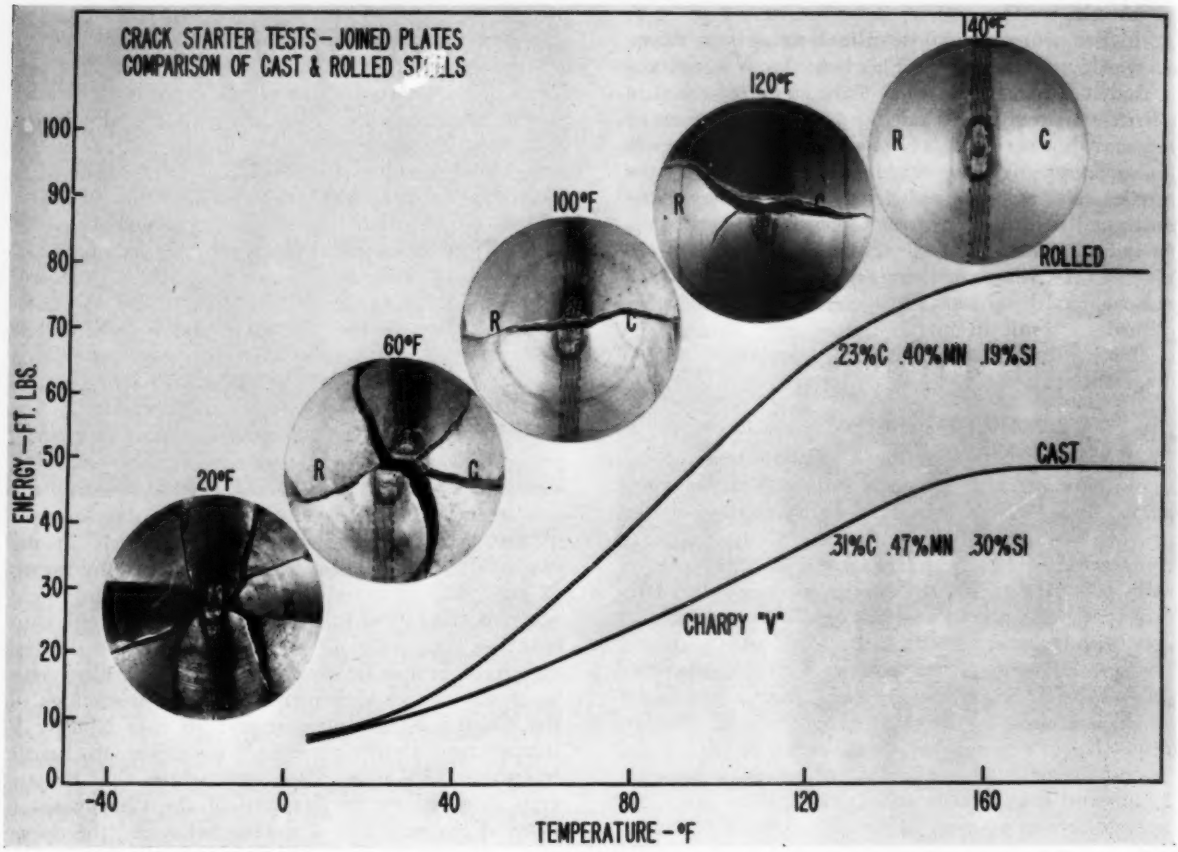


Fig. 37—Illustration result of simultaneous crack initiation in rolled (R) and cast (C) steels welded to form a single test plate. Equal cracking characteristics are demonstrated for the two steels having equal Charpy V 10 ft-lb transitions.

shelf of the transition curve) has no significance to the problem of brittle fracture. This should be the case, inasmuch, as these values represent the relative resistance to shear fracture.

The relative fracture characteristics of Class B cast steels of various carbon levels are illustrated in Figs. 38 and 39. It is observed that there is a gradual increase in brittle fracture tendencies with increasing carbon content.

The relative effects of a proper quench and temper treatment as compared to normalizing treatment for cast alloy steels is illustrated in Fig. 40. The test plates were heat treated separately and then joined in the manner described previously. The close relationship to the Charpy V curves is indicated by the cracking characteristics of the respective halves of the composite test pieces.

All of the tests of the cast steels were conducted on plates which were cast to 1½-in. thickness and machined to ¾-in. thickness by removal of equal amounts from the sides. In order to resolve the question of possible differences in crack propagation due to the presence of surface metal, tests were conducted on two sets of plates from the same heat; one set was machined in the usual manner while the comparison set was machined from one side only, and the crack starter weld was placed on the as-cast surface. Figure 41 illustrates that the performance of the two sets was exactly the same. It may be concluded that the as-cast surface does not influence the propagation of brittle cracks.

These various test series provide proof that the inherent fracture characteristics of cast and rolled steels are predicted by the Charpy V tests without distinction as to type. It is also demonstrated that the Charpy V transition criteria of significance to fractures of high brittleness (which are characteristic of structural failures) are related to the low energy portion of the transition curve.

Drop Weight Tests—It is realized that explosion tests are not practical for general use. In an attempt to develop a generally practical method of evaluating the resistance to fracture *of the highly brittle type* a technique was evolved by which the specimen shown in Fig. 42 is impact loaded by means of a drop weight. This specimen is identical to the crack-starter plate specimen except for the width dimension. The specimen is supported at its ends and the weight impacts on the center of the span, Fig. 43. The height of the drop is adjusted so that the tension side of the specimen develops a small amount of deformation if a fracture does not occur. Inasmuch as the brittle weld cracks at the yield point of the metal, the refusal of the test piece to develop fracture signifies that the metal continued to deform in the presence of a cleavage crack. The weight-drop combination used in the

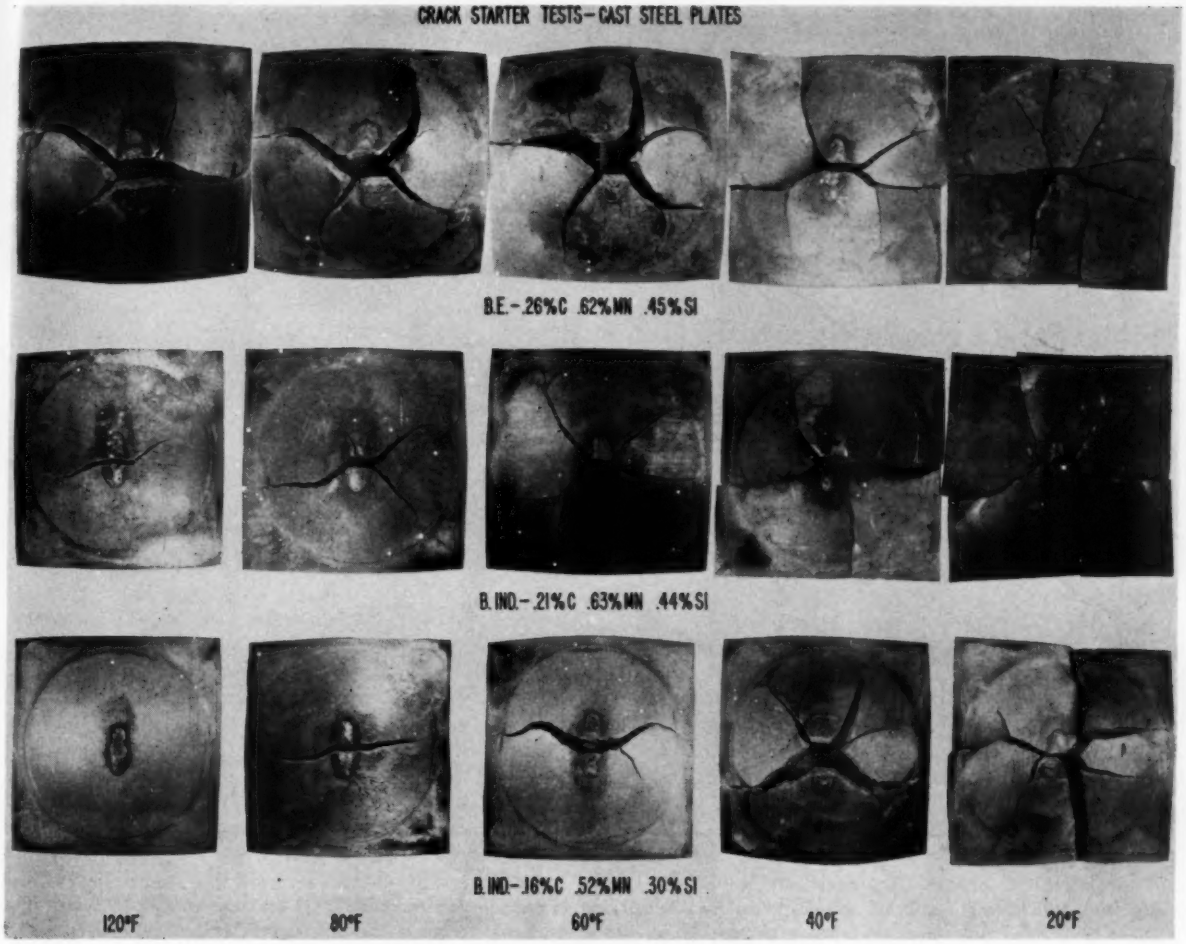

CRACK STARTER TESTS–CAST STEEL PLATES
B.E.–.26%C .62%MN .45%SI
B. IND.–.21%C .63%MN .44%SI
B. IND.–.16%C .52%MN .30%SI
120°F
80°F
60°F
40°F
20°F

Fig. 38

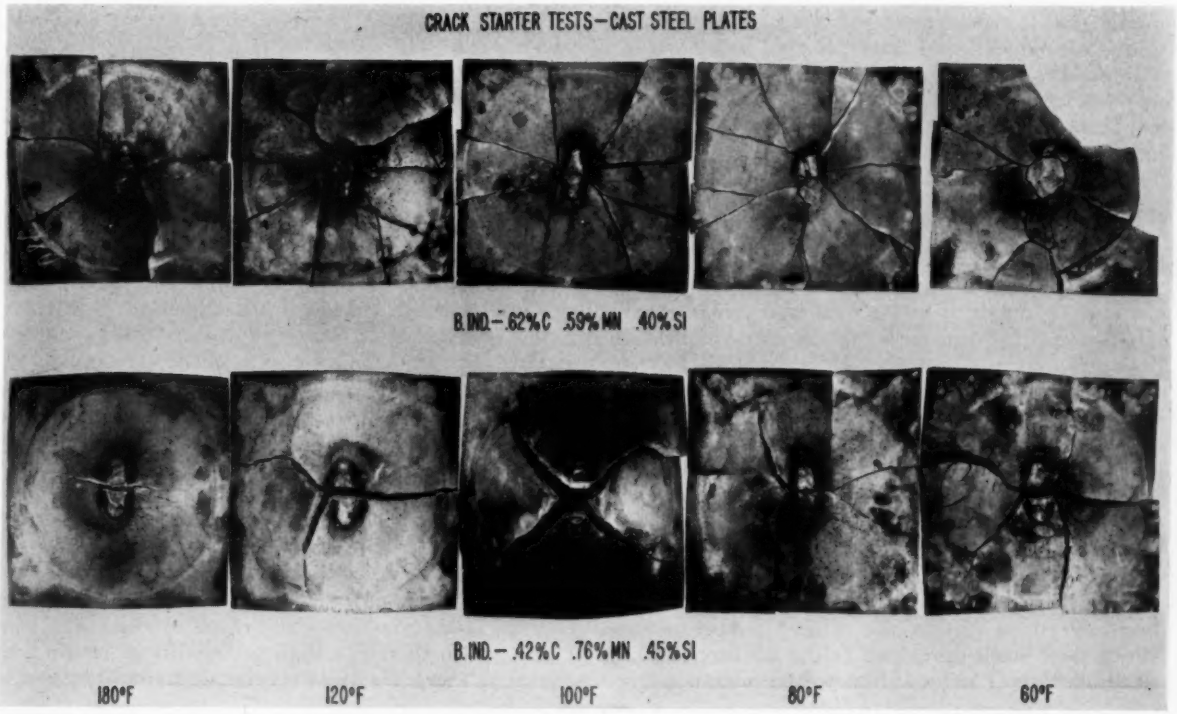

CRACK STARTER TESTS–CAST STEEL PLATES
B. IND.–.62%C .59%MN .40%SI
B. IND.–.42%C .76%MN .45%SI
180°F
120°F
100°F
80°F
60°F

Fig. 39

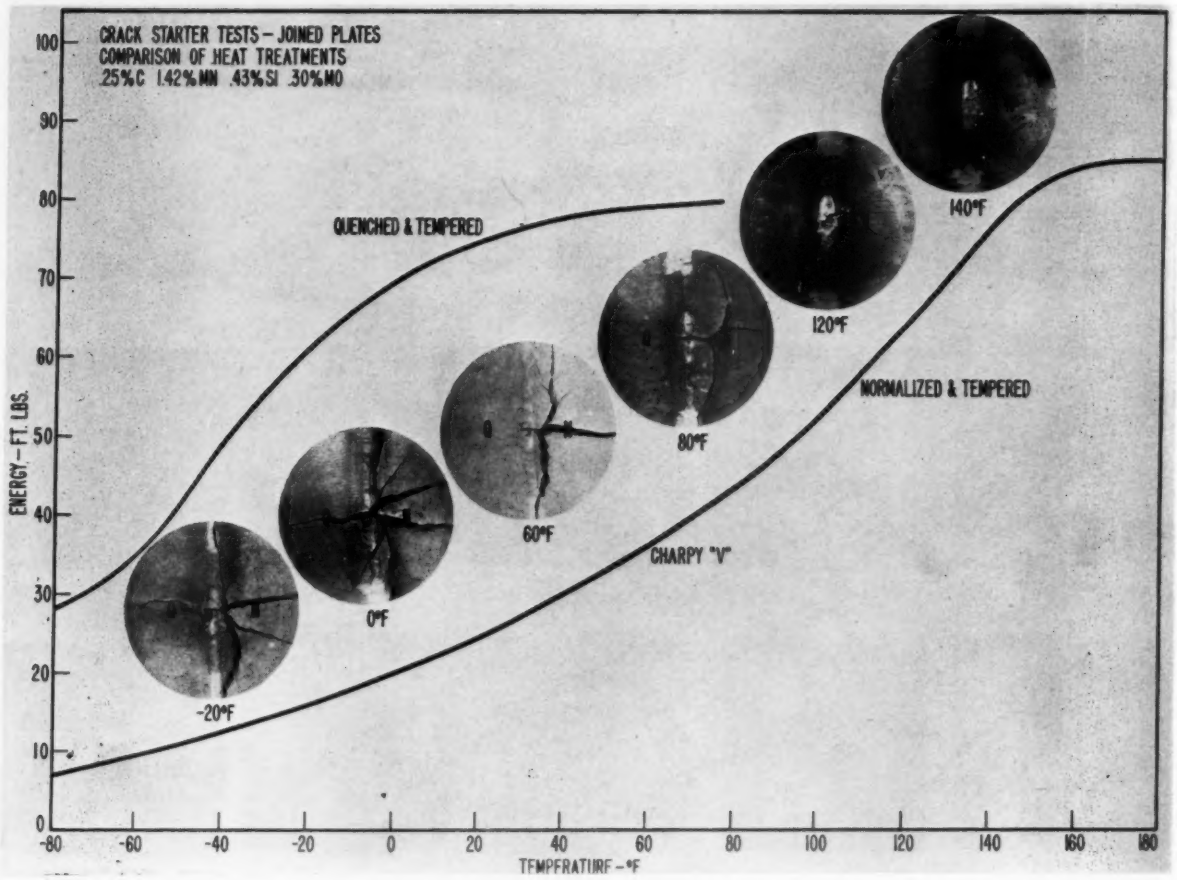


Fig. 40—Illustrating result of simultaneous crack initiation in quench and tempered (Q) and normalized (N) cast alloy steels from same heat, welded to form a single plate. Greater resistance to brittle fracture of quenched and tempered steel predicted by Charpy V transition curves.

tests to be described results in a bend angle of 10 degrees. In the event fracture occurs, it is observed that the broken halves are perfectly flat, indicating that fracture occurred immediately following the development of the cleavage crack in the weld. This behavior signifies that the metal was not capable of developing deformation in the presence of a cleavage crack.

Figure 44 shows typical "break" and "no break" specimens illustrating the principal features described above. The results obtained with this specimen are the same as obtained in the explosion test in that it develops a "flat-break" (no bend) at the same temperature that the explosion test develops a "flat-break" (no bulge). At temperatures above the flat-break temperature of the explosion test the drop weight test bends slightly but does not break. The drop weight method is not suitable for the evaluation of crack propagation characteristics at temperatures which require appreciable plastic deformation prior to cracking.

The drop weight test has been applied to investigating the performance of rolled mild steels and has been shown to predict the critical temperature at which these steels developed brittle fractures leading to the failure of welded ships.[3] The correspondence is not accidental since the test synthesizes the elements which promote brittle failures of structures; (1) sharp cracks; (2) low temperatures and (3) small amounts of deformation at points of constraint. Thus, as in the case of structures, a sharp crack, which is the potential source of failure, remains ineffective at temperatures at which the metal retains ability to deform; however, with lowered temperature this ability is lost and the crack is triggered into a brittle fracture. Extensive studies of the ship fracture plates by the N.B.S.[4] demonstrated that the plates which started fracture (in practically all cases a visible flaw was apparent at the source) developed 3-12 ft-lb Charpy V at the temperature of fracture. These data are of basic significance and represent the only detailed information available concerning accurately established conditions of brittle failure. Drop weight test for ship plate steels of the same type indicated that brittle fracture occurred in the temperature range of 3-14 ft-lb Charpy V. These data have been interpreted to signify that the drop weight test conditions are equivalent to the service conditions which lead to brittle fracture of structures and also that the Charpy V 10 ft-lb temperature represents a critical level below which there is a high probability of brittle fracture *if sharp cracks or similar defects are present.*

In view of this background of information, drop weight tests were conducted on Class B cast steels for the purpose of obtaining a similar correlation with the Charpy V transition curve. Figure 45 illustrates a typical test series from which it may be observed that the Charpy V 10 ft-lb level is also critical for cast steels. Figure 46 provides a summary plot relating the Charpy V fracture energy for various rolled and cast steels to the temperature of brittle fracture determined by the drop weight test. This summary illustrates that the behavior of the cast steels is exactly the same as that of rolled steels. The Charpy V 10 ft-lb temperature defines a boundary temperature below which there is a high probability of brittle fracture in the presence of sharp flaws and above which the steel may be expected to perform satisfactorily despite the presence of sharp flaws, *if* the design is such as not to permit extensive deformation. These conclusions apply to both cast and rolled steels.

Relation of Charpy "V" and Keyhole Data

Investigations[3] of the relation of Charpy V and Keyhole data for rolled structural steels have indicated that the correspondence of the transition curves is as shown in Fig. 47. It is observed that while the Charpy V curve falls gradually over the entire transition range, the Keyhole energy curve shows a region which is characterized by the development, for the most part, of either high or low values. This region is termed the "Keyhole scatter band" and the common procedure of drawing a Keyhole transition is to trace a mean curve through the center of the band, as shown. It has been demonstrated that the 15 ft-lb Keyhole transition temperatures occurs at the same



Fig. 42—Drop weight specimen with crack starting brittle weld. Dimensions 3½x14x1 in.

temperature as the Charpy V 5 ft-lb transition, and approximately at 20 F lower temperature than the Charpy V 10 ft-lb transition.

A limited number of Keyhole transition curves was obtained for Class B cast steels and compared to Charpy V transition curves. It was determined that the relationships described above were also applicable to the cast steels. For a detailed discussion of the specific characteristics of Charpy V and Keyhole transition curves, and particularly for an analysis of the relative merits, the reader is directed to reference (3).

Summary Discussion

It is general knowledge that all ferritic steels, show marked changes in behavior with lowered temperature. In the simplest representation these changes may be defined by a three stage process:

(1) *Ductile stage*—ductile behavior irrespective of stress conditions, i.e., irrespective of the presence of notches.

(2) *Notch Brittle stage*—brittle behavior in the

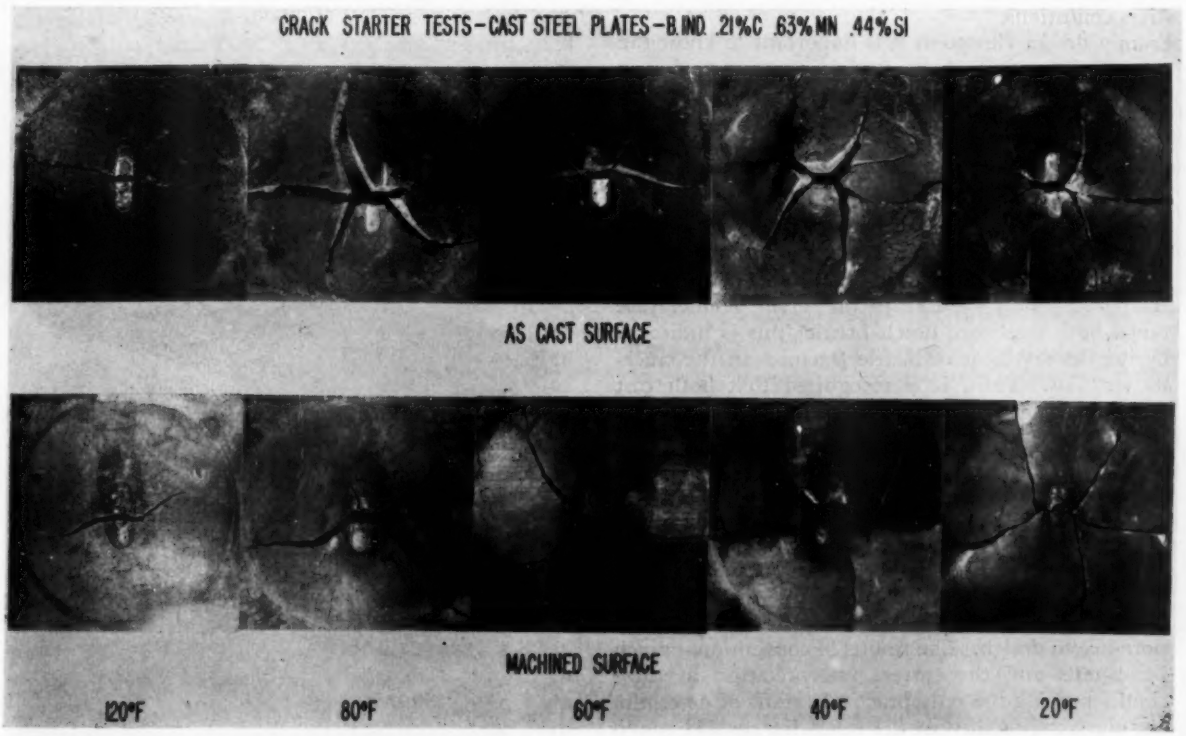


Fig. 41—Illustrating similar fracture characteristics of as-cast and machined surface test plates of Class B cast steel.

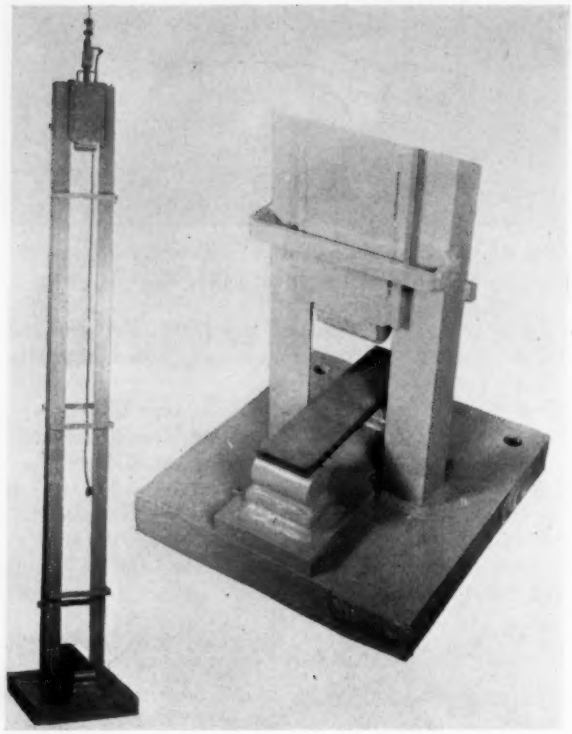


Fig. 43—Drop weight test equipment showing specimen in position for test.

presence of a notch but ductile behavior in the absence of a notch.

(3) *Brittle stage*—brittle behavior irrespective of stress conditions.

From a design viewpoint it is important to know the characteristic stage of the steel in the range of service temperatures (+100 to —40 F). It is deduced from this investigation that commercial Class B cast steels and rolled mild steels are ductile only above service temperature range; this is indicated by refusal to develop brittle fractures at 120-140 F in the crack-starter tests. It is deduced also that the steels are brittle only below service temperatures; this is indicated by the failure to develop bulge deformation at approximately —80 F. In the service temperature range both steels are notch brittle; this is indicated by the development of brittle fractures in the crack-starter tests. Thus, it is recognized that both cast and rolled mild steels are potentially dangerous in the range of common service temperatures. If the nature of the structure is such that brittle failure cannot be tolerated it is essential to use alloy steels which are ductile to the lowest service temperatures.

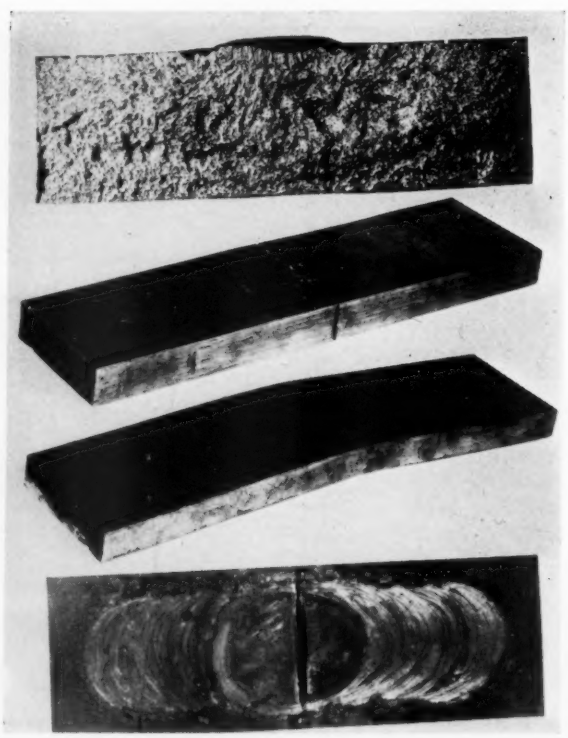
Inasmuch as the presence of notches is necessary to promote the failure of steels in the notch brittle stage, the evaluation of the relative sensitivity of the steels in this stage to notches is of paramount importance to design. The results of conventional notch bend tests and the correct interpretation of these results provide the only practical means of determining the acceptability of the steels for specific conditions of service.

This investigation has demonstrated that the notch toughness quality of commercial rolled and cast mild steels varies over wide limits; the overall range is essentially similar for the two types, however, the average quality of Class B cast steels is slightly better due to alloy and processing differences. Steels of strictly equivalent analysis, heat treatment, section size, etc. are indicated to be of equivalent notch toughness. Notch toughness tests accordingly do not discriminate between cast and rolled steels.

It has been demonstrated also that the notch toughness data are of equal significance for cast or rolled steels. The Charpy V 10 ft-lb temperature, which has been shown to be the criterion of significance to initiation of brittle fractures in the absence of deformation in rolled steel structures, also applies to cast steels as demonstrated by the drop weight tests and the explosion crack-starter tests. Thus, Charpy V data may be used for design purposes without distinction as to types, and the inherent tendency of a Class B cast steel to develop brittle fracture in service should be considered no different from that of a structural rolled steel of equivalent notch toughness.

The serviceability of a specific steel in the notch brittle condition depends on the nature of notches which may be expected to be present. In the presence of cleavage cracks, both Class B cast steels and rolled structural steels *of average quality* develop brittle fracture under essentially elastic loading at approximately 20 F. Steels of poorer than average quality should be expected to fail in a similar manner at

Fig. 44—Typical "Break" and "No Break" tests (Center); brittle fracture of "Break" test (Top); and crack in brittle weld which did not result in fracture of specimen (Bottom).



the higher temperatures of ordinary service. Ship failures which have occurred at temperatures as high as 90 F provide factual evidence of such behavior for the rolled steels.

While similar service data are not available for cast steels, all evidence of this investigation points to a similar performance, provided defects similar to those present in welded structures are present in castings. This last point remains a matter of con-

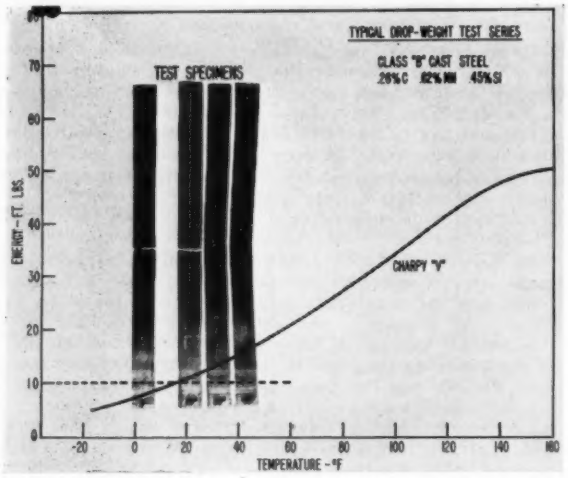


Fig. 45—Illustrating development of brittle fracture of cast Class B steel due to presence of sharp crack at temperatures near or below 10 ft-lb Charpy V transition.

jecture—the only evidence which was obtained entailed casting defects of non-critical geometry. It was shown that shrinkage, sand pockets, porosity and similar defects did not develop sufficiently critical stress systems to cause the failure of the bulges at low levels of deformation. Inasmuch as the cleavage crack of the crack-starter weld was fully effective at the same temperatures, it must be concluded that subject defects are of non-critical nature. The common, small hot-tear defects which have a sharp geometry similar to cleavage cracks were not investigated because of the difficulty of developing such defects in flat plates. It is suspected that such defects would be highly critical and equivalent to the defects found in welded structures. Additional research is required to evaluate the relative severity of the various types of casting defects.

Acknowledgments

The active support and technical advice provided by Dr. O. T. Marzke, Superintendent of the Metallurgy Division during the course of this investigation was highly instrumental to its successful completion. The cooperation of the Steel Founders Society of America T. & O. No. 1 Technical Committee and the T. & O. No. 1 member foundries in providing commercial cast steel samples permitted the obtaining of essential data which would not otherwise have been possible. Their contributions are acknowledged with the appreciation of the authors.

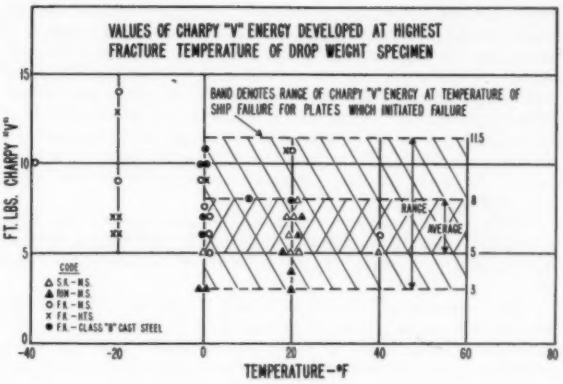


Fig. 46—Illustrating equal susceptibility to initiation of brittle fracture for cast and rolled steels. Ship fracture data bands serve to show that drop weight tests duplicate conditions of brittle failures which occurred in service.

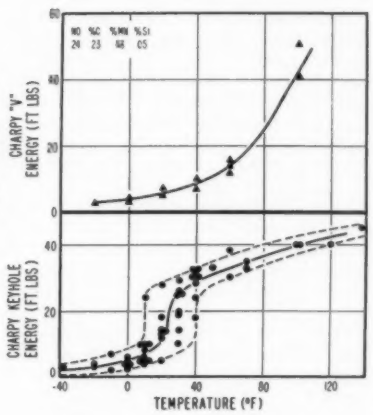


Fig. 47 — Correspondence of Charpy V and Keyhole transition curves which is typical of both rolled and cast steels.

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2. C. H. Lorig, "A Metallurgist Looks at Fracture," *Trans.* A.S.M., vol. 44 (1952).

3. P. P. Puzak, E. W. Eschbacher and W. S. Pellini, "Initiation and Propagation of Brittle Fracture in Structural Steels," presented A.W.S. Fall Meeting Oct. 1952. *Welding Journal,* Nov. 1952.

4. W. L. Williams, "Investigation of Fractured Steel Plates Removed from Welded Ships," National Bureau of Standards, Report N.B.S. 3, June 1951.

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DISCUSSION

Chairman: A. W. Demmler, Campbell, Wyant & Cannon Foundry Co., Muskegon, Mich.

Co-Chairman: J. Neff, American Steel Foundries, E. Chicago, Ind.

Recorder: C. R. Sorensen, National Malleable & Steel Castings Co., Cicero, Ill.

W. K. Bock *(Written Discussion):*[1] The authors' discussion of the mechanics of brittle failure can be useful to the testing engineer who has to explain his interest in tests at temperatures below any encountered in service. If notches are to be reproducible they must be machined and this imposes limitations on the notch angle and on the sharpness at the base of the notch. Practical machined notches give rise to triaxial tension stress states but the notch concentration factor is well below that developed by the limiting case where all the prin-

cipal notches are equal. (Remember the efforts made to produce notches severe enough to measure cohesive strength.) If then, the test engineer cannot have a notch which is sufficiently severe to embrittle the metal under test he is forced to use low temperatures which have nothing to do with service conditions. From the viewpoint of specifications the requirement of a Charpy V-notch test at a given temperature imposes embrittling conditions of a given severity which may be met in service by some entirely different combination of the three embrittling conditions.

While on the subject of testing, do the authors feel that the drop weight test they propose, perhaps combined with staircase testing procedures, can supplement or displace the Charpy test?

The statement of the authors that finer grain size provides the necessary conditions for increased resistance to brittle fracture causes some concern. While the statement agrees perfectly with one made by Doan (see *Principles of Physical Metallurgy*, McGraw-Hill Book Co., p. 79), Sachs and Van Horn (see *Practical Metallurgy*, A.S.M., p. 116, 147) list coarse ferrite grain size as an embrittling factor and state in another place that iron, tungsten, molybdenum and zinc in coarse-grained condition become brittle at temperatures where fine-grained metal is still ductile.

A fine-grain structure will give higher tensile strength and lower ductility in the tension test than a coarse-grained metal. These conditions are associated with lower Charpy values at room temperature. For the comparison of fine and coarse-grained structures at low temperatures, Fig. 86 in the book *Molybdenum* by Archer, Briggs and Loeb can be cited to show that fine-grained steel is not more brittle than coarse-grained metal; in fact, the reverse is shown.

Perhaps the difficulty lies in the definition of brittle fracture. It might be well for the authors to clarify this point.

MR. PELLINI *(Written Reply to Mr. Bock):* Mr. Bock's comments are appreciated, while the discussion is short and to the point it covers considerable ground by implication.

The discussion of the mechanics of brittle fracture was aimed at reviewing a highly specialized complex subject in the simplest possible terms. As indicated by Mr. Bock the arbitrary notch used in a Charpy specimen gives an answer which is specific only to the testing conditions. In service the notches which lead to brittle fracture are ordinarily sharper and hence more critical. Thus, it is necessary to use Charpy data empirically requiring in many cases the use of temperatures which are lower than service. This difficulty is eliminated by the use of a "sharp crack" test such as the "drop weight." While the drop weight test could be used for evaluation of the performance of materials in the presence of an ultra-sharp crack it requires a considerable amount of test material. Moreover, the Charpy tests have been established to the extent that most laboratories are equipped to conduct them. The greatest need for the testing engineer is for an understanding of the significance of Charpy tests. In great part the paper was aimed at developing this understanding for cast steels and illustrating that our knowledge background for rolled steels is fully applicable to the case of cast materials.

Fine grain size is generally considered to provide increased notch toughness, in fact the data available in the literature on this point are quite conclusive. It is quite certain also that tensile ductility is poor parameter on which to judge notch ductility—thus, high tensile ductility provides no assurance that the metal is capable of resisting brittle fracture.

By a brittle fracture it is meant a fracture which proceeds in a "catastrophic" manner, that is, at high speed with very little energy absorption. The best criteria for brittle fracture is a "square break" appearance with a shiny crystalline surface. It may be that Mr. Bock is thinking of the case of metals such as nodular irons or malleable irons which give low Charpy V energy values with concomittant fibrous fractures. It has now been demonstrated at N.R.L. that low energy Charpy V values associated with fibrous fractures are not "brittle" fractures. For example nodular iron resists brittle fracture at 18 ft-lb Charpy V temperatures when tested by the explosion crack starter method using the exact same weight of explosive, etc. as for steels. On the other hand steel, at 18 ft-lb Charpy V temperatures will fracture in a highly brittle manner.

The answer lies in the fact that at 18 ft-lb the nodular iron is developing full fibrous fractures in the Charpy V test while the steel does not do so until values of 50-60 ft-lb are reached. A report presented at the A.S.M. October (1953) meeting demonstrates these facts and provides further evidence that the importance of the Charpy V test is not in the actual values of energy but in the transition range. Actual values of energy are important only when considering materials of a given class which have similar maximum energies at the "upper shelf" of the Charpy V transition curve. For further detailed discussion of this question references (1) and (2) are suggested.

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1. W. S. Pellini, "Evaluation of the Significance of Charpy Tests," A.S.T.M. Symposium on Metallic Materials at Low Temperatures, June 1953.

2. W. S. Pellini, G. Sandoz and H. F. Bishop, "Notch Ductility of Nodular Iron," ASM., Preprint 1953.

C. H. LORIG *(Written Discussion):*[2] Information contained in the paper is of great value to anyone interested in the problem of brittle behavior of steel both at room and at sub-zero temperatures. The paper is most welcome.

I should like to ask Mr. Pellini if the excellent correlation they have experienced between the behavior of steel in the Charpy V-notch tests and the explosion crack-starter test, as shown in Figs. 36, 37, and 40, remains substantially the same for all steel compositions studied and for all conditions of heat treatment covered by the tests.

In the case of the Navy tear test and the Charpy keyhole notch test on semikilled ship-plate grades of steel we have found that the correlation between these tests varies, for instance, with changes in composition of the steel. The Navy tear test, for example, is inherently less sensitive to the effect of manganese on toughness than is the Charpy keyhole notch test. The fact that the Navy tear test is conducted under substantially static loading whereas the Charpy test is conducted under dynamic loading may, among other things, have some effect on the difference in sensitivity of the two types of tests we used. This, of course, means that all tests do not rate steel the same as to their ductile-to-brittle behavior.

From the information given in the paper, there is an indication that the Charpy V-notch and the explosion test, at least in some cases, are equally sensitive to the effect of temperature on fracture characteristics. It is remarkable that brittle behavior should start with both tests at approximately the same temperature.

MR. PELLINI *(Written Reply to Dr. Lorig):* Dr. Lorig's comments are appreciated particularly in view of his outstanding experience in the general field of the brittle fracture problem. The questions which are asked are of basic significance to this problem and of prime importance to the design engineer who must perforce decide on the use of steels based on Charpy V or Keyhole data.

The correlations experienced between the behaviour of the steel in ship service and in the crack starter tests has been demonstrated to be the same *at same temperatures.* Data to substantiate this claim have been presented in detail previously.[3] Thus, at least for semi-killed and rimmed structural steels used in ships the crack starter tests are validated in direct fashion. For the case of other steels such as the quenched and tempered alloy types there are no adequate service correlations on which to base a correspondence to the crack starter tests and it is assumed that the performance indicated by these tests is equivalent to that of service. If this assumption is correct it follows that the Charpy V relationships of other steels to service should be as indicated by the relationships to the crack starter tests. This question is discussed in detail in a new report by the writer to present at the A.S.T.M. Symposium on Metallic Materials at Low Temperatures, Atlantic City Meeting June 28-30, 1953, entitled "Evaluation of the Significance of Charpy Tests." Briefly, the correlation with Charpy V curves does vary with the class of steel the major difference being a tendency to develop fracture in the crack-starter tests at temperatures equivalent to higher positions on the Charpy V transition curve than for the mild steels. In other words the improvement predicted by the Charpy V curve for an alloy steel is not fully equivalent to the shift in the V transition curve but somewhat less. For further details the above quoted report is suggested for reference.

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[1] Research Engineer, National Malleable and Steel Castings Co., Cleveland.

[3] Assistant Director, Battelle Memorial Institute, Columbus, Ohio.

CONVEYOR MOLDING STANDARDS

By

Everett C. Reid*

This paper presents a method of developing time standards for mechanized molding lines. The term "molding line" is here used to refer to several sets of Cope and Drag, Jolt-Squeeze Machines grouped together in such a way as to feed molds to a continuously moving conveyor line. Pouring, Core Setting, Shake-out, and other operations, are also done in conjunction with this line.

The manpower for which we set the "line" time standards at Ford of Canada, includes all direct labor associated with the production of a casting up to the cleaning operation. All, that is, apart from what is involved in core making and metal preparation. This manpower ranges from 35 to 45 per line. We consider it very important to set accurate standards when the direct labor factor is so considerable.

The main objects in developing time standards for conveyor molding are:

1. To establish a time standard that will show the total direct labor allowed to produce the casting under specified standard conditions.
2. To establish a standard conveyor line speed that will result in performance to standard.
3. To establish the number of men necessary to man the line when work is distributed equitably and standard line speed is maintained.
4. To establish the basis for economic scheduling of castings assigned to the line, and for cost comparison of alternative production plans.

The development of standards for these purposes is the topic of the present discussion. Time Standards, as referred to here, are not used as a basis for incentive earnings. However, it is possible that an incentive plan could be developed, based on this method of approach.

Time standards for mechanized molding lines are merely group standards established by co-ordination of information normally obtained in ordinary time study work. Most systems of time study in use for independent operations are adaptable to "line" work. However, before you even start a molding line study it is desirable to commit your statements of policy and procedure to written form. These line standards have to be sold to Management and Labor and, without proper comprehension, this cannot be done. Experience indicates that most labor trouble, on mechanized lines, has stemmed from indefinite or ambiguous policies on time standards work.

To illustrate what I mean let me quote a few condensed statements of our Policy and Procedure.

A time standard is a measure of the time allowed for the proper performance of a specified task or tasks by a qualified operator, or operators, while working at a standard pace.

The key phrases are "time allowed," "specified task," "qualified operator" and "standard pace." They call for a moment's consideration.

First, the "time allowed." This is not just a theoretical possibility compiled from stop-watch readings. The time study man has to bear in mind certain standards of quality and safety. Again, there will in practice be all sorts of delay factors: Minor hitches in routine, the personal needs of operators, and sometimes unavoidable fatigue effects. These cannot be anticipated in detail, yet cannot be ignored either. They are covered, therefore, by percentage additions to the theoretical time. For instance, Ford of Canada allows a man 24 min per 8-hr shift for personal needs. And all time calculations take account of this fact.

Next, the "specified task." An operator's work must be exactly laid down. He must use a standard method with written specifications behind it, and follow a schedule giving particulars on such matters as volume and mix. Essential services also enter the picture.

A "qualified operator" means a person suited to the task and proficient in following the standard method. A "standard pace" means a pace he can be expected to maintain, working on an hourly wage rate, if he goes along correctly and conscientiously without piling up undue fatigue at any part of the shift.

Basic concepts like these stated, of course, in more precise language, are absolutely vital.

We approach the actual study of the molding line by dividing our work into seven phases. These phases are:

* Superintendent, Manufacturing Engineering, Foundry and Heat Treat Plant, Ford Motor Co. of Canada, Windsor, Ont., Canada.

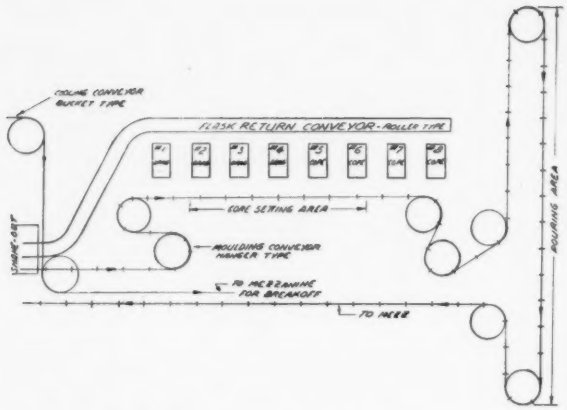


Fig. 1A—Schematic Diagram of a Molding Line.



Fig. 1B—Type of Line for which Time Standards Are Established.



Fig. 1C—An operation on a Moving Conveyor Line for which Time Standards Are Established.



Fig. 1D—Receiving Metal for Pouring to the Molding Line.

1. Planning.
2. Establishment of procedures for collecting existing process and allowance data.
3. Measurement and development of operational standard times.
4. Line balancing, assignment of crews, and development of the time standards.
5. Writing of records and authorities.
6. Installation of standards and publication of supporting data.
7. Maintenance of standards, performance analysis, and evaluation of results in terms of cost reduction.

It may be helpful, before detailed analysis, to consider roughly how a typical molding line would be laid out (See Fig. 1). The plan has been reduced to essentials so that the general approach will be more obvious, and easier to adapt to other circumstances, should anyone so desire.

Now, when we set out to provide time standards for conveyorized molding on a line similar to that shown, our work develops through its seven phases something like this.

Phase 1—Planning—Planning is always important, and planning the time study of a mechanized line is particularly so. A "hit-or-miss," "cut-and-try" approach will not only take much longer, and lead to a muddle, but it will give Supervision and labor an idea that the time study man does not know what he's doing, which will probably be right. In the planning, therefore, we use a Planning Sheet, (See Fig. 2) which gives us written preliminary information, estimated or factual, to guide us in our work. On this sheet we list:

1. Basic operations performed on each casting.
2. All castings produced on the line, tentatively grouped, if possible, in economic groups or combinations. Some castings, owing to their design or process requirements, can be grouped advantageously.

MOULDING LINE PLANNING SHEET - LINE NO.

OPERATION DESCRIPTION	OPER. NO.	GROUP A CREW	GROUP A STU-DIES	GROUP A CO-DE	GROUP A T.S. MAN	GROUP A REMARKS	GROUP B CREW	GROUP B STU-DIES	GROUP B CO-DE	GROUP B T.S MAN	GROUP B REMARKS	GROUP C CREW	GROUP C STU-DIES	GROUP C CO-DE	GROUP C T.S. MAN	GROUP C REMARKS	SPECIAL PARTS CREW	SPECIAL PARTS STU-DIES	SPECIAL PARTS CO-DE	SPECIAL PARTS T.S. MAN	SPECIAL PARTS REMARKS	TOTAL STUDYS	CO-DE
PRODUCE CORE MOULD	1																						
PRODUCE DRAG MOULD	2																						
GAUGE & SET CORES	3																						
CORE SUPPLY	3A																						
CLAMP MOULDS	4																						
POUR MOULDS	5																						
REMOVE CLAMPS	6																						
SHAKEOUT-PULLOFF	7																						
-TRANSFER	8																						
-CLEAN HANGER	9																						
FLASK RETURN-UPPER	10																						
-LOWER	11																						
BREAKOFF	12																						
MIX SAND	13																						
TEND SAND HOPPER	14																						
CLEAN UP BASEMENT	15																						
HANDLE PYROMETER	16																						
PREPARE & DIST. FACING SAND	17																						
PARTS IN GROUP																							
	CODES	TIME STUDY MEN					TYPE OF TIME STUDY CH - CHECK, D - DELAY, Sc - S AND CONTINUOUS ALL OTHERS ARE SNAP BACK																	

Fig. 2—Molding line planning sheet.

MOULDING DATA SHEET

DU-325 DATE _____

Moulding Data to be supplied by foundry relative to new parts, experimental parts and current parts when moulding method changes.

TYPE OF METAL
- ☐ Cast Iron
- ☐ Camshaft
- ☐ Crankshaft
- ☐ Special
- ☐ No. 4
- ☐ No. 6
- ☐ No. 7

MELTING
- ☐ Electric
- ☐ Brackelsburg
- ☐ Induction

MOULDING SYSTEM
- ☐ No. 1 Reel
- ☐ No 3 Reel
- ☐ No. 4 Reel
- ☐ No. 5 Reel
- ☐ No.6 Reel
- ☐ Jobbing
- ☐ Induction
- ☐ Volta

Green Sand Flask _____
Dry Sand Holder _____
Castings Per Mould _____
Castings Per Tier _____

Poured Weight Per Mould _____
Poured Weight Per Casting _____
Cleaned Weight Per Casting _____

	Cope or Top	Drag or Btm	Centre	Pin	Splash	Ramp					Cup or Basin	Strainer
Cores per mould												
Weight per core												
Cores per casting												
Produced: Turn table												
Osborn												
Rollover (H or M)												
Manual												
Silica Wash												
Core Room												
Cores per pattern box												
Cores per oven drier												
Cores per oven plate												
Core oven: North No. 1												
North No. 2												
South No. 3												
South No. 4												
South No. 5												
South No. 6												
South Drawer Oven												

Remarks:

Approved By _____
Foundry

Part Name _____ Part Number _____

Fig. 3—Molding Data Sheet.

We may have four different pattern plates each day, one on each set of machines, but we try to group them to promote labor economy. This procedure tends to complicate scheduling, but it is justified by reduction of costs.

3. The manpower currently assigned per operation, relative to each of the combinations actually scheduled. The remarks column is used to indicate crew variations, which sometimes occur, before the line is organized.

4. A code for the type of study judged to be necessary.

5. A code for each time study man assigned to each specific study or operation.

This sheet also serves as a record of time study work load, assignment and progress, if suitable notes are made in the remarks column.

Phase 2—Procedures for Collecting and Recording Existing Process and Allowance Data— Process data for example, patterns, flask, metal type, cores per casting, will now be collected and posted to a "Molding Data Sheet" (See Fig. 3). This information rightly should come from a process section, and such a section is now being established. However, at present, some of it comes from Supervision, and the rest we collect from observation on the floor. In this phase we prepare to collect, daily, information which will later be used to establish the molding line allowances. We record the data on a "Molding Line Allowance Sheet" (See Fig. 4). In this way we build up a historical record of significant time study factors while we are studying the line. This information is highly important and should be collected with the aid and approval of operating Supervision.

MOULDING LINE ALLOWANCE SHEET

DEPT. LINE NO. SHEET OF

1	2	3	4	5	6	7	8	9	10	11	12	13	14	15
DATE	LINE SPEED	SCHEDULE	POSSIBLE PROD.	ACTUAL PROD.	PROD. DIFF.	ACCOUNT 751		LINE STOPPAGES			PROCESS SCRAP	LINE CHANGE-OVER/LOSS	TOTAL LOST MOULDS	REMARKS
								TOTAL	INCIDENTAL					
			(MOULDS)	(MOULDS)	(MOULDS)	(MIN.)	(MOULDS)	(MIN.)	(MIN.)	(MOULDS)	(MOULDS)	(MOULDS)		
TOTAL														
TOTAL														
TOTAL														
TOTAL														
TOTAL														
TOTAL														
TOTAL														

Fig. 4—Molding Line Allowance Sheet

Since the type of information posted to this form will depend entirely on the specific line and organization to be studied, an elaborate treatment here would not be useful or even practical, but the results will be discussed briefly in the line balancing phase.

Phase 3—Measurement and Development of Standard Times— In Phase 3, we actually start our time study work. Our main objective, at this stage, is to determine the measured work content of each operation and find the "control" operation. By control operation we mean *the operation which requires the most work time to complete,* an operation where overall economy or space limitations prohibit additional manpower. For example, on a line like the one illustrated in Fig. 1, the control operation is the making of the cope half of the mold on the jolt squeeze machine. Since the mold is manually handled and mold close-up is delicate, two men are required. These men's job assignment, requires more time, according to the time study, than any other on the line. An additional man cannot be added to the machine crew owing to space limitations. This control operation, in most molding line work is usually apparent by observation if the staff is well trained and experienced. However, it is best to keep an open mind as to what the control will be, until all apparent "tight spots" have been studied.

When it is thought that an operation may be the control, time study men first analyze it by use of a work sheet which we call a "Control Operation An-

CONTROL OPERATION ANALYSIS SHEET

DATE DEPT. MACHINE NO. COPE OPERATOR - SHEET OF

TIME STUDY MAN LINE PART NO. DRAG HELPER LINE SPEED

ELEMENTAL DESCRIPTION	TENTATIVE BREAK OFF		SPOT CHECK	FINAL BREAKOFF	METHOD CHECK
	OPERATOR	HELPER			

Fig. 5—Control Operation Analysis Sheet.

DU-175IC DATE TSM- START- DISALLOW- PART NO.-

DEPT. STOP - ACTUAL - PART NAME-

OPERATION -

RUN IN COMBINATION WITH 1. 4.
2. 5.
3. 6.

METAL -

SAND DATA -

POUR REEL SPEED- MIN/HANGER F.P.M. -
COOL CONV. SPEED- MIN/HANGER F.P.M. -

FLASK NO.-	CORES PER BOX
PATT. DATA-	CORES PER PLATE
CHAPLETS-	CORES PER CASTING
FACING SAND-	CORE WT. (GREEN)
CASTINGS PER MLD.-	CORE WT. (DRY)
CLEANED WT. PER CAST-	WIRES PER CORE
POURED WT. PER CAST-	CORE PLATE SIZE
CORES PER MOULD	PLATES PER SHELF
1.	SHELVES PER RACK
2.	OVEN SPEED- MIN/RACK F.P.M.
3.	

OPERATOR- CC NO.- EXP.-
OPERATOR- CC NO.- EXP.-

EQUIPMENT & TOOLS: B.T.#-

SAFETY DATA

APPROVED FOR METHODS AND SPECIFICATIONS

FOREMAN:

PART & LAYOUT SKETCH

REMARKS:

Fig. 6—Timestudy Observation Sheet.

alysis Sheet" (See Fig. 5). On this sheet we write up the job as it is being done, showing tentative time study breakoff points. We pay particular attention to operator methods at this stage, and also proper balance of work between operator and helper, when applicable. The stress is not on time values, but on Method improvements and re-distribution of work. Recommendations are now made to Supervision. When everything is done that can, or will be done, the time study description is written up on our time study observation sheet (See Fig. 6).

On a two-man control operation, we find it advisable to take continuous, simultaneous studies on both helper and operator. Several studies are taken if necessary. We also use a basic motion time study approach, occasionally to check these points. In these studies, we establish measurement of work content only and disregard idle time, particularly where *both* operator and helper have such time. No allowances are considered yet. The elemental time values are posted to a sheet which we call a "Master Element Control Chart" (See Fig. 7). It is designed specifically for our needs where we have a control operation with a helper and an operator. It merely serves to summarize and compare the data obtained from representative time studies. Studies taken are numbered, and the elemental times are posted against the Standardized Elemental Description, opposite the related time scale printed on the form. This gives us a graphic presentation of the work elements, and makes it easier to select a master or standardized element time value for operator and helper. The sum of these elemental values, excluding any simultaneous idle time, constitutes the standard time for the control operation or work station. It is not the time standard, though, since no allowances have yet been considered.

This time value or standard time determined for the control operation sets the line speed. Since this operation is the slowest of all jobs on the line, a line speed set for it will also cover all other faster operations. Suppose for example, we have a line similar to that shown in Fig. 1, where four cope and drag machines deliver molds to a moving pendant hanger type conveyor. The control operation is determined to be the making and close-up of the cope mold. Time studies tell us that the standard time for this is .5 min. In other words, the two-man crew have .5 min from the time they deposit one cope on the hanger until the next one will be required. Since there are four cope machines operating the line speed must be four times as fast as the individual cope cycle. Thus we see that actual line speed is .500 divided by 4 or .125 min per hanger. That is, given any fixed spot, one

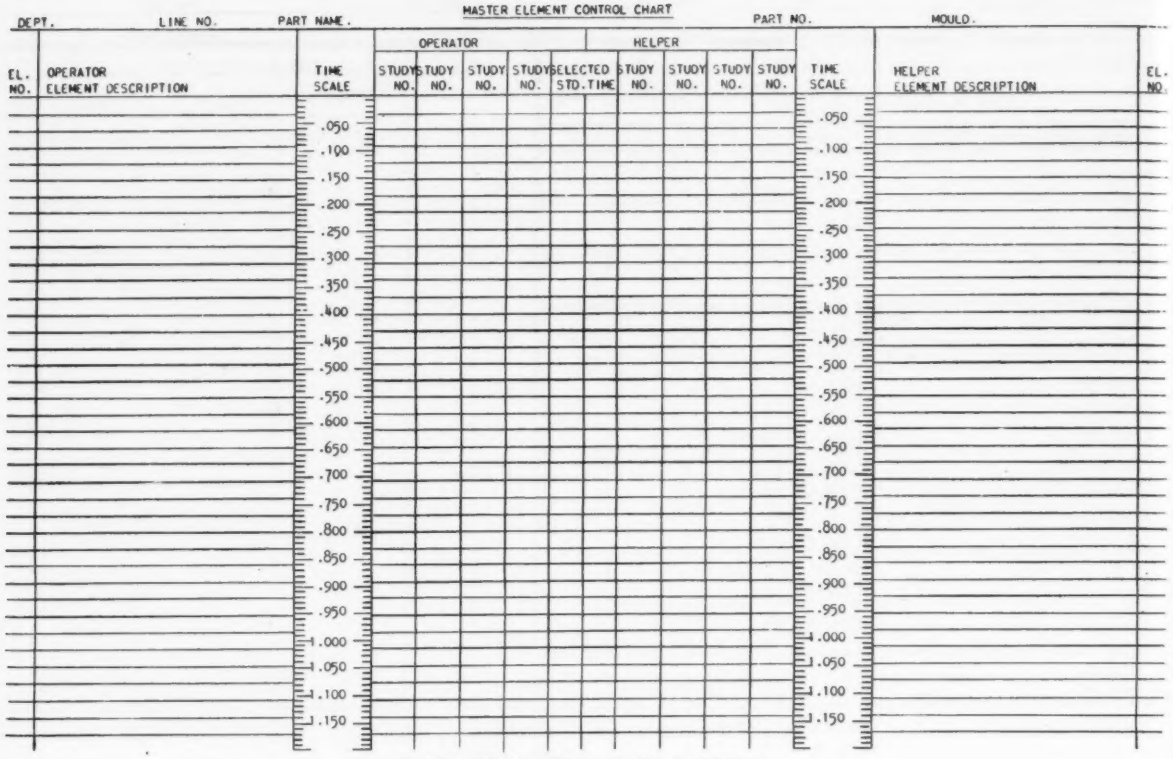


Fig. 7—Master Element Control Chart.

hanger will pass it every .125 min or eight hangers pass per minute. Line speed can also be quoted in terms of feet per minute when hanger spacing is known. A hanger spacing of 3 ft in the above would be equivalent to 8×3 ft or 24 ft per min.

There is no need to go into detail about the actual time study in this discussion. Any recognized system is acceptable. However, the study on the control station or operation *must* be right and it is well worth while to spend considerable time in making it so.

The other non-control operations can be studied in a less refined manner. With them, it is only necessary to establish that the measured work content is less than that of the control station. The idle or balance time which comes up later, in connection with the line balancing phase, is the difference between the *non-control operation standard time* and the *control operation standard time.* This time loss, when accumulated for each non-control operation, is the penalty paid for mass production by mechanized molding. Methods study, if applied to non-control operations can pay high dividends, by elimination of unnecessary manpower.

At this point, then, we know the *standard time* for the control operation and thus the proposed line speed. We also have standard times for all other operations which are necessary to run the line.

Phase 4—Line Balancing, Crew Assignment and Calculation of Final Time Standards— In line balancing we record all operations necessary to run the line, with a breakdown of work time and unavoidable operator idle or balance time. We record this for each economical pattern combination relative to the number of molding units (pairs of machines) operated, at the appropriate schedule.

For this purpose, and also to show the resultant manpower distribution, we use a standard form "Molding Line Manpower Distribution Chart". (See Fig. 8). This form is eventually distributed to operating Supervision. It provides them with:

1. A detailed breakdown of operation standard times.
2. A guide to formation of efficient mold combinations.
3. Manpower assigned to each operation.

The most significant figure, in connection with the final time standards calculation is the total manpower per molding unit or pattern set-up. This form is used by Supervision to determine how their men should be distributed for the particular pattern combination they intend to run. Line performance to standard requires strict adherence to this standard manpower. The line manpower distribution sheet also shows Supervision the idle or balance time for each operation. We consider it their responsibility, as well as ours, to reduce this if practical. It is good time study practice to highlight the idle time inherent in mechanized molding lines whenever and wherever possible.

And now, the second stage of the line balancing is done on our molding line standards sheet (See Fig. 9). This is divided into three sections:

1. **Production Specifications**— This shows Supervision the standard conditions upon which the time standard was set. The information is obtained from the molding line data sheet collected in Phase 2, but

MOULDING LINE MANPOWER DISTRIBUTION CHART

DEPT. LINE NO. FOUNDRY DATE OF ISSUE -

OPERATION NAME	CODE	AVAILABLE WORK TIME PER MOULD	LABOUR MINUTES PER MOULD MACH. #1 WORK	MACH. #1 IDLE	MACH. #2 WORK	MACH. #2 IDLE	MACH. #3 WORK	MACH. #3 IDLE	MACH. #4 WORK	MACH. #4 IDLE	CREW REQD	EQUIPMENT OR TOOL DETAIL
PRODUCE COPE MOULD												
PRODUCE COPE MOULD												
PRODUCE COPE MOULD												
PRODUCE COPE MOULD												
PRODUCE DRAG MOULD												
PRODUCE DRAG MOULD												
PRODUCE DRAG MOULD												
PRODUCE DRAG MOULD												
GAUGE & SET CORES												
GAUGE & SET CORES												
GAUGE & SET CORES												
CLAMP MOULDS												
POUR MOULDS												
REMOVE CLAMPS												
SHAKEOUT - PULLOFF												
- TRANSFER												
- CLEAN HANGER												
FLASK RETURN - UPPER												
- LOWER												
BREAKOFF												
MIX SAND												
TEND SAND HOPPER												
TEND BASEMENT BELT												
CLEAN UP BASEMENT SAND												
HANDLE PYROMETER												
PALLETIZE CASTINGS												
TOTALS												COMBINATION GROUP
TOTAL MINS. PER MOULD												#1
EQUIVALENT MEN PER MOULDING UNIT												#2
CODES: "C" - CONTROL STATION x - MEN EXHANGE PERIODICALLY												#3
* - TIME PER MOULD BASED ON PROPORTIONAL AMOUNT OF TOTAL WORK TIME												#4

APPROVED FOR PROCEDURE AND EQUIPMENT - TIME STUDY MAN -

TIME VALUES SHOWN ARE FOR OPERATIONS AND METHODS AT DATE OF TIME STUDY
NOTIFY TIME STANDARDS AND METHODS DEPARTMENT OF ANY CHANGES.

Fig. 8—Molding Line Manpower Distribution Chart.

is audited on the floor before it is recorded.

2. **Standard Allowances**—This shows Supervision the standard allowances provided. These have been developed from historical records subjected to statistical procedures and are based on actual production figures collected during the period of time study, as suggested in Phase 2. We must be realistic in our Foundry Time Study Work, and we know from experience that, in a rough process such as molding, certain things occur which tend to prevent the most efficient Supervision from attaining ideal or possible productivity. For instance our molding lines start work at 7:30 A.M., but a certain length of time is necessary to get into cycle. Likewise, all operations cannot quit exactly at 4:00 P.M. This condition we recognize by granting a "start and stop" allowance. Other special allowances which we build up from actual running records are "incidental delay" and "process scrap allowance."

Intelligent recognition of factors like these turns an academic "paper" standard into a standard that is practical and attainable. Planning and adherence to established procedures will permit finalizing when, and only when, all vital facts have been taken account of in the time standards calculation. The significant figure in this standard allowance section is, of course the standard production or program schedule, quoted in molds per day per machine.

3. **Calculation of Standard**—In this section we actually calculate the time standard per mold using:

(a) Manpower from the manpower distribution sheet.

(b) Standard production in molds per day per machine, obtained from the allowance section of the same sheet.

(c) Standard relief as established by Company Policy.

The time standard per mold is then converted to a time standard per casting by using the "casting per mold" information shown in the production specifications section of this same sheet.

The "actual line speed" calculation shown on this sheet is not important in a general discussion. It is a calculation which gives us the line speed necessary to produce 8 hours production, even though the line is stopped for two 10-min rest periods, in keeping with the Union Agreement. The final time standard, in minutes per piece and hours per piece, is shown with the effective date and other identifying data in the spaces provided at the bottom of the sheet.

Phase 5—Development of Records and Authorities—In addition to the forms previously mentioned, which quote time standards information, we also issue a less detailed form to our time study audit section

D-15

MOULDING LINE STANDARDS SHEET

DEPT. LINE NO. FOUNDRY DATE OF ISSUE

PRODUCTION SPECIFICATIONS	STANDARD ALLOWANCES
FLASK NO. - CASTINGS PER MOULD -	TOTAL WORK DAY = 480 MINS.
COPES USED -	TOTAL POSSIBLE MOULDS AT STANDARD LINE SPEED OF MIN. PER HANGER =
	DEPARTMENT SCHEDULE= 480 MINS. = MINS. PER HANGER
METAL SPEC.-	PROCESS & INCIDENTAL ALLOWANCES -
CHAPLET DATA -	START - = MOULDS
POURED WEIGHT PER CASTING -	STOP - = MOULDS
	MOULDING SCRAP - = MOULDS
	INCIDENTAL DELAY - = MOULDS
	TOTAL MOULDS LOST- = MOULDS -
	NET MOULDS PER 8 HOUR SHIFT =
DISC NO.- DISC WEIGHT -	PROGRAM SCHEDULE = ____ = MOULDS PER
	PER MACHINE 4 MACHINES SHIFT
HANGER SPACING - C. TO C.	

TIME STANDARD CALCULATION

ONE MACHINE -

TOTAL DAILY LABOR TIME MEN X 480 MINS. = MINUTES

RELIEF TIME ALLOWED 5.26% = MINUTES

TOTAL MINS. PER MACHINE = MINUTES

TIME STANDARD PER MOULD ____ MINS. / MOULDS = MINUTES PER MOULD

TIME STANDARD PER PIECE MINS. PER MOULD / CASTING PER MOULD = MINUTES PER CASTING

ACTUAL LINE SPEED = 480 MINS. PER DAY - 20 MINS. FOR "BREAK" PERIODS / MOULDS PER SHIFT = MINUTES PER HANGER OR FEET PER MINUTE

T.S.M.	DATE EFFECTIVE	HOURS/PC.	MIN./PIECE	AUDIT	PART NAME	COMBINATION GROUP
						PART NO.-

Fig. 9—Molding Line Standards Sheet.

and, through them, to the Cost Department. When we spend so much time and effort on line studies, we feel that our work deserves proper filing and handling so that we can refer to it readily (See Fig. 10).

We arrange for this by maintaining a molding line authority file, in which all work pertinent to an established time standard is held. The file numbering system tells us at a glance the date the time standard was established, the department number, and the assigned authority number. Definite procedures for file reference and obsolescence are also provided, so that a good time study job will not be wasted by inadequate records and files.

Phase 6—Installation and Publication of Data—When we have completed the study of a molding line we find it expedient to issue an "installation report" in an "acro press" binder, in which we comment on the procedures used to develop the standards, particularly allowances, and make suggestions for future improvement. Supervision is asked to review this report, and a discussion is held to iron out any misunderstandings or misinterpretation on their part or ours. We have found this procedure very effective. The spirit of co-operation fostered by these discussions has been one of the main factors in our successful time study installations. We believe we are fortunate in having a progressive group of Supervisors and Management Men in our Organization, but we also believe that we have enlisted their help in a most effective way.

A good standards installation which has not been

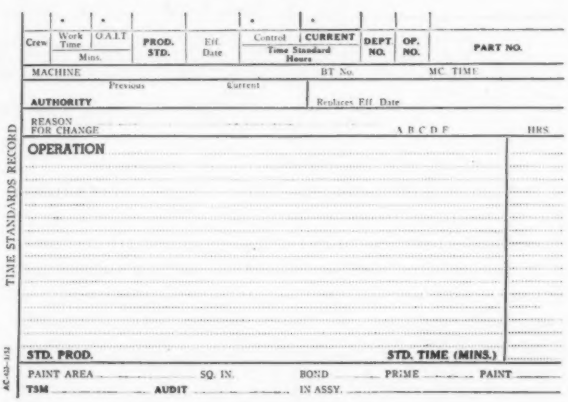


Fig. 10—Time Standards Record.

"sold" to Supervision and Management, to the extent that they provide active help in making it work, is worse than useless. It is necessary to do a good time study job, and then do a good selling job. After everyone is reasonably satisfied, the standards are made effective, and we co-operate with Supervision, as requested, to make them work as planned. In this phase, we also issue our standards data to any other part of the organization, that may be interested.

Phase 7—Maintenance of Standards and Performance Analysis—When conditions change, permanently, the time standards may also have to be changed. However, unless the change affects the control operation or calls for more or less manpower at non-control stations, the time study work involved is not too

great. The changes mainly consist of re-writing records. This, we call time study maintenance.

Our performance analysis is done on a daily basis for each molding line. We make the following calculation:

$$\frac{\text{Direct Labor Hours (From Payroll Department}}{\text{Total Standard Hours for the Line (Production count} \times \text{Standard Hours per Piece)}} \times 100$$

The formula gives a percentage figure which will show:

(a) Below 100 pct if performance is *better* than standard.

(b) 100 pct if performance is standard.

(c) Above 100 pct if performance is below standard.

This performance index is designed to reflect labor performance. Time lost because of off-standard conditions (for example, conditions not covered by the time standard) should be charged out as indirect labor time through the accounting system. It is very important that Supervision should know precisely the work covered by the time standard, so that credit can be claimed by charging out lost time due to off-standard conditions. By showing the cost of off-standard conditions through these accounting charge-outs, Supervision can keep Management informed as to the nature and cost of conditions which require attention.

So much for the seven phases of "Developing Time Standards for Conveyor Molding." We have used our procedure for over two years. It is not spectacular, nor is it entirely original. It is merely a common-sense, organized approach to a rather difficult time study problem. We know it works and although our production figures, are confidential, it is no secret that the combined plan, that is, good time study plus active co-operation of Supervision, increased production on one high volume casting 12 pct and at the same time reduced unit cost. Records of other castings have shown a similar trend.

Summary

And now in conclusion, and as a summary, here are a few bits of general advice, directed at anyone who is trying to deal with the same problem.

1. Formalize your regular time study policy and procedures by publishing them in written form.

2. Organize your study so that is gives:

(a) Time Standards for Costs.

(b) Correct conveyor speeds.

(c) Proper crew assignments and labor distribution.

(d) A basis for economic scheduling.

(e) Cost reduction through better planning, better methods and increased productivity.

3. In actual line time study, try to emphasize:

(a) The work on the control operation.

(b) The establishment of fair allowances that will produce practical, workable standards.

4. Publish adequate records of work done, and file complete authorities to back up your decisions.

5. "Play ball" with your Supervision and Management. Tell them how much and what you have done. Sell them on co-operating with you in the installation phase.

If you keep these key points in mind you, too, can develop time standards for conveyorized molding lines.

MECHANICS OF FOUNDRY MECHANIZATION AND IMPROVED METHODS

By

R. J. Anderson*

ABSTRACT

Reduced costs and improved quality can be obtained through mechanization and through better work methods, according to the author. Neither step alone will develop maximum savings; improved work methods are especially desirable in the smaller shop where size of capital investment limits extent of mechanization. Mechanization calls for a good preventive maintenance program to minimize down time.

■ Reasons for mechanization of a foundry are: (1) To reduce labor cost; (2) To reduce material cost; (3) To increase capacity with the available floor space; (4) To improve quality of product.

To reduce labor costs; we mechanize so that fewer men are required for handling the same tonnage, or so that more tonnage can be produced with the same number of men, or both. How this and the other advantages of mechanization were achieved are illustrated by scenes in the author's company.

With the old air furnace fired by pulverized coal, two truck drivers delivered coal to the various pulverizers and one crane operator and crane loaded the coal from the ground to the bunkers so that the shop trucks could be readily filled. Now the pulverizers have an outside coal hopper and coal is purchased locally. The coal company dumps the coal in the hopper and from there it is conveyed to a storage bin directly over the pulverizer. There is no labor connected with the handling of coal at the present time.

The overhead or yard crane used in making up the cupola charge requires only one crane operator who has a scale for weighing the charge directly in front of his cab. One charge car operator is also needed. These two operators are capable of charging 17 to 20 tons per hour, which results in a 25 to 30 per cent reduction in labor cost, plus the advantage of a continuous supply of molten iron. The cupola iron is run into the air furnace where analysis and temperature are adjusted.

For iron delivery, lift or fork trucks are used. These have a cylindrical ladle of 1000 lb capacity. The savings in both man hours and fatigue accomplished by this type of pouring, as compared to the old method, are obvious.

Molders formerly would put up the number of molds they thought they could pour off. Thus, molding pro-

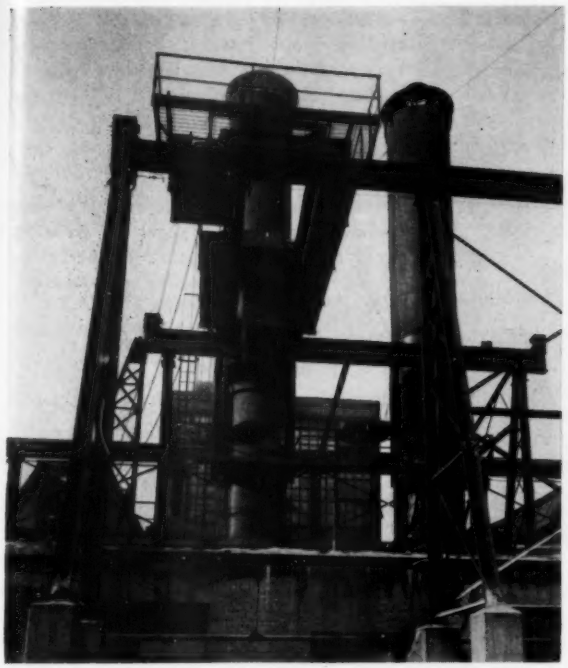
* Director of Manufacturing, Belle City Malleable Iron Co., and Racine Steel Castings Co., Racine, Wis.



Yard crane used for making up cupola charge handles pig, purchased scrap, and returns easily and accurately.



Charge car, operated by one man, teams up with yard crane to handle 20 tons of melting stock and fuel per hour.



Charging bucket in one of Belle City's cupolas. Mechanization of charging reduced labor cost over 25%.

duction was pretty much controlled by the molders' ability to carry iron. In molding with the old side floor arrangement, a good molder would put up an average of 15 to 20 molds per hour. On Belle City Malleable's 12-hopper, squeezer molding line with power driven mold conveyor unit, production is from 30 to 50 molds per hour per man depending, of course, on the type of job and number of cores to be set. For this line one serviceman and one sweeper are required.

The jacket and weight shifting on this line is not good and a program to improve it will be discussed later. Pouring on this line is done with 1000-lb ladles and three men pour all the molds.

Slingers are used for heavy cope and drag molding. Production on this line is from 80 to 100 molds per hour depending on the type of job being run. Fatigue is very low on this unit because hydraulic slingers are used and there is no manual lifting anywhere on the line.

Weight Shifting Simplified

All of these molds are made in slip or pop-off flasks and all of the machines are the pin-push-off type. The drag is rolled over in a bail without the use of any bottom boards which, of course, eliminates clamping and facilitates dumping as compared to tight flasks. The bottom board is transite and is actually the car top, never leaving the car. The largest pop-off flask used is 27 x 52 in. because this is the largest size the conveyor car will take. The jackets and weights of these molds are shifted by two men. This is accomplished by timing so that the same type of mold passes a predetermined place on two parts of the loop. The jacket and weight are merely transferred from one mold to another with a hoist. Extended pins on the weights rest on top of the jacket when it is transferred so that both the jacket and weight are moved with one lift. Two men do all the pouring on the slinger line.

One man attends the automatic shakeouts. He supplies empty buckets from the overhead conveyor line and hooks the full buckets back on to be transported from the foundry to the hard iron spruing and chipping conveyor. Formerly, a number of men were used for dumping or shakeout.

Add Pallet Line

In mechanization it is important to make a careful study of the type of mold or castings that have to be made, giving consideration to such factors as availability of continuous iron and storage space, and the cooling time needed. After installation of the squeezer line as previously shown, and the slinger line, something had to be added to balance production requirements. A pallet line was added.

The flexibility of this 22-station unit, permits use of various types of metal, and both squeezer work and cope and drag work. Some metals are not delivered continuously but in batches from electric furnaces. Except for the flexibility of this unit, this would not be possible.

Considerable storage space, both before and after pouring, is provided for the molders. Each has from one to three hours storage space depending on the type of job and the length of floor. After the molds are poured and sufficiently cooled, they are dumped by a powered dump cradle. The sand and castings in this position are dumped to a conveyor which feeds them to the vibrating shakeout. The dump cradle, operated by a hydraulic motor, is then reversed. The car with the bottom board is returned to the molder by gravity on a return track underneath the platform. The author's company holds patents on this arrangement.

The company's sand mixing unit employs a timing device for elimination of manual labor and human element so that the best possible control and uniformity of sand mixes can be obtained.

Core Room Improved

Before mechanization, the company used conventional bench core making. Installation of two horizontal ovens with overhead conveyor resulted in substan-



Iron, with composition and temperature adjusted in air furnace, is delivered to pouring area by lift truck.



12-hopper squeezer molding line with power driven mold conveyor gives 30 to 50 molds per hour, depending on the nature of the casting and the number of cores to be set.

tial savings in core making and core trimming. However, equivalent savings could be had by applying the proper mechanics to the mechanization and equipment itself. For example, a study of the operation of one of the core blowers showed that the core machines were used only 25 to 30 per cent of the time. On that basis, to produce the equivalent of 100 per cent of the capacity of the core blowing machine would require at least two more machines or 200 per cent more capital investment.

The type of box and the method of handling boxes and equipment are just as important, if not more important, to savings then the conveyor and oven itself. After a study of this job, machine blowing time was increased approximately 100 per cent.

Castings loaded at the foundry shakeout are subsequently dumped on an apron conveyor for spruing and chipping prior to annealing. The sprue is left on the apron conveyor and as the buckets pass the chippers and spruers, it is picked off the apron conveyor and thrown back in the buckets. These buckets, on their travel back to the foundry, pass the charging yard at the cupola and the sprue is automatically dumped at this location.

These examples of mechanization give some idea of the possible savings from the actual mechanization of a few departments. Most of these savings are based on the melting, core, and molding departments and result from installation of the equipment. In addition, production per square foot of floor area is greatly increased, sand control is better and the quality of the castings is improved.

Reduce Material Costs Too

In connection with the reduction of material costs, a great deal has been and a lot more can be accomplished. For example, sand reclamation is a source of savings, not only on the basis of the actual sand saved but by the elimination of handling cost of both bringing sand in and removing sand. Resin and other new binders should furnish a real incentive for greater study to develop a sand for both cores and molds that will result in lower costs and improved quality. Mechanical sand mixing has greatly improved sand quality and resulted in improved quality of castings. This, in turn, reduces finishing costs.

One of the points that should be emphasized is that in any substantial mechanization it is found that an operation is not one but anywhere from 5 to 25, or 50 smaller operations. These various units generally are not given careful enough study; insufficient importance is attached to them in studying the original layout. The result is an efficient unit except for one or two items of work. Too little attention is paid to the small items that really make the investment worth while.

Avoid High Capital Investment

The smaller foundry cannot afford to spend one or two million dollars to install molding and handling equipment, but must still sell in a competitive



market. To do this, cost of operations cannot be out of line, so savings and methods of cost reduction must be applied without high capital investment. This can be illustrated by improvements made in the author's plant. As an example, experience shows that a substantial reduction in grinding costs can be accomplished by using pressure fixtures, or special fixtures for long runs. The cost of these fixtures is small and the results are very gratifying; in fact a 20 to 30 per cent savings is not unusual. This is true whether or not the plant is mechanized. And savings can amount to 10 to 40 per cent on operations and even on total department costs.

Follow-Through Important

In the case of a plant that is mechanized, the importance of following through with the type of improved methods suggested cannot be over emphasized for large or small plants. Savings gained can almost equal savings accomplished originally through mechanization. A good mechanic or mechanical engineer with ingenuity and initiative, with cooperation of foremen and top management, can accomplish these savings.

Savings of this type made on individual jobs, operations, or piece of equipment are usually permanent and not affected by the operations of other equipment. For example, on the squeezer line there are many pieces of equipment, including the elevators, conveyors, shakeout, sand mill, and all the electrical equipment and automatic equipment that goes with it. There are 12 molders on this line, and all it takes to stop 12 molders from working, plus the shifters, pourers, shakeout man, and spruers and chippers, would be one switch going out, one elevator belt breaking, or any one of a number of small items failing will stop that type of equipment.

Besides the possible mechanical failures that cause downtime, other considerations are material handling, material delivery, and service to the mechanized lines; such as, cores, sand, metal delivery, pattern changes, pattern and flask repairs, etc. Each contributes anywhere from ½ to 3 or 4 per cent downtime during a shift, and by the end of a shift, totals 10 to 15 per cent.



Hydraulic slinger operated by man at left produces 80 to 100 molds per hour, is used for heavy cope and drag work.

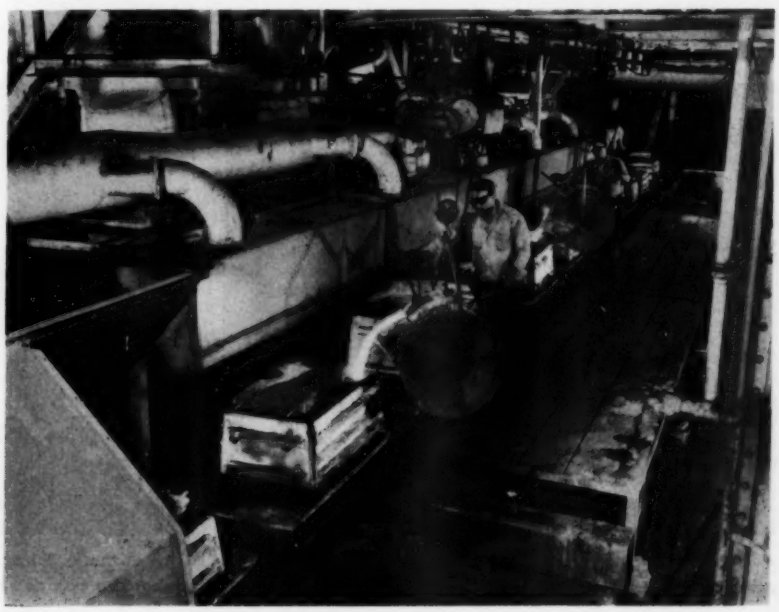
Don't Underestimate Downtime

In laying out a mechanization job, one would originally figure the capacity for sand and pouring and the possible production per molder per hour. On that basis, reduction in costs expected to be realized from the capital investment involved would be estimated. Without careful study, one would figure about five per cent downtime, but it is probably closer to the 15 per cent mentioned previously. The result is another 5 or 10 per cent unanticipated downtime, which in turn is loss of tonnage or production. This tonnage or production based on, for example, 1,000 tons of castings per month would be a loss of 100 to 150 tons of castings. At today's selling prices this is a substantial amount of money.

Besides that, after budgeting man-hours for the

Before (left)—Pouring off in the old foundry before mechanization.

After (right)—Pouring of squeezer molds as well as the slinger molds shown being poured here is mechanized for maximum efficiency and minimum fatigue of pouring crew.

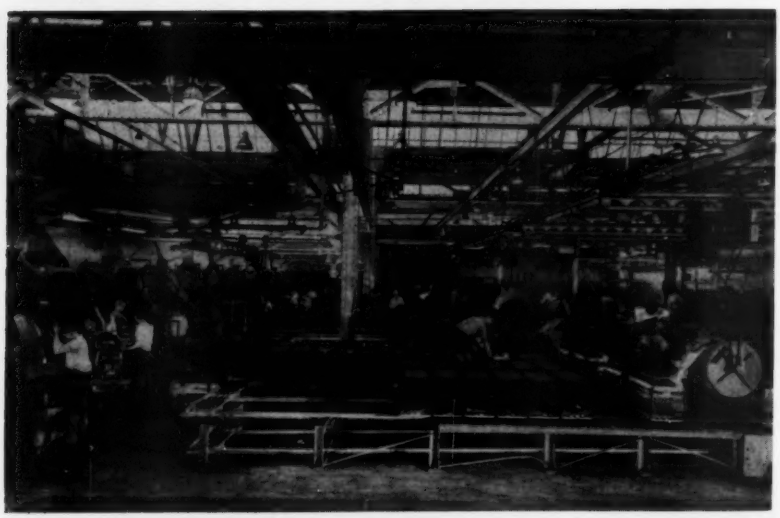




balance of the departments on the basis of forecasted tonnage, a plant finds itself with enough men to finish 1,000 tons of castings but winds up with 850 to 900 tons. This again increases costs in subsequent departments. It is easy to see how costs of mechanization—costs not originally considered—can run up. Thus, every mechanized unit in the plant is in effect part of another unit and any difficulty on mechanized lines anywhere will have an effect on the balance of the operations. This points up the importance of better than usual scheduling of materials to the mechanized lines and also emphasizes the great need for and the importance of a good program of preventive maintenance. These are increasingly important, the more mechanized a plant is. Considering the problem of maintenance alone, it is known that a better maintenance department will produce substantial savings, in increased tonnage due to less downtime, in replaced parts costs, and in total operating costs.

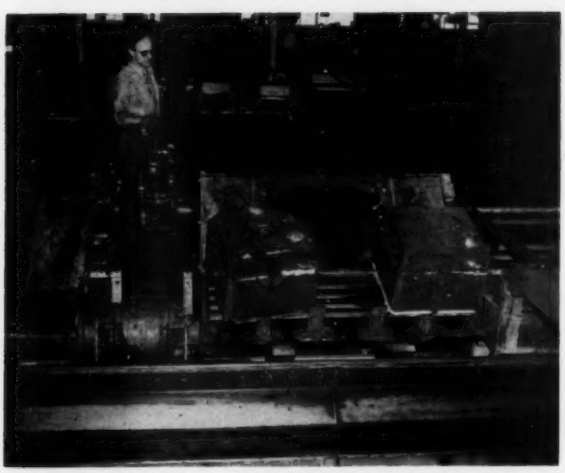
Two Approaches to Maintenance

There are two approaches to the maintenance problem. One is to have a so-called repair department pro-



Above—Molds slide onto shakeout grate automatically as conveyor car is tilted mechanically.

Left—22-station pallet line rounds out molding facilities and provides flexibility not available through squeezer and slinger units.



Power dump cradle empties pallets onto conveyor which carries castings and sand to vibrating shakeout.

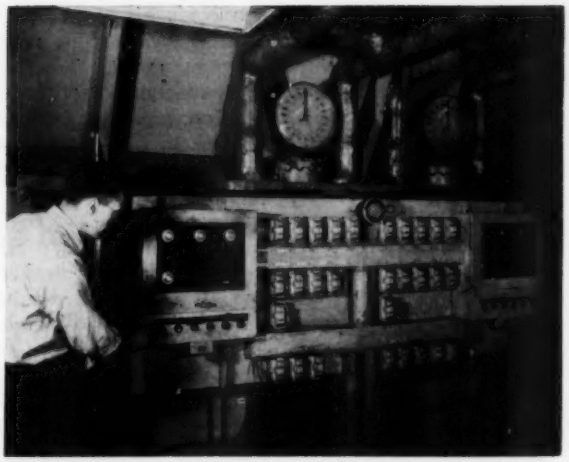


Automatic dump cradle in return position. Pallet with bottom boards returns to molder on under-floor track.

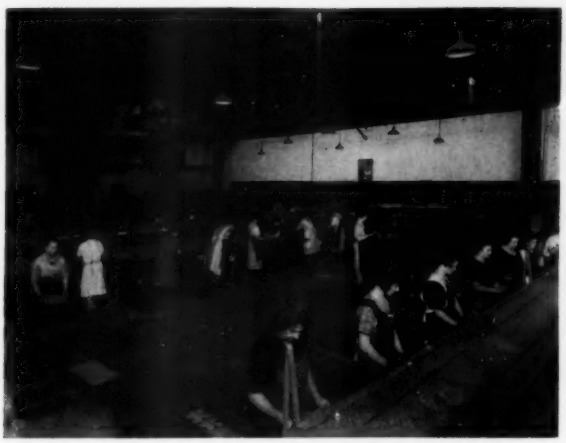
vide breakdown maintenance. The other is to have a maintenance department which will provide maintenance service to prevent breakdowns—in other words, a preventive maintenance program. Such a program can be carefully planned as to labor and material requirements and would consist of:

1. Inspection by a competent inspector.
2. Daily maintenance-work schedules based on the inspector's findings. (Work Order System.)
3. Creation and operation of an equipment log.
4. Program of regular replacements of expendable material.
5. Study of each equipment failure with a view to determining the cause of the failure and then taking the necessary steps to correct it.
6. Instructions to operating personnel.

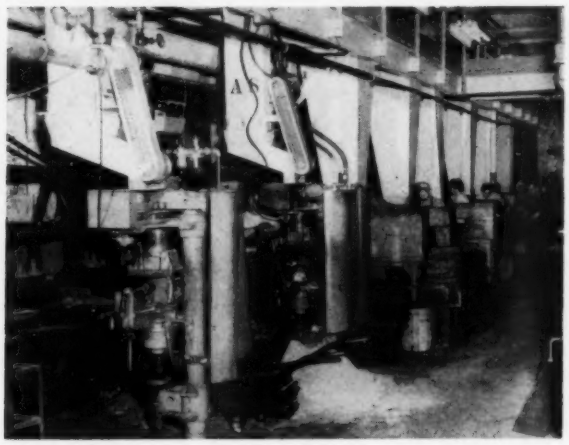
Some interesting results of ratings to determine the effective use of labor indicated that it was only 65 per cent. The primary cause of loss was a managerial problem rather than poor effort on the part of the workmen. The largest single loss factor was inaccurate and insufficient information on work orders. This shows the importance of *improving work orders* and developing more effective planning and scheduling. This is a continuing project.



Controls for two muller-type sand mixers automatically eliminate manual labor and human element of sand control.



View in old core room before modernization shows production of cores by hand; baking was done in batch ovens.



Same core room as at left following installation of two horizontal, conveyorized ovens and core blowers.

The foundry industry is following essentially the pattern established in the automotive industry and industries supplying it. A study of the progress made in the automotive industry shows that originally the savings and increased production were the results of conveyor lines that moved a car from one work station to another. That, in effect, is the same procedure followed in the foundry.

The reduction in the cost of cars did not result from the reduction in labor and materials alone. The automotive people made their greatest savings by: (1) having suppliers mechanize their plants, (2) standardization and interchangeability of parts, and (3) competent industrial engineering and methods departments, which took one job at a time, made a thorough study of it, and reduced the number of operations and improved efficiency. In their machine shop, new machine tools were designed and built that gave a 100 to 500 per cent increase in production on one part. Research to develop new materials was also responsible for a great deal of reduction in costs.

The foundry industry is going through the same phases of production and cost reduction that the automotive industry went through some years ago. For that reason, savings possible in the casting industry through reduction in costs on individual jobs and operations are still very substantial.

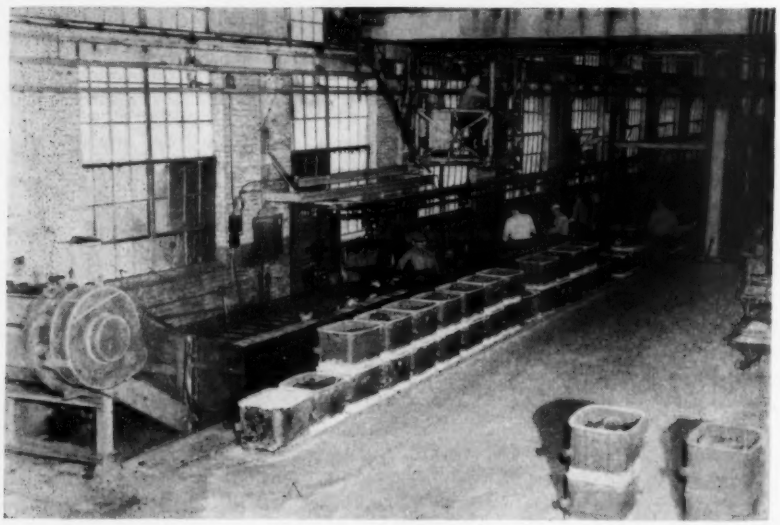
Where Do We Stand?

Has the foundry industry progressed or kept in step with other industries? Sand molding was started about 650 B.C., and the first molding machines were used in about 1890! The beginning of foundry mechanization came only about 60 years ago. Look again at the automotive industry. In about 1915 that industry started working on production methods and has made extensive progress in 30 to 40 years. They didn't make that progress by just putting in conveyors for assembly lines; they made a complete study of the problem.

The automotive people then called in material supply engineers to determine what material best fitted

that particular job. They called in machine tool builders and other tool and die engineers to determine what machine tools to use, how many operations could be eliminated, how many could be combined, and with each step or change they insisted on better quality and close tolerances. While tolerances of plus or minus 0.003 to 0.005 in. were considered close, now 0.0002 to 0.0005 in. are not uncommon in production.

The automobile manufacturers called in the engineers from the steel mills. They explained their problem and they developed a good deep drawing steel so the entire turret of the body could be drawn in one piece eliminating all of the fitting, welding, soldering, etc., that had to be done with the old material. The result again was a much better product at lower cost.



Buckets back of chipping and spruing conveyor carry sprue to charging yard and automatically dump near cupola.

The tire companies are another example of an industry that has developed and progressed to keep pace with the great progress in design, methods of manufacturing, and development of new materials.

The foundry industry should take note of the advances that have been made in forging and stamping techniques . . . fabrication and weldments of many parts that were previously castings . . . parts made of powdered metal, and with the hydrogen brazing methods. These units generally are cheaper in cost than castings and usually reduce machining costs because the part is held to much closer dimensional tolerances.

Foundry Must Compete

These things seem to point up the importance of the foundry industry finding materials and methods that will, at a competitive cost, maintain a finish and dimensional tolerance on castings which will give competition to the materials mentioned.

Some work has been on shell molding and pressure molding, but there is much to be done in order to produce at a cost that will be competitive. In addition, if the industry applied enough good mechanical study and research to result in 15 to 20 per cent of cost being labor, instead of 50 per cent as it is now, both the individuals and the industry as a whole would benefit.

The field of material handling alone is one that has potential savings beyond the comprehension of most foundrymen. Sand reclamation is significant because it saves on the initial cost of the sand, the excessive handling cost, for unloading and storing cost, and cost of hauling away refuse sand. Coke handling (for the foundries that use coke) is another phase of material handling which if properly done would eliminate the cost of handling and greatly improve melting by the elimination of fines. The foundryman buys good coke, then proceeds to break up about 10 per cent of it by using clam shell buckets or some other means of unloading that causes the coke to be broken.

Mechanics Determine Success

In conclusion, it should be remembered that it's the mechanics of each individual job that determines whether or not mechanization is successful. To overlook them is to literally step over dollars to pick up nickels.

One phase of production badly in need of this kind of study is molding materials—such as resin binders and equipment for handling it—that will produce a casting at a lower cost than the present sand casting, and hold dimensional tolerances to a fraction of what is done now. Another is improved methods of cleaning castings. Present cleaning does not do a satisfactory job. Tests indicate that after pickling, which removes all sand and scale, tool life was increased 50 to 150 per cent. Improved methods for removal of metal such as gates, riser connections, fins, etc., for the reduction of finishing costs, is in order.

A serious study of pattern and core box equipment should be made to eliminate fins, parting lines, etc., to reduce finishing costs, and to develop composite molding practice wherever possible to reduce molding costs and increase yield. By using time and motion study, work simplification, quality control methods, research, and good preventive maintenance, the industry can greatly improve its costs and its products.

Finally the industry can obtain graduate engineers from the universities through the FEF program. These engineers have taken studies to specifically fit them for the foundry industry; they can be the answer to many foundry problems.

INDUCED FLOW PROCESS OF BAKING OIL-BONDED CORES

By

C. T. Marek* and R. J. Wimmert**

Introduction

Purpose of Project. Published reports and experiments confirmed by the author indicate that the time required to drive the moisture out of a core requires at least two thirds of the baking time. For example, in 90 min of drying time, it was found that the center of a core rose to the vaporization temperature of water (212 F) and remained there for 65 min.

Developments in the use of oxidizing agents as additives to core oils and sand mixtures, have proved beneficial in reducing core drying time. It must be recognized, however, that this improvement is limited to the heating cycle which takes place after the moisture is driven off.

The potentiality for greatest success in reducing the time of drying lies in the phase in which moisture is being driven out of the core. The problem resolves itself to the following desirable accomplishments.

1. To get heat inside the core in order that evaporation of water may be accelerated.
2. To find a method of removing steam from the core as rapidly as possible.
3. To get an adequate supply of oxygen into the core.

In order to appreciate the advantage of drying a core by forcing heated air through it, the author believes it desirable to investigate the transformations taking place in conventional core production, and to study the function of the ingredients.

Fundamentals Involved In Conventional Core Production

Bonding Principles of Oil and Water in a Green Core. When a core mixture contains little or no moisture, its green strength can be attributed to the oil content. This, however, is not sufficient to produce the desired strength customarily expected in core production. If no cereal is used, green strength is almost entirely dependent on the bonding action of water. To develop effective bonding action, sufficient water must be present to wet all grain surfaces. Anything less, produces results even less effective than the feeble action of core oil. Core sand bonded properly with water for maximum green strength is adversely affected by the oil bond. This may be illustrated by mixing sand and water, and observing the green strength before and after oil is added. A number of observers of this phenomenon explain this action by the lubricating action of oil which causes slippage of sand grains. Under this condition, bonding forces no longer are aided by frictional resistances between sand grains.

A number of circumstantial observations lead one to realize that the effect of moisture in core production is far reaching and deserves even more consideration than heretofore afforded it. For example, it was found that a green core has greater density when no water is used. This would lead one to believe that moisture not only wets and holds sand grains together, but spreads the grains apart. This same phenomenon can be substantiated by the fact that green permeability of core sand is least when no water is used. Further evidence is derived from observations that an oil-bonded core with no moisture will produce a dry sand core of greater strength than an oil-bonded core containing water in its formula. It is believed that the moistureless core is cemented together more strongly because of greater surface contact between grains.

It is the author's belief that the ionic theory of bonding silica sand with clay and water, may be applied to a silica and water mixture. Silica grains (quartz) may carry unbalanced electrostatic charges. In the presence of water molecules which tend to hydrolyze according to reactions $H_2O \leftrightarrows H^+ + OH^-$, the silica particles will attract hydroxyl (OH^-) ions and adsorb them on their surfaces. The negative unbalance attracts a layer of positive (H^+) ions which surround each particle. The quartz particle with its surrounding layers of ions is referred to as a micelle.

Due to the influence of the ions in one micelle on other micelles (alike charges repel and unlike charges attract each other) the ions shift about to form dipolar positions as illustrated in Fig. 1. The clusters of alike charges of one micelle act as a unit in attracting clusters of alike charges of opposite polarity

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in other micelles. The composite of these forces is what produces green bond. The alike charges within each cluster, however, repel each other. The sum of these forces is what keeps the grains apart and produces less density in green sand, increases green permeability and reduces the dried strength of a core when water is an ingredient in the mixture.

Thermodynamics in Core Drying. The rate of conduction of heat in a green core is obstructed by a moisture barrier which recedes toward the center as moisture is converted to steam.

At some particular point in the heating operation of a core, a situation may exist that the temperature of its center is at or near the vaporization point of water and its outside surfaces are above the vaporization temperature. Somewhere between these two extremes is an area where moisture is being evaporated from the core. The temperature gradient in the core drops sharply from the exterior temperature to the point where vaporization takes place.

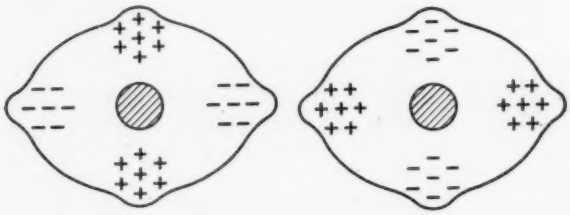


Figure 1

Most of the heat that is conducted into the core is absorbed by the mixture at the barrier and is consumed in the conversion of water to steam. In spite of any effort to penetrate this barrier by raising the oven temperature, the core temperature remains constant where vaporization takes place. Increased heat will merely accelerate vaporization.

From the standpoint of heat transmission, moistureless sand can be considered a good heat insulating material, its conductivity being 0.188 Btu/hr/ft²/in. at 68 F. For purposes of comparison the heat transfer properties of other insulating materials commonly used for their low heat transfer properties are as follows: fused magnesia 0.114, mineral wool 0.27, fiber insulation 0.24.

Not only is it difficult to get heat into a core because of its poor heat transfer properties, but when it does penetrate the core, it is immediately lost by being dissipated to steam with no effective change in core temperature. To illustrate this, the following computations compare the energy needed to raise core sand from 60 F to 400 F, with the amount of energy needed to evaporate and expel the moisture in the mixture.

Estimating the specific heat of sand in this temperature range to be 0.23, it would require the following heat energy to raise 1 lb of sand from 60 F to 400 F.

$$0.23\,(400\text{–}60) = 78.2 \text{ Btu}$$

To raise 1 lb of water through the same temperature range at a constant atmospheric pressure, standard steam tables disclose a heat energy of 1179 Btu is needed.

A core containing 6 pct moisture would require the following heat energy to raise its temperature to 400 F.

$$0.94 \text{ (lb sand)} \times 0.23 \left(\frac{\text{Btu}}{\text{lb F}}\right) \times (400\text{–}60)\,(\text{F}) = 73.5 \text{ Btu}$$

$$0.06 \text{ (lb water)} \times 1179 \left(\frac{\text{Btu}}{\text{lb}}\right) = 70.7$$

Total energy required 144.2Btu

It, therefore, requires approximately twice as much heat to bring a moist core up to the required temperature as compared to one without moisture.

The effect of a high increase in volume when moisture is converted to steam, may be a significant factor in core drying and should be considered in this discussion. One pound of water (0.02 cu ft) under constant standard atmospheric pressure will expand to 26.82 cu ft of saturated steam at 212 F, and when raised to a superheat temperature of 400 F its volume will be 34.65 cu ft.

When this expansion takes place in a core, it is conceivable that a slight rise in pressure may take place, especially if the permeability of the sand is low. This would cause slight changes in vaporization temperatures, heat absorption properties, and volume of expansion. In view of all factors affecting the drying of a core, these changes appear to be only academic in interest.

An important effect of the expansion of moisture to steam is that this volumetric change accelerates the flow of gases through the core. Some of the steam may flow to the center of the core and if that area is below the saturated temperature of the steam, it will give up some of its heat. This may lead to a supersaturated condition which leads to condensation. The result of this action is a soggy core center such as is often experienced in core drying.

Most of the steam will pass out of the core in the direction of low pressure. As it passes through the heated dry section it picks up superheat and continues expanding until it is exhausted to the oven atmosphere. This amounts to a heat counterflow which tends to reduce conductivity below the natural capacity of sand.

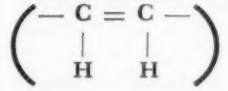
The degree of air movement in the oven will determine how rapidly the blanket of humid air is dispersed from the core. If the core oven is not sufficiently vented, the oven capacity for moisture absorption is reduced and drying efficiency is lessened.

As the moisture barrier moves inward the temperature of the outer portions of the core approach the oven temperature. Somewhere along this temperature gradient, the volatile portions of the core oil are distilled off. As in the case of water forming steam, the distillation process is an endothermic reaction which picks up heat from the sand and expels it to the atmosphere in a counterflow action. Thus we have another conteraction to heat conductivity.

Chemistry of Core Drying. As the surface and near surface portions of a core acquire favorable drying conditions, a change begins to take place which hardens the sand. Although distillation of volatile oils

takes place prior to hardening, core drying cannot be attributed to thickening of oil. The process of changing a liquid oil to a solid is a chemical reaction which may or may not depend on the presence of oxygen in the reaction.

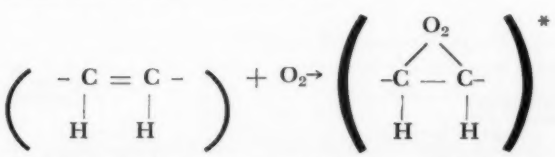
In the liquid state, drying oils (mineral, fatty or synthetic) are composed of molecules, consisting of long carbon chains which may vary from 12 to 24 atoms in a group. Each carbon atom has 4 bonds, 3 of which may be attached to other atoms such as hydrogen, oxygen, etc., and the fourth bond is shared with an adjacent carbon atom. In certain cases pairs of carbon atoms may share two bonds instead of one, forming an unsaturated hydrocarbon as follows:



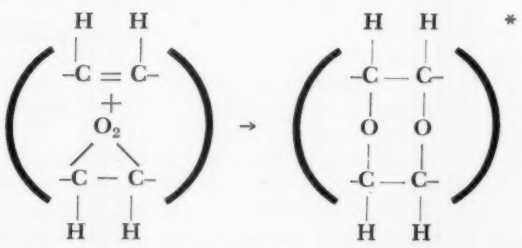
*. The degree of unsaturation depends on the number of double bonds in a chain. Actually unsaturation represents the lack of stability, or rather the capacity to combine with other molecules. This molecular consolidation to form polymolecules is commonly referred to as polymerization.

When in a liquid state, the molecules of oil possess the necessary kinetic energy to move freely and at random with no definite boundaries. When converted to the solid state, their activity changes from migratory to vibratory motion. Whereas we ordinarily associate solidification of liquids with removal of heat energy, solidification in this case is attributed to transformation of kinetic to potential energy by increasing the weight of the molecule. The resulting decrease in molecular activity transforms the oil from liquid to solid.

In the study of the drying of unsaturated oils in the paint industry, polymerization is preceded by an intermediary reaction with oxygen. The oxygen attaches itself to carbon atoms at a point of unsaturation. In so doing it removes one of the double bonds from the carbon atoms as follows:

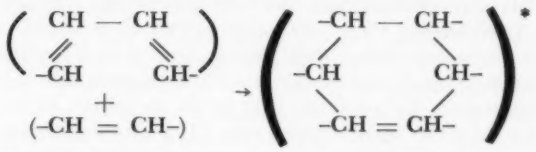


This new product will combine with another unsaturated molecule to form a polymer. The oxygen atoms in the primary reaction are locked in between two regular molecules as follows:



The weight of the new molecule is greater than the two original molecules by the added oxygen atoms. Thus, polymerization, in this case, increases the density of the oil.

Under certain conditions as when the oil temperature is elevated, oils may harden without the aid of oxygen. This process is used to partially polymerize drying oils, and is referred to as "oil bodying." The reaction takes place between unsaturated molecules by divesting a pair of carbon atoms of their double bond and sharing it with carbon atoms of another molecule without an oxygen interlock. The following illustration shows a double bonded functional group of one molecule polymerizing with a single bonded functional group of another molecule.



The product of this reaction differs from the previous one in that a six-member all carbon ring is formed. This type of product is considered the most stable configuration known in organic chemistry.

A single hydrocarbon chain may possess one, two or three double bonds, and sometimes even more. Unsaturated hydrocarbons containing single isolated points of unsaturation, show very little activity and are not suitable for drying purposes. An ideal oil is one which contains functional groups having three points of unsaturation as follows:

$$(C = C - C = C - C = C)^{*}$$

Linseed oil, tung oil and other commercial driers contain such complicated structures as these. They form endless chains which branch out into the matrix and produce super-giant tridimensional polymolecules which are very stable, infusible and insoluble.

The mechanism of drying and bodying can be described in four phases.

1. **The Induction Period.** In spite of attempts to remove all anti-oxidants, commercial drying oils contain certain amounts of natural inhibitors which prevent polymerization. This period represents the time needed to reduce these inhibitors.

2. **The Orientation Period.** Under normal liquid conditions it is unlikely that polymerization will take place because of the stability effect of the fast moving molecules. However, under core drying conditions the heat activated double bonds become more sensitive and consequently have greater tendency to combine. Also, in an oxidizing atmosphere, the oxides slow down molecular motion. This offers the functional groups an opportunity to be oriented for favorable polymerization.

3. **The Molecular Rearrangement Period.** If the double bonds are isolated from each other by carbon atoms with single ties as illustrated below the tendency for polymerization is greatly reduced.

$$- C = C - C - C = C -$$

An ideal situation is one in which the double bonds are located in "conjugated" positions as in the following illustration.

$$- C = C - C = C - C -$$

Under the proper conditions of temperature and

* Only a small portion of a complicated formula is shown to simplify explanation.

time, isolated double bonds will shift to their conjugated positions and produce molecules more favorable for polymerization.

4. **Period of Molecular Growth.** When a functional group of three conjugated double bonds polymerizes, the product contains one more double bond than was contained in an original combining group. Thus, after a slow start, the reaction has a self-propelling acceleration because it gains in double bonds after each combination.

A molecular structure may consist of three functional groups. Therefore if this reaction of increasing acceleration occurs on three different parts of a molecule at one time, it is quite plain that growth can be swift and solidification can take place quite rapidly.

Final Drying Phase. When the external portions of a core become dried and polymerization of the oil is complete, further exposure to oven temperatures will disintegrate the core. At this point, the center of the core may still be in a green state. This is the dilemma in which the foundryman constantly finds himself. Ordinarily he will permit the exterior surfaces to be overbaked in an attempt to partially overcome the undesirable green center. In some instance, when it is very important to get a uniformly baked core, he will reduce oven temperatures and increase drying time.

The disadvantages of a nonhomogeneously dried core are many. It lacks hardness and may lose dimensional accuracy because of abrasion in handling, or erosion by molten metal. It lacks strength and may have to be reinforced to reduce breakage. It lacks collapsibility because the molten metal in the mold must first bake the interior of the core before it will disintegrate and collapse. It produces more gas because of the presence of moisture and saturated oils in its center which would have been distilled off if the core were properly dried.

These are the problems confronting the foundryman in his daily production of cores.

Induced Flow Process

Principles of the Induced Flow Process. To overcome the inherent difficulties encountered in conventional core drying, the Induced Flow Process was conceived and investigated. The object was to force heated air through the core by developing a pressure difference between the top and bottom of the core plate or drier.

A study of the probable physical and chemical changes taking place under this new condition, indicated that the basic principles of the process were sound. Unlike the conventional method of exposing only the surface sand to heat, the hot oven air would contact sand inside the core with equal effectiveness. The retarding effect of increased vapor pressure due to steam formation in conventional core drying would be vastly reduced because of air flow through the core. The fact that heat flow would be in the same direction as air flow, the counterflow problem as described in conventional core drying would be completely eliminated. Removal of distilled saturated hydrocarbons would be performed as effectively as in the removal of moisture. In the polymerization phase,

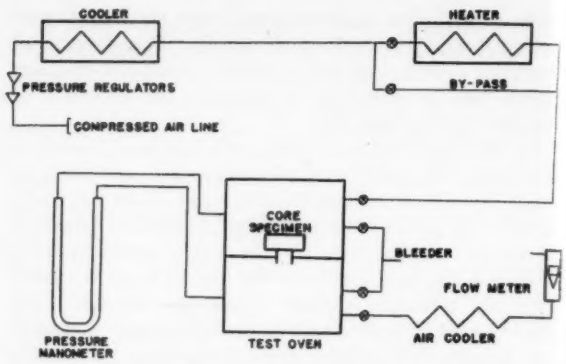


Fig. 2—Schematic flow diagram of experimental oven and equipment.

the center of the core would be adequately supplied with oxygen and heat, which should produce a core of homogeneous consistency. The dried core should produce less gas and it should have greater strength than a conventionally dried core because of uniformity in drying. Considering the prospect that the center of the core would be thoroughly dried, it was conceivable that when metal surrounds the core in a mold, core collapsibility would be superior to that of the conventionally dried core.

In addition to all these possibilities, the most advantageous prospect of all was that of reducing oven time by a considerable amount.

The Purdue Research Foundation therefore deemed it advisable to grant funds for the services of a graduate assistant to verify these conclusions and to further investigate the process.

Description of Equipment for Induced Flow Process of Core Drying. The equipment shown in the flow diagram of Fig. 2 was designed and constructed specifically for this investigation. The pressure of air, obtained from the compressed air line servicing the laboratory, was decreased from 78 psi to 3 psi by two

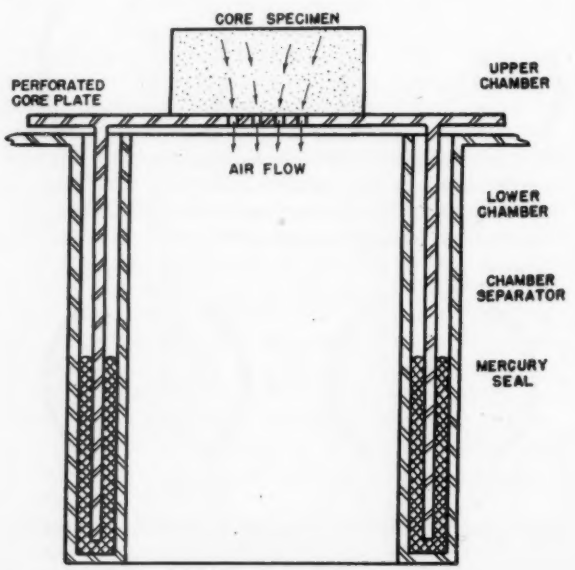


Fig. 3—Core plate mercury seal detail.

regulators shown on the left of the diagram. The air cooler consisted of a series of pipes submerged in brine solution and held at a temperature of 32 F. By cooling the air to a temperature below the saturation point of atmospheric conditions, moisture was condensed in all cases, and saturated air at constant temperature slightly above freezing of water, was supplied for the experiment.

The cool humid air was piped to a gas-fired heater and a portion of it was passed through the heater to raise the temperature of the air to 500 F. The remainder of the air was by-passed around the heater to facilitate the control of air temperature in the upper chamber of the oven.

The oven was made in two sections: the upper chamber and lower chamber. The only connection (except the by-pass) between the two chambers during drying operation is the core specimen which rests on a special core plate. Detail of sealing the core plate is shown in Fig. 3. Pressure differences across the core specimen caused the heated air to pass from the upper chamber, through the core and out to the lower chamber. By manipulation of a bleeder valve, pressure differences between the two chambers could be adjusted.

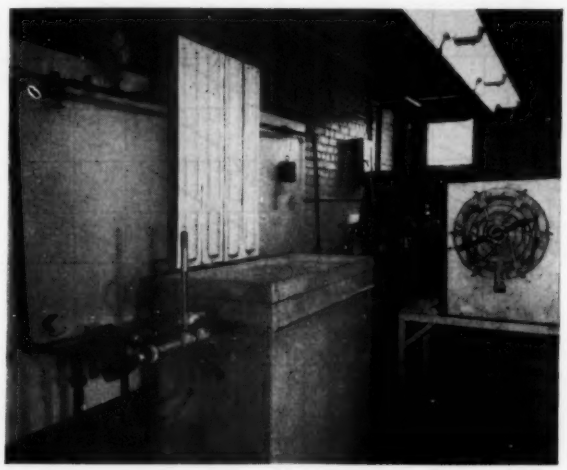


Fig. 4—Experimental equipment designed to dry cores by the Induced Flow Process. Air is dried in the refrigerator unit at left, heated in the furnace in center and then passed into the oven where cores are dried.

Figure 4 shows the over-all view of the equipment and Fig. 5 illustrates a core being removed from the experimental oven. The door on the oven is designed for rapid opening and closing, in order that the core may be removed or inserted without delay.

To calibrate the rate of air flow through the standard core at various pressures, the exhaust air from the lower chamber was cooled by passing it through a series of pipes before it reached the flow meter.

Procedure of Test. All test specimens were made with a standard A.F.S. Tensile Test Core Box. The cores were made according to standard procedures recommended by the A.F.S. A standard test core mixture of 100 parts of sharp (lake) sand (A.F.S. Fineness 57), 4 parts of water and 1 part of raw linseed oil (by weight) was used in the experiment. In an attempt to avoid unnecessary variables, the entire oil supply was obtained in a single bulk container. Sufficient sand was also set aside for the entire experiment. Standard test procedures as recommended by the A.F.S. were used wherever possible.

The above proportions were selected on the basis of relative good green strength obtained with a minimum amount of water for tempering. Cereal binders were avoided in order to reduce variables in test results. It was felt that because of the sensitivity of cereal-bonded cores to atmospheric humidity, extreme care would be necessary to avoid damaging effects of moisture pick-up by the dry sand cores. Even with the oil-bonded core, a desiccator was used wherever possible. Due to the long period of time over which the experiment was performed, it was felt that linseed oil would show less aging than commercial oils.

The mixtures were prepared in a 24-in. Simpson laboratory mixer in accordance with A.F.S. recommendations.

The selection of a parameter to evaluate the extent which the test cores were baked, proved rather difficult. After some consideration and evaluation of available equipment, it was decided that tensile tests augmented by core hardness tests would be used to evaluate the degree of drying. Tensile tests were performed on a motorized universal sand strength machine. A pre-test load of 16 psi was used to compensate for elongation of the specimen. Attempts were made to obtain core hardness readings on both sides of the broken sections of the tensile specimen.

In order to make comparisons with the convention-



Fig. 5—Test core being removed from experimental oven, illustrating position of core on special drier and quick acting door in open position.

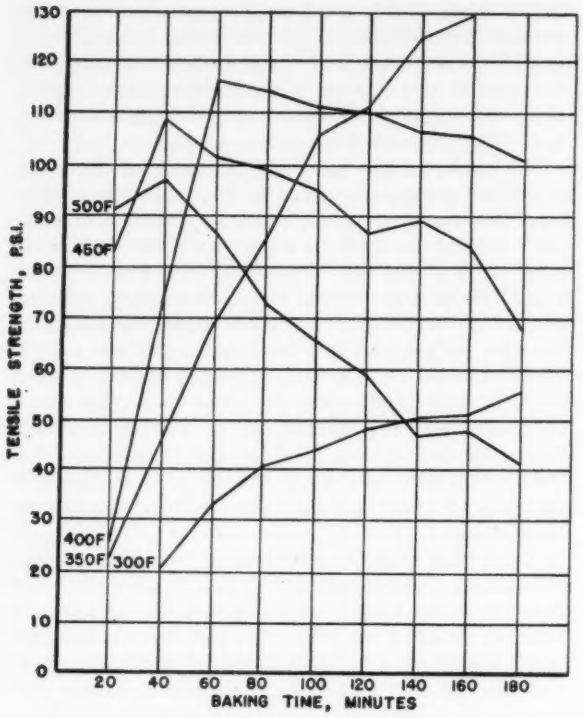


Fig. 6—Tensile strength, Baking Time curves for conventionally baked cores.

al drying process, cores were dried in a Dietert test core baking oven at temperatures ranging from 300 F to 500 F in increments of 50 F. The time of drying was varied from 10 to 180 min for each temperature. Based on these results it was decided to consider a core "baked" when its tensile strength reached 100 psi. The graph of Fig. 6 illustrates the time-temperature characteristics obtained for the standard core mixture.

After some experimentation with temperatures in the experimental oven, it was found that high temperatures yielded best results. All experiments with induced flow were therefore performed at 500 F, the maximum temperature available. Time was varied from 5 to 12 min in 1-min intervals. The pressure across the core was varied from 6 to 15 in. of water. Pressures higher than 15 in. of water were not used because small amounts of sand passed through the vent holes in the core plate, leaving small pockets in the core. It is believed that further experimentation with core plate vents and with core mixtures would permit higher pressures.

All tests were performed on 5 samples and all results were used in a statistical analysis.

Discussion of Data. Figure 7 in a graphical representation of data listed in Table 1. Each curve represents the tensile strength of cores dried by the induced flow process, holding the time constant and varying the pressure difference across the core from 6 to 15 in. of water in increments of 1 in.

The rate of increase in tensile strength with respect to an increase in pressure may be judged by observing the slope of the curves. The 5-min curve, for example, is quite flat and therefore tells us that the response of strength to increased pressure is low. As drying time is increased, the rate of change in tensile strength continues to improve up to the 7-min drying cycle. The slope of the 8-min curve is not as steep as the 7-min curve, but there is little practical difference between them. As drying time is extended beyond 8 min, the slope of each succeeding curve becomes more nearly horizontal. Consequently the response of tensile strength to pressure change becomes less when longer drying cycles are employed.

Within the range of this investigation it was found that at any given pressure, the cores which were exposed to heated air the longest, developed the greatest strength.

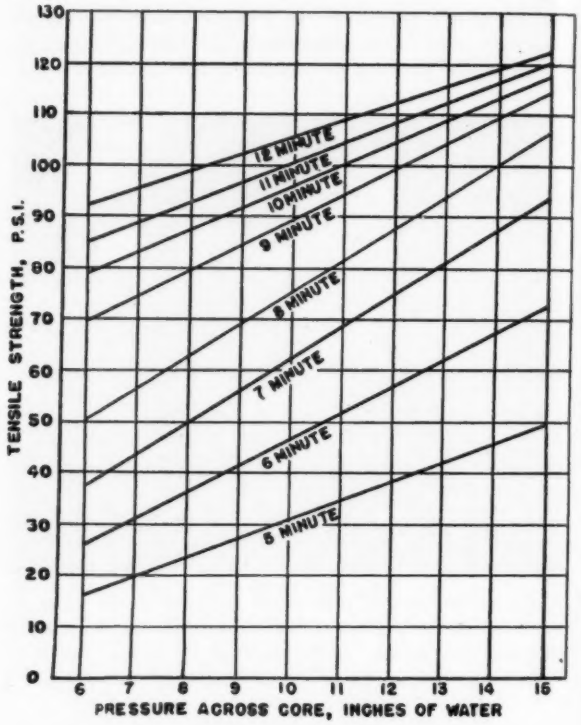


Fig. 7—Tensile Strength vs Pressure Curves.

The curve of Fig. 8 converts pressures across a standard core specimen, to rates of air flow through it. By this means the graph of Fig. 9 was developed, relating tensile strength to the total volume of air used in the drying of the core specimens. As in Fig. 9, the slope of these curves represent rate of change. In this case the rate of change in tensile strength is correlated with total air volume. The 7-minute drying cycle has the steepest slope and consequently represents drying conditions at highest efficiency. This means that by increasing the rate of flow through a core at a 7-min drying time, the corresponding rise in strength is higher than at any other drying time.

It is important to note that at any constant volume up to 11.2 cu ft, the strongest core is obtained from the longest drying cycle. Beyond 11.2 cu ft, the opposite is true; the curve representing the longest drying time, produces the weakest core.

Results obtained from standard cores dried in a

conventional laboratory type core oven, are listed in Table 2 and illustrated graphically in Fig. 6. For purposes of comparison with induced flow, it should be noted that the 500 F temperature curve reached its maximum strength at 97.6 psi in approximately 40 min. The same strength was acquired by the induced flow process in 8 min after passing 10.5 cu ft of air through the core at 500 F. This represents a reduction in drying time of 80 per cent.

The peak strength at 500 F in the conventional drying oven was achieved at the expense of burning the exterior surfaces of the core. Specimens dried by the induced flow process did not appear to be burned and the cross-section seemed to be uniformly dried.

Limiting the conventional core drying method to a reasonable drying time of 1½ hr, a comparison with the induced flow process revealed that the new technique would dry cores in much less time and produce stronger cores.

The results of the hardness test by induced flow, were not expected to be too reliable because only a short test stroke could be made with the hardness tester on the broken pieces of the tensile test core. Nevertheless, by noting the trends on the graph of Fig. 10 and Table 3, some significant points are obvious. Points on graph in Fig. 9 are average hardness of cores under a given condition.

1. The curves representing drying times of 5, 6, 7 and 8 min move upward under all the drying conditions. Also the longer drying time, produced the harder top core surface.

2. The 9-min curve begins to slope downward slightly and then plunges steeply. This should indicate that the surface of the core had passed its optimum strength. The 10- and 11-min curves are below maximum hardness and continue dropping at a greater rate than the 9-min. The 12-min curve drops even more rapidly.

Figure 10 and Table 4 represent the hardness characteristics on the bottom of the same cores produced by the induced flow process. The following observations are called to the reader's attention. Data were recorded and plotted same as for Table 3 and Fig. 9.

1. All the curves slope upward. This would indicate that the bottom surface is not overbaked.

2. The 8- through 12-min curves are bunched together and their slopes are nearly horizontal. This would indicate that the bottom hardness is near its peak.

TABLE 1—TENSILE STRENGTH OF TEST OVEN CORES AT DIFFERENT TIMES AND PRESSURES

	6 in.H_2O Pressure	7 in.H_2O Pressure	8 in.H_2O Pressure	9 in.H_2O Pressure	10 in.H_2O Pressure	11 in.H_2O Pressure	12 in.H_2O Pressure	13 in.H_2O Pressure	14 in.H_2O Pressure	15 in.H_2O Pressure
5 Min	22.0	22.5	25.0	25.5	34.5	34.5	41.5	42.5	48.0	62.5
	17.5	20.0	20.0	28.0	27.5	34.0	39.0	43.5	40.0	57.0
	19.0	22.5	23.5	28.0	30.0	31.0	40.0	45.5	45.0	53.0
	20.0	22.0	20.0	29.0	28.0	30.0	37.0	45.0	44.0	48.0
	22.5	21.0	23.0	26.0	26.0	32.0	39.5	42.0	45.0	43.5
6 Min	24.0	29.5	32.5	39.0	45.5	49.0	55.5	59.0	75.5	68.5
	28.0	31.5	35.0	41.5	47.0	56.0	60.5	65.0	80.0	72.0
	29.0	34.0	37.5	42.0	47.5	50.5	55.0	64.0	60.0	69.5
	28.5	28.0	34.0	44.5	43.0	53.0	57.5	62.0	67.0	74.5
	32.5	30.5	34.5	43.5	40.0	52.0	56.5	58.5	65.0	75.5
7 Min	40.0	45.0	50.0	50.5	64.5	67.5	75.0	84.0	90.0	95.5
	38.0	42.0	47.0	50.0	62.0	69.0	78.0	78.0	95.0	92.5
	39.0	47.0	48.5	52.5	61.5	63.0	79.0	77.5	87.5	90.0
	40.0	47.5	47.5	55.5	64.0	69.5	71.5	82.0	87.5	88.0
	40.0	45.0	50.0	52.5	60.0	76.0	74.5	85.0	90.0	94.0
8 Min	50.0	54.0	64.0	70.0	72.0	79.0	88.0	89.5	95.5	102.0
	49.5	58.0	65.0	68.5	73.5	84.0	91.5	93.0	103.0	101.0
	47.0	59.0	64.5	69.5	77.5	83.0	87.5	91.5	105.0	107.5
	49.0	55.0	66.0	72.5	72.5	85.5	89.5	92.5	104.0	110.0
	51.5	56.5	65.0	70.5	71.5	84.5	89.0	94.5	97.0	109.0
9 Min	71.0	78.0	82.5	86.5	93.5	95.0	100.0	103.0	111.0	113.0
	68.5	76.0	75.5	89.5	88.5	92.5	103.5	101.0	110.0	114.5
	68.5	74.0	76.0	85.0	98.0	94.0	101.0	102.0	105.5	114.0
	70.5	75.5	73.0	81.0	92.0	98.0	105.5	109.0	107.5	117.0
	73.5	77.5	71.5	83.0	87.0	89.5	106.0	99.5	109.0	115.0
10 Min	78.0	85.0	85.0	89.5	101.0	106.0	106.5	106.5	113.0	115.0
	79.0	84.0	88.0	93.5	97.0	102.5	105.0	109.0	114.5	117.0
	81.5	83.0	89.5	92.0	95.0	99.0	105.5	107.5	114.5	116.5
	80.0	81.0	89.0	93.0	96.5	99.5	109.5	110.0	110.0	119.0
	76.0	82.5	89.0	91.5	98.5	100.5	103.0	111.5	112.0	117.5
11 Min	84.0	89.0	95.0	98.5	102.5	104.0	110.0	113.5	114.5	118.5
	82.5	86.0	93.5	97.0	102.0	108.0	107.5	113.0	117.0	120.0
	87.5	90.5	92.5	96.5	105.5	105.0	112.5	114.5	116.5	121.5
	85.5	90.0	90.0	95.0	99.5	103.5	109.0	110.0	120.0	122.5
	87.5	87.5	94.5	98.0	98.0	105.5	105.5	114.0	118.5	118.0
12 Min	90.0	97.5	100.5	99.0	107.5	109.0	113.5	115.0	122.5	118.5
	91.0	96.0	99.5	101.5	110.0	111.5	115.5	115.5	121.0	117.5
	94.0	96.0	97.0	103.5	105.0	110.5	115.0	118.5	118.5	121.5
	92.5	94.5	101.5	103.0	105.5	108.0	111.0	117.5	119.0	121.0
	92.0	95.5	101.0	100.5	106.5	112.5	111.5	117.5	118.5	121.5

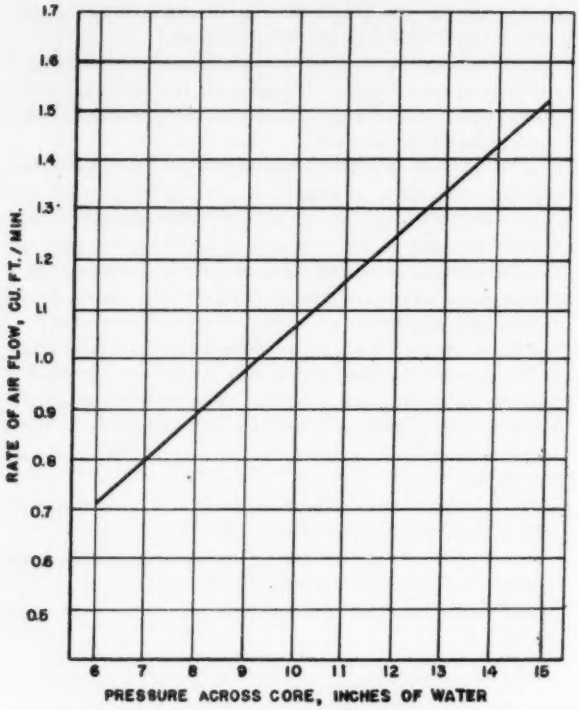


Fig. 8—Rate of Flow vs Pressure Curve.

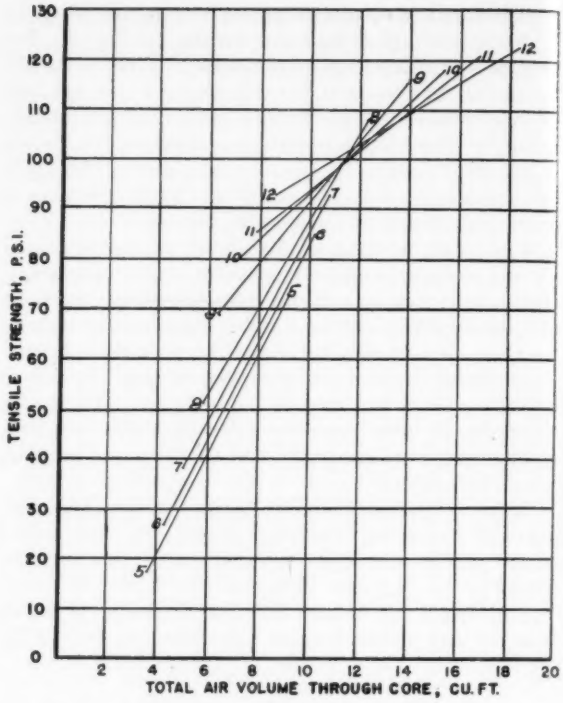


Fig. 9—Tensile Strength vs Air Volume Curves.

3. Although hardness of bottom surfaces lags behind top hardness, there is not a great difference between them. By blocking in the area which includes the majority of points, the pattern would present a core hardness of 70 to 76 for bottom hardness and 74 to 81 for top surface hardness.

Although the top surface hardness seemed to pass beyond its peak in some of the tests, the tensile strength curves do not indicate this trend. Because of the narrow margin in properties between the top and bottom, it is believed that some of the specimens were very near to their optimum strength.

What happens beyond the range of the experimental data is a matter of speculation. Data beyond 500 F and greater volumes of air at higher pressures were not feasible because of limitations of equipment and core mixture.

Conclusions

The Induced Flow Process of forcing heated air through cores was found to be superior to the conventional method of core drying in a number of ways.

1. The Induced Flow Process required only one-fifth of the time normally needed to produce a baked core.

2. A higher tensile strength was obtained by this method.

3. The core was thoroughly dried throughout.

4. Although the hardness tester indicated signs of surface deterioration at the end of certain drying cycles, no surface burning was apparent.

5. Within the limitations of the experiment, linear relationship was found to exist between the tensile strength of the specimen and the pressure differ-

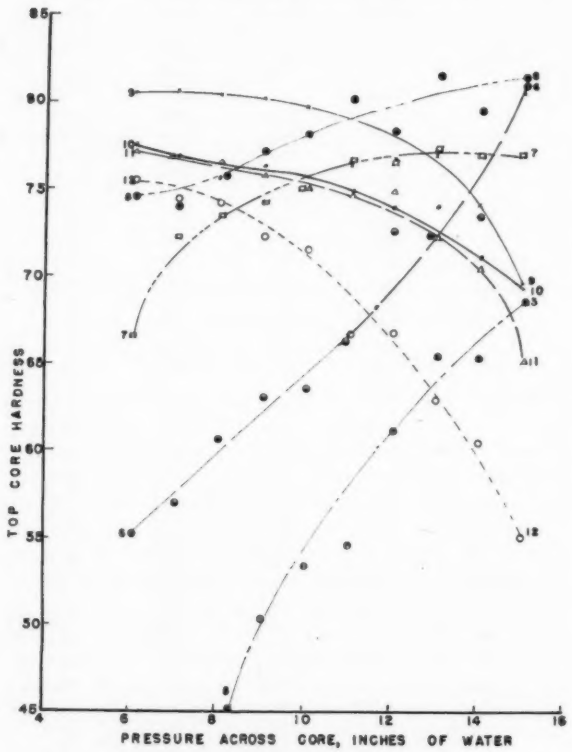


Fig. 10—Top core hardness of test oven.

ence across the core. It appears that there are some prospects for obtaining even shorter drying periods and higher strengths within the realm of higher temperatures and/or pressures.

6. The rate of air flowing through a core varies

Table 2—Tensile Strength of Standard Oven Cores at Different Times and Oven Temperatures

	20 Min	40 Min	60 Min	80 Min	100 Min	120 Min	140 Min	160 Min	180 Min
300 F	....	20.3	32.7	41.1	44.2	48.4	51.0	51.5	55.4
350 F	22.8	46.0	69.8	86.3	106.5	111.9	125.0	130.0	133.7
400 F	25.9	75.1	116.2	114.2	111.3	110.1	107.0	106.9	102.5
450 F	82.8	109.5	101.2	99.5	95.5	87.1	89.4	84.6	68.8
500 F	91.4	97.6	86.9	73.4	65.6	59.4	47.3	48.4	41.2

in a straight line ratio with pressure. Therefore a linear relation also exists between pressure and the volume of air forced through the core at a constant time.

7. The most efficient utilization of air was noted in the 7-min drying cycle.

8. When less than 12.2 cu ft of air was consumed in the drying cycle, the core with the longest drying cycle, exhibited the greatest tensile strength.

9. When more than 12.2 cu ft of air was consumed, the opposite was found to be true. This would indicate that the rate of flow of heated air is a very influential factor in core strength.

Industrial Applications. With the certainty of reducing core drying to one-fifth normal time and the prospect of achieving even greater substantial reductions with higher temperatures and other refinements, design for production should be considered as a very important development in core baking.

It remains up to the design engineer to develop an entirely new oven, incorporating simplicty and economy. This is entirely possible and is awaiting the ingenuity of someone to put it into use. There would be two demands for such an oven, one for small foundries for limited quantity and the other for high production which would probably be developed on the conveyor plan.

Acknowledgments

The author wishes to express appreciation to all his students who have performed various investigations in core drying. He especially wishes to con-

Table 3—Top Core Hardness of Test Oven Cores at Different Times and Pressures

	6 in.H_2O Pressure	7 in.H_2O Pressure	8 in.H_2O Pressure	9 in.H_2O Pressure	10 in.H_2O Pressure	11 in.H_2O Pressure	12 in.H_2O Pressure	13 in.H_2O Pressure	14 in.H_2O Pressure	15 in.H_2O Pressure
5 Min	30	31	42	42	52	57	55	61	71	77
	34	28	41	55	53	53	60	68	65	68
	45	50	43	51	51	57	64	63	57	70
	36	47	43	52	54	52	65	66	69	70
	37	46	48	51	57	54	63	70	66	65
6 Min	54	54	57	65	65	66	69	69	75	77
	57	59	55	63	66	68	74	77	74	78
	53	58	54	64	62	65	74	76	75	78
	54	56	56	64	62	63	72	73	68	87
	58	59	57	60	64	72	75	66	76	88
7 Min	67	71	74	72	75	75	78	79	78	78
	67	72	73	76	79	78	77	76	77	75
	70	72	74	75	74	77	77	78	78	77
	72	72	73	76	73	77	74	75	77	77
	70	75	73	74	74	77	77	79	76	79
8 Min	77	73	76	74	79	77	78	78	78	82
	74	72	75	75	78	79	80	84	78	80
	75	78	78	78	77	81	79	81	80	84
	74	71	74	81	78	87	76	84	84	84
	72	76	76	80	79	77	80	82	79	79
9 Min	83	83	80	81	79	74	75	75	72	71
	79	81	80	86	80	76	76	74	73	69
	80	76	80	80	79	77	78	71	74	68
	79	83	80	77	80	80	75	77	75	72
	80	79	81	77	80	75	78	75	75	67
10 Min	76	78	73	76	75	76	72	71	69	68
	77	77	76	79	74	75	76	75	72	70
	80	77	77	76	75	73	74	73	72	69
	77	77	75	76	77	73	75	74	72	72
	76	76	77	76	77	77	73	77	71	68
11 Min	77	76	79	75	74	75	73	70	71	66
	77	78	76	75	74	75	76	73	70	66
	77	77	77	77	77	75	75	71	72	63
	76	76	76	77	75	75	75	70	68	67
	78	77	75	76	76	74	76	77	72	65
12 Min	75	73	75	71	69	66	63	64	56	57
	76	73	72	73	73	69	69	65	60	55
	76	75	72	72	71	70	71	63	61	52
	73	75	75	72	70	66	68	64	65	54
	77	76	77	73	69	70	67	64	61	58

TABLE 4—BOTTOM CORE HARDNESS OF TEST OVEN CORES AT DIFFERENT TIMES AND PRESSURES

	6 in.H_2O Pressure	7 in.H_2O Pressure	8 in.H_2O Pressure	9 in.H_2O Pressure	10 in.H_2O Pressure	11 in.H_2O Pressure	12 in.H_2O Pressure	13 in.H_2O Pressure	14 in.H_2O Pressure	15 in.H_2O Pressure
5 Min	21	20	20	21	21	39	36	48	55	55
	20	18	20	23	23	33	42	50	51	51
	21	28	21	19	24	33	42	40	71	52
	20	19	18	27	26	32	41	45	44	47
	19	23	21	21	28	31	42	49	43	39
6 Min	27	38	41	49	50	53	59	58	64	70
	29	38	40	50	50	51	60	66	61	70
	31	43	39	41	51	58	53	63	63	69
	40	35	39	43	51	54	58	62	66	73
	41	36	41	42	49	58	58	56	63	75
7 Min	50	56	61	57	63	64	67	68	65	66
	45	58	60	60	63	65	64	69	68	67
	49	59	60	59	58	64	66	71	66	67
	53	59	57	61	61	67	66	68	67	68
	58	58	57	58	60	64	63	70	68	70
8 Min	65	63	69	71	72	59	75	76	75	75
	66	68	69	70	73	63	72	73	73	76
	66	70	67	68	72	61	71	77	77	76
	67	66	69	75	72	64	76	75	76	75
	66	62	70	71	72	57	73	81	75	77
9 Min	67	76	71	70	69	73	72	75	76	74
	70	71	71	72	71	73	74	74	76	74
	70	69	67	72	70	70	73	73	76	71
	68	71	71	67	72	72	68	74	74	75
	69	69	70	71	67	67	68	74	73	74
10 Min	69	69	70	73	72	75	74	73	74	74
	70	72	73	71	73	72	75	73	76	74
	73	70	72	71	72	73	75	71	76	74
	71	70	71	74	75	70	74	74	74	74
	69	70	71	71	70	74	73	78	68	75
11 Min	67	69	74	73	72	76	77	78	73	73
	68	71	74	74	78	76	76	78	78	74
	69	70	72	75	75	75	78	74	77	73
	67	72	72	73	75	78	76	73	78	72
	71	70	73	74	75	80	77	78	78	72
12 Min.	68	70	70	72	72	69	71	72	71	72
	71	68	73	73	72	74	73	74	73	72
	71	74	72	70	73	74	73	70	72	72
	72	71	72	69	73	72	71	72	74	72
	70	70	73	74	72	73	72	72	71	72

TABLE A—ANALYSIS OF VARIANCE OF TENSILE STRENGTHS

5 Minute Test

(Sample Data from Thesis "An Investigation of An Improved Method of Drying Sand Cores" by R. J. Wimmert)

Source of Variation	df	Sum of Squares	Mean Square	F	F*	F**
Between Pressures	9	5,660.68	628.96	.....	.....	.....
Due to Linear	1	5,443.63	5,443.63	.....	.....	.....
Deviation from Linear	8	217.05	27.13	2.83	2.18	2.99
Due to Quadratic	1	126.99	126.99	.....	.....	.....
Deviation from Quadratic	7	90.06	12.87	1.34	2.25	3.12
Due to Cubic	1	7.22	7.22	.....	.....	.....
Deviation from Cubic	6	82.84	13.81	1.44	2.34	3.29
Within Pressures	40	383.20	9.58	.....	.....	.....
Total	49	6,043.88	.....	.....	.....	.....

* 5% Confidence Level
** 1% Confidence Level

TABLE B—ANALYSIS OF VARIANCE OF TENSILE STRENGTHS

(Sample Data from Thesis "An Investigation of An Improved Method of Drying Sand Cores"

Source of Variation	df	Sum of Squares	Mean Square	F	F*	F**
Between Pressures	9	73,339.95	8,148.89	1,146.12	1.91	2.48
Between Times	7	255,236.52	36,462.36	5,128.32	2.04	2.71
Interaction Pressure with Time	63	4,261.01	67.64	9.51	1.38	1.57
Within P & T Cells	320	2,274.80	7.11	.....	...	...
Total	399	335,112.28	.....	.....	...	...

* 5% Confidence Level
** 1% Confidence Level

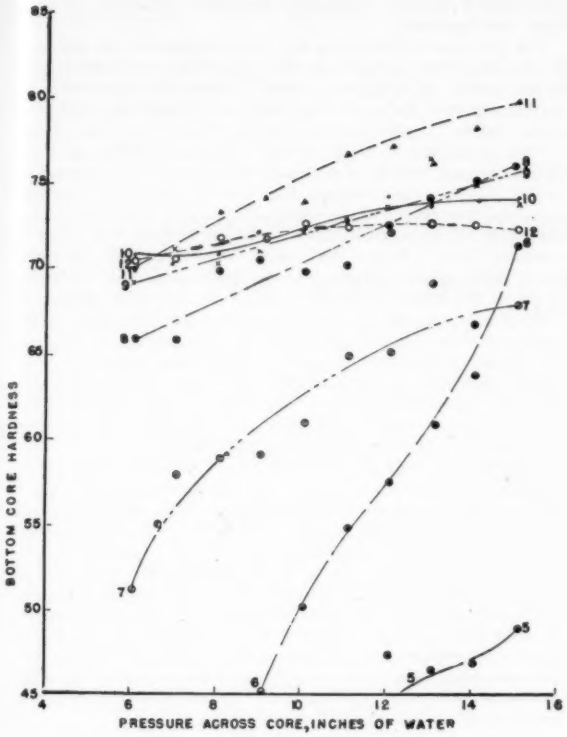


Fig. 11—Bottom core hardness of test oven.

gratulate the co-author, R. J. Wimmert, who performed the experiments on which this report is based.

APPENDIX

The following data represent the statistical studies connected with the experiment. Mr. Wimmert begins his discussion of results in his thesis, "Investigation of An Improved Method of Drying Sand Cores" with the following comments.

"The analysis of variance[3] of tensile strengths showed that both the time the cores remained in the test oven and the pressure difference across the core faces, had a significant effect upon the tensile strength developed by the specimens. The effects of the time and the pressure were shown, by the high F value of their interaction, to be dependent upon each other. The analysis of variance of core hardness substantiated the conclusions derived from the analysis of the tensile strengths.

The Orthogonal Polynomal[4] method of deriving the equations for the curves was selected because of the reduction of the computations necessary for the statistical analysis and the derivations of the equations. The analysis of variance of tensile strengths for the individual times disclosed that at the 1 per cent level of confidence there was not a significant deviation from a linear trend in any of the baking cycles. At the 5 per cent level of confidence, however, a significant deviation from linearity occurred in the 5, 7, 9 and 12-min baking cycles.

Throughout the following discussion the assumption was made that an increasing tensile strength was indicative of an increasing degree of baking."

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DISCUSSION

Chairman: E. C. ZIRZOW, Werner G. Smith, Inc., Cleveland.

Co-Chairman: H. W. MEYER, General Steel Castings Corp., Granite City, Ill.

Recorder: C. C. SIGERFOOS, Michigan State College, East Lansing, Mich.

RECORDER SIGERFOOS *(Written Discussion):* The Foundry Industry is indebted to the authors, Marek and Wimmert, for presenting this experimental evidence that points to the advantages of forcing heat and oxygen into a core for rapid baking.

It appears to the writer that the commercial success of the application of the induced flow process will largely depend upon the design of the core. For example, a thin or flat core resting on a large core plate area will lend itself to the induced flow. On the other hand, the flow will obviously be more difficult through a thick core resting on a small core plate area.

In the authors' discussion of industrial applications they state that it now remains for some ingenious core oven designer to incorporate simplicity and economy into the process. In considering the induced flow for the wide variety of shapes and sizes of cores, it appears that the design of cores to facilitate induced flow may well be as important a factor as the oven design. There is certainly a possibility that the thick or chunky cores, referred to above, might be designed with thin walls. Also, why not design core vents with the idea that the vents could be used for induced flow during the baking process?

Another possible commercial aspect to the induced flow process is in the more rapid baking of oil-sand shell molds that are currently being made by blowing the sand into core driers. This H. W. Dietert Co. process ("D" Process) yields oil-sand shell molds of approximately 3/8-in. thickness. It is conceivable that this new shell process coupled with the induced flow method described here by Marek and Wimmert, will provide considerable competition for the shell process that now uses the expensive thermosetting plastic binders.

H. W. DIETERT: [1] In the new "D" process no water is used in the oil-sand mixture and thus the baking time is reduced. Research should be started to study the possibility of eliminating water from core mixtures.

G. J. GROTT *(Written Discussion):* [2] The authors are to be congratulated for their orderly approach and systematic collection of data on a highly interesting possibility in the field of core baking.

It is of particular interest to the writer as this entirely independent investigation confirms work done several years ago at M. I. T. In 1947, the writer, being unaware of the Croning or C-Process, worked at the idea of making castings in what are now called shell molds. Thermosetting binders were not common in foundry work at that time so a search was made for a method for quickly baking thin sections of oil-sand cores.

The method developed was essentially that described in the paper under discussion.

Fine mesh copper screening was reinforced with heavy wire backing to support the thin core sections. Air was drawn from a large oven at 800 to 1000 F through pipes and through the core by suction. Previous work had shown that at temperatures above 390 F baking was instantaneous as far as development of high strength was concerned. Thus, the cores were baked only until a given temperature was reached. Thermocouple studies showed that a temperature of 500 F could be reached in 90 sec and cores baked for this length of time were quite hard and strong matching the best strengths obtainable through conventional baking.

Shortly after completion of the first successful run of "shell molds" the writer was presented with a piece of a Croning shell and quickly lost interest in drying oils as binders for shell molds. However, this was but one possibility.

The authors point out the desirability of a rapid removal of moisture as a means for shortening the baking cycle.

This is particularly pertinent when it is considered that high moisture contents are necessary to obtain the maximum dry strength from cereal binders. Where gas evolution from the baked core is not critical this is a most economical means of obtaining baked strength as far as binder costs are concerned. That this moisture is needed only for th proper distribution of the cereal and need not be present during baking is sometimes overlooked.

Thus the role of the core blowing machine in reducing moisture content has received little attention. Even the short blow necessary for filling the box removes considerable moisture from the core and would remove more if the core box were vented to a maximum rather than a minimum. Drying and heating the air and increasing the amount used would remove even more of the moisture.

Such partial drying would allow the use of higher moistures for better cereal distribution with the added advantage of high green strength after blowing and low moisture content for baking. Truly the possibilities are numerous.

It was stated above that the writer considered his cores to be baked when a certain temperature was reached but this judgment was based on strength alone. Further experience has disproved this belief and, having made the error himself, the writer wishes to caution others in this respect.

The chemistry of core baking as presented in the paper under discussion is, through necessity, directed at simplicity of theory rather than as being a basis for following the reactions as they actually occur in core baking.

The mechanisms presented are for the most simple type of polymerization; that in which the final polymer weighs the same as the total of the reacting molecules and/or atoms. This occurs only infrequently, if at all, in drying oils.

The actual mechanism is the much more complicated condensation polymerization: the final polymer weighs less than the reacting molecules and/or atoms as the polymerization occurs with the elimination of some low molecular weight side product such as water, carbon monoxide, etc. In the polymerization of fatty acids totally free of solvents or other such volatile material in an oxygen-free atmosphere such side products are formed regardless of the temperature of polymerization. It is not surprising that they are formed in core baking in an oxygen-containing atmosphere: the unpleasant gases from core baking are primarily aldehydes formed as or from these side products.

The purpose of discussing this is to emphasize the difficulty in obtaining the carbon-to-carbon polymerization rightly cited by the authors as being most stable. Baking in oxygen containing atmospheres leads to a high degree of oxidation polymerization before carbon-to-carbon polymerization can take place.

This consideration is of utmost importance where strength tests alone are used as a means of evaluation for a process or an oil. Taking the authors' Fig. 6 for a reference, strength considerations taken alone would dictate the choice of a core baked for 160 minutes at 350 F as being the best core. This is a serious fallacy. Such a core could be used in only the mildest of applications without erosion and considerable evolution of gas as the bond is primarily that of an oxidation polymer.

Tests for resistance to veining and erosion have proved beyond any doubt that cores must be "overbaked," at least on the surface, to give best results in casting iron or steel.

The idea that an underbaked core lacks collapsibility because the molten metal must first bake the core before it will collapse seems a logical and reasonable deduction but it is not borne out in practice or in the laboratory.

Collapsibility tests in a dilatometer show that cores baked to peak strength or for shorter times collapse very soon. Cores baked to peak strength even at 450 F usually collapse in half the time required for cores baked somewhat beyond peak strength. Once the cores have been baked to maximum resistance to collapsing this time to collapse does not fall off until after the cores have been baked beyond useability because of crumbling during handling. Baking to maximum resistance to collapsing at high temperatures is essential to attaining maximum resistance to erosion. Correlation between resistance to erosion and time to collapse under load in the dilatometer is sufficiently close to allow the use of dilatometer tests for production control of the usability f cores.

The temperatures and times of baking to produce the degree of carbon-to-carbon polymerization necessary to resist veining and erosion are not compatible with attainment of peak strength.

"Oil bodying" or the promotion of carbon-to-carbon polymerization is accomplished in a CO_2 or N_2 atmosphere and these gases are bubbled through the oil to remove side products.

On the other hand, oxidized or "blown" oils are prepared by bubbling air through the heated oil and this bears a close relation to the forcing of hot air through a core.

Table 2 in the text suggests that while peak strength may have been reached for the longest baking times at the highest pressure differentials, some doubt remains as to whether useable cores were produced. Accordingly, the evaluation of the cores produced must be based on resistance to erosion as well as strength.

It is not suggested that excellent cores cannot be produced by this method but only that every effort should be made to attain the maximum amount of carbon-to-carbon polymerization regardless of the method used. It is hoped that further development of this promising process will soon be undertaken.

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[2] Superintendent of Standards, Unitcast Corporation, Toledo, Ohio.

INFLUENCE OF FURNACE ATMOSPHERE ON THE ANNEALING OF MALLEABLE IRON

By

J. T. Bryce,* A. Hernandez,** and F. B. Rote***

ABSTRACT

An investigation of the controlled atmosphere annealing of white cast iron was conducted to determine the effects of the carbon evolution from the iron itself on the annealing atmosphere and the annealing characteristics of commercial malleable iron. The results were used as a standard for comparison to further experiments using air additions to modify the atmosphere for control of decarburization, rim and rate of anneal. Results showing time-temperature-gas analysis relationships are graphed. Characteristic photomicrographs of resulting structures are discussed and the relationship of mechanical properties to structural changes in the iron, as it is affected by atmosphere and rate of heating, are established. Data on nodule formation is correlated to atmospheric changes, rate of anneal and effect of heating rate. The cause of pearlitic rim is explained on the basis of rates of nodule formation and decarburization.

Introduction

With the development in design of controlled atmosphere furnaces for the annealing of white cast iron, the malleable iron foundryman has had to face an entirely new set of problems. The selection, generation, and control of the furnace atmosphere has largely developed into a series of individual plant problems, because of basic differences in white iron analyses, size and type of castings being processed, furnace design, and tonnages handled from plant to plant. These differences have inclined to retard recognition of clear cut generalizations as to what is the "best" atmosphere in which to anneal average malleable iron castings, how that preferred atmosphere is to be generated, and how it should be controlled—assuming of course that the furnace in which the annealing is to be performed is capable of positive control.

In general, for most steel applications these generalizations are well understood. In the case of malleable iron the recognition, acceptance and evaluation of the effect of the carbon in the white iron itself upon the composition and control of the atmosphere has delayed widespread understanding of the principles of sound atmosphere control. Since only small tonnages of gray cast iron are annealed in controlled atmosphere furnaces, this fundamental consideration is almost unique to the field of atmosphere annealing of malleable iron.

Early in the stages of this development it was recognized that different atmospheres produced significant variations in the extent, degree or rate of anneal of a given iron. Since the range of these variations may effect annealing rate by over 100 per cent of the optimum cycle, study and investigation of this entire atmosphere problem is not one which an alert malleable foundry management can economically ignore. In view of the developments in the last ten years in the field of controlled atmosphere annealing of both ferrous and non-ferrous alloys, it was decided that a re-evaluation of the fundamentals of this problem as it applied to the annealing of white cast iron would prove profitable. Some of the results of that work are reported in this paper.

Excellent papers[1,2] have appeared in recent years, dealing with the effect of machine generated gases as furnace atmospheres for malleable iron annealing, and on the effect of additions of air to atmospheres in continuous malleable iron annealing furnaces. The present field of investigation is the evaluation of effect of the carbon in the white iron on the annealing atmosphere in a batch annealing operation, and the effects of additions of air to the atmosphere so produced.

Experimental Procedure

In order to make the results immediately applicable to operating conditions, all of the heat treating experiments were conducted on regular production iron and in a conventional batch-type atmosphere-controlled furnace. Because of these limitations, the approach to the problem is predominantly technical.

Four distinct experiments were performed. In the first, Test No. 10, a normal load of white iron castings was given a standard annealing cycle in a gas-tight radiant tube-fired annealing oven. No atmosphere gas additions were made during the entire cycle, and gas analyses were made at 1 hr intervals throughout the entire duration of the anneal. Standard ASTM test bar samples were strategically placed in the load so that they could be conveniently with-

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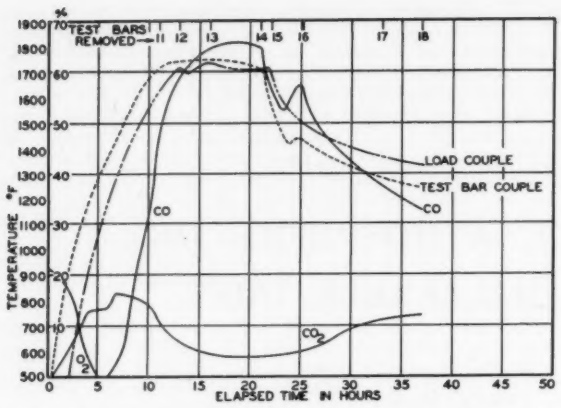


Fig. 1—Time-temperature-atmosphere curves for test No. 10 (normal atmosphere-no purges).

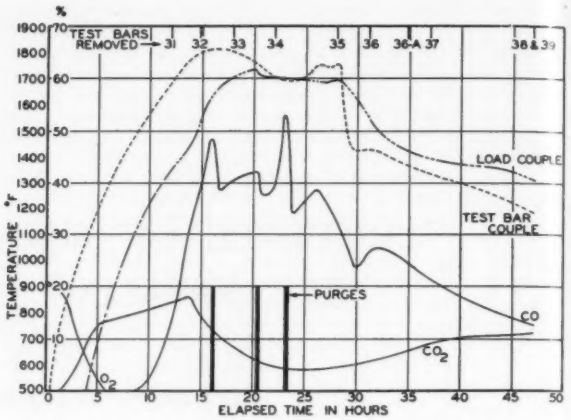


Fig. 2—Time-temperature-atmosphere curves for test No. 30 (three intermittent purges).

drawn at predetermined times in the cycle, and at a point where their exact thermal treatment could be recorded.

In the second experiment, Test No. 30, the same general procedure was followed, with the exception that air was admitted and atmosphere gas vented for three individual 10-min intervals at the following times:

First; when the entire mass of castings reached 1700F.

Second; exactly 4 hr after the first venting.

Third; exactly 6 hr after the first venting. Venting was performed through two port holes in the casing of the furnace; one being about 8 in. above the hearth at one end of the furnace, and the second about 18 in. below the peak of the arch at the other end.

The third experiment, Test No. 40, followed the general pattern established by the two previous experiments. Vent openings and times were adjusted to try to maintain a CO/CO_2 ratio of between 2/1 and 3/1.

The fourth experiment, Test No. 50, was a duplicate of the pattern of Test No. 40, except that granular wood charcoal was scattered on the iron charge before heating began, so as to produce a rapid elimination of free oxygen from the annealing atmosphere.

TABLE 1—CHEMICAL ANALYSES OF SAMPLE TEST BARS

Tests No.	Carbon	Silicon	Manganese	Chromium
10, 30, 40	2.36	1.56	.45	.037
50	2.35	1.58	.44	.032

Analyses of the test bars used is shown in Table 1. Great care was taken to insure having bars of uniform composition and inherited metal characteristics. The metal was duplexed in a cupola and oil-fired air furnace combination.

Test bars are identified by number; the first digit being the first number of the respective test, the second being the ordinal number of removal from the furnace in each respective test. The bars were removed from the furnace at the following times.

X1 (e.g. 11,31, etc.)-When the test bars reached 1700F on heating.

X2 (e.g. 12,13, etc.)-After the test bars had soaked 3 hr at 1700F.

X3 -After the test bars had soaked 6 hr at 1700F.

X4 -After the test bars had soaked 10 hr at 1700F.

X5 -At the start of the fast cool phase of the cycle.

X6 -At the time the bars on fast cool reached 1380F.

X6A- (Tests No. 30 and No. 50 only) At the time the bars on slow cool reached 1350F.

X7 -At the time bars on slow cool reached 1330 F.

X8 -At the time the annealed castings were removed at the end of the annealing cycle.

X9 - (Tests No. 30, No. 40 and No. 50 only) These bars were inserted into the furnace 1 hr after the X-1 bars were removed and they remained in the furnace for the balance of the cycle. Observed heating time was 15 min each.

Results

Development of Atmospheres: The fundamental data describing the temperature cycle and the atmosphere gas analysis throughout the four cycles are graphicaly shown in Figs. 1, 2, 3, and 4.

Test No. 10 (Fig. 1) is the basic experiment which best illustrates what happens when white cast iron is annealed in a sealed container. On heating, the gas reactions are quite slow at temperatures below the critical temperature of the iron. When this point is reached, carbon evolution from the iron and the formation of CO are very rapid. The equilibrium between CO and CO_2 is very sensitive to temperature changes, and in commercial annealing CO concentrations as high as 77 per cent have been recorded, when the temperature is in the 1750 - 1800F range. As an illustration of this sensitivity, note that after the fast cool (25th hour) a rise of 20F from 1420 to 1440F is coincident with an increase from 52.5 pct CO to 57.5 pct at a time when the mass of the load was slowly cooling (note load couple). In the cooling phases of the cycle thermal equilibrium between CO and CO_2 makes automatic adjustment of the atmosphere, as shown by the gradual decrease in CO and increase in CO_2.

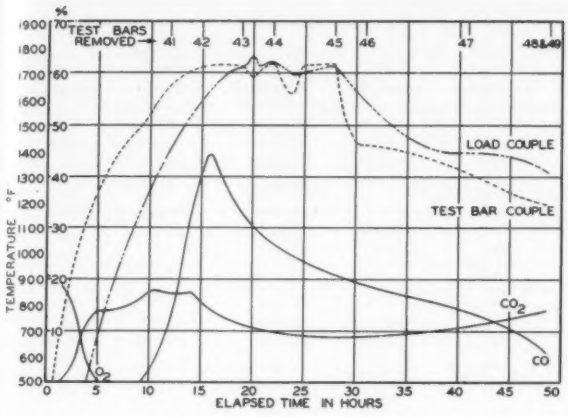


Fig. 3—Time-temperature-atmosphere curves for test No. 40, intermittent purging to 2/1 to 3/1 CO/CO_2 ratio during soak.

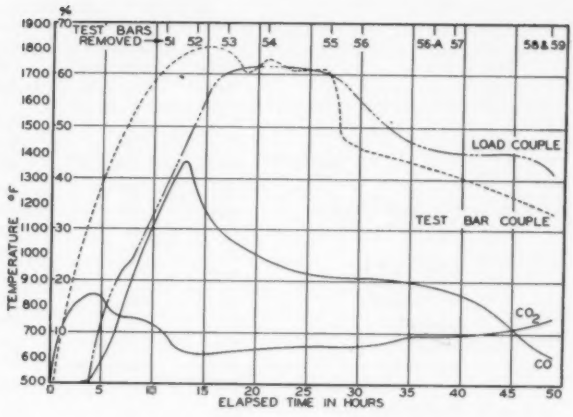


Fig. 4—Time-temperature-atmosphere curves for test No. 50, charcoal and intermittent purging to 2/1 to 3/1 CO/CO_2 ratio during soak.

In Fig. 2 the general slope of the temperature and gas concentration curves closely follow the corresponding curves of Fig. 1 up to the 16th hour. (A heavier mass of white iron in Test No. 30 accounts for the slower heating and carbon evolution). At this time the first purge arrested the formation of CO, but caused no appreciable increase in CO_2. Each of the three purges occasioned a similar reaction. In each case chemical recovery and readjustment followed the oxidizing effect of the air substitution. While development of a 60 pct CO atmosphere was successfully prevented, the curves clearly show the extreme sensitivity of the equilibrium to changes in temperature. As in Test No. 10 the significant drop in CO and rise in CO_2 came with a lowering of temperature.

The first 16 hr of Test No. 40 (Fig. 3) is a replica of the previous tests. At the 15th hour, the analysis was almost exactly 3/1 CO/CO_2, and purging was started. However, the amount of air required to maintain the desired atmosphere with the rising temperature was greatly under-estimated. In spite of successively greater increments of air, control to the predetermined limits could not be maintained until the 20th hour. It is while the load is increasing in temperature from 1500F to 1700F and for the first 2 hr of 1700F soak that the carbon from the iron is most readily available. This is to be expected, since the rates of carbon diffusion at these temperatures follow a logarithmic curve. All air additions were stopped at the end of the fast cool phase of the cycle, and beyond the 30-hr period chemical change is attributable to changing thermal equilibrium. Because of the large volume of air used to rectify the atmosphere during the heating and high temperature hold phases of the cycle, the CO/CO_2 ratio was just 2/1 when the slow cooling phase started. As cooling proceeded insufficient carbon in the atmosphere was available to maintain the desired limits, and the ratio fell to approximately 1/2. This drop had no observable adverse affects on either the test bars or production iron.

In Test No. 50 (Fig. 4) the presence of the charcoal upset the normal carbon-oxygen-iron equilibrium by providing an excess of available carbon throughout the duration of the test. A small amount of charcoal remained unburned after completion of the annealing cycle. The first indication of a change was the fact that within 1 hr of the time the furnace was sealed, the free oxygen of the chamber air had all combined with the charcoal to form CO_2 at lower temperatures than had occured in corresponding phases of previous tests. While charcoal is unquestionably a very active form of carbon, this behavior is interpreted as an indication that below the critical temperature the rate of carbon evolution from white iron is very slow. Therefore, carbon not being readily available from the iron below 1350F, the time required to eliminate free oxygen is extended. This is suggested by lingering quantities of free oxygen up to the fifth hour of heating in the previous tests.

Another factor which deserves consideration at this point is the probable formation of iron oxide on the surface of the castings at subcritical temperatures. Aside from the fact that this reaction removes some free oxygen from the atmosphere on heating, it is more interesting for its probable effect in forming a sealing barrier which retards early and rapid decarburization of the iron as it is being heated to 1700F. This subject will be discussed in greater degree as further data is presented.

It is quite significant that while the charcoal reacted quickly with the free oxygen to form CO_2, the CO did not form in appreciable quantities until the white iron reached the lower critical temperature. From this point the tendency to form CO and reduce CO_2 was very strong, in spite of considerable venting of the furnace after the ninth hour. In a few hours a reasonably purging rate was established which permitted establishment of an atmosphere approaching the original intention.

After the fast cool the readjustment in CO/CO_2 ratio to the lower temperatures did not take place, and reduced venting of the furnace continued to take place until the 42nd hour. Until this time the free

TABLE 2—AVERAGED MECHANICAL PROPERTIES

Test No.	Sample No.	Duration of Heat Treatment, hr	Temp. F of Removal	Brinell Hardness	Yield Strength	Ultimate Strength	% Elongation in 2 in.	Nodule Count-Cor
10	16	25	1400	255	—	98,000	3.5	48
(No purge)	17	33	1300	134	39,600	53,200	8.0	50
	18	37½	100	131	37,400	52,800	10.0	51
30	36	31	1380	277	—	107,500	4.5	44
(Three Intermittent Purges)	36A	35	1350	241	—	86,000	5.0	44
	37	37	1330	143	39,200	56,000	14.5	42
	38	47	100	143	37,800	55,400	17.5	48
	39	34	100	143	37,200	53,000	13.0	8
40	46	31	1380	255	—	97,500	4.5	50
(Purge to 2/1 to 3/1 CO/CO_2 Ratio	47	41	1330	143	38,000	54,300	13.5	46
	48	49	100	137	36,900	53,600	14.0	42
	49	36	100	131	37,800	54,000	13.5	20
50	56	30	1380	255	—	106,700	4.5	48
(Charcoal Plus Purge to	56A	37	1350	149	39,800	58,500	14.5	35
2/1 to 3/1 CO/CO_2 Ratio)	57	39	1330	137	37,500	55,500	19.5	42
	58	49	100	143	36,200	54,700	20.0	40
	59	38	100	134	35,600	50,000	23.0	17

carbon of the unburned charcoal had been responsible for the maintainance of a CO/CO_2 ratio of over 2/1. As the temperature of the load dropped below the upper critical temperature, and in spite of the excess available carbon, the ratio dropped abruptly.

Mechanical Properties: The test bars were checked for mechanical properties and prepared for micro examination. The properties of all bars tested are reported in Table 2.

Test No. 10 bars were characterized by low elongations, although the other properties were normal. This is attributed to the heavy pearlitic rim induced by the high CO gas concentration generated during annealing. In Tests No. 30 and No. 40 better elongation values were produced. In each case the microstructures showed lesser amounts of pearlite in the rim than in the bars from Test No. 10. The bars of Test No. 50 showed high elongations resulting from the heavy decarburized skin on these bars, in spite of the scattered pearlitic layer underlying the ferrite surface.

An interesting phenomenon was the lowered ultimate and yield strengths of the bars inserted into the furnace after it had reached 1700F. These reductions correlated with the lowered nodule counts for these bars, are a result of the rapid heating rate. Nodule count was made at the center of the cross-section of the test bar and is the full count in an area of .00088 sq in., (area of 4 in. by 5 in. at 150 diam).

Decarburization Rate: In evaluating decarburizing effect, some contention exists regarding whether or not the pearlitic band is decarburized area. The present studies show convincingly that not only is the decarburized rim and its usual underlying pearlitic band a part of the decarburized zone, but also that "rim" also includes the ferritic areas under the pearlite in which the carbon nodules are enlarged in size and depleted in number. The curves shown in Fig. 5 are drawn from measurements of this distance.

In Test No. 40, in which the objective was to hold a constant ratio CO/CO_2 gas, the rate of decarburization during the period when the iron was above

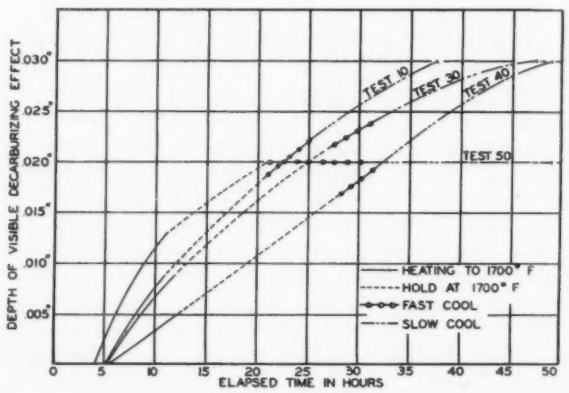


Fig. 5—Relationship of time and phase of cycle to visible decarburization effect.

the critical temperature was uniformly .0007 in. per hr. This held constant, in spite of changes in atmosphere gas analyses and temperature. Tests No. 10 and No. 30 showed a surprising similarity to this rate, however, they had a faster rate of decarburization during heating to 1700 F. This may have been affected by the fact that bars in the tests No. 10 and 30 were heated to 1700 F at a faster rate. The data do not seem conclusive on this point.

The initial rate of decarburization for the No. 50 tests was very rapid (as was the heating rate), and the rate to a depth of .020 in. the fastest of all tests. What was surprising was to find no additional decarburization for the last 28 hr of the annealing treatment.

Nodule Formation: Because of the apparent connection between nodule count in the rim and decarburization, the investigation was extended to include a study of the inter-relationship. Figures 6, 7, 8 and 9 show the progressive development of nodules as affected by progress of the anneal. In all tests nodule formation developed progressively during the heating to soak temperature. In Test No. 10 this development continued at a reduced rate through the high temperature hold phase. In Test No. 30, No. 40 and

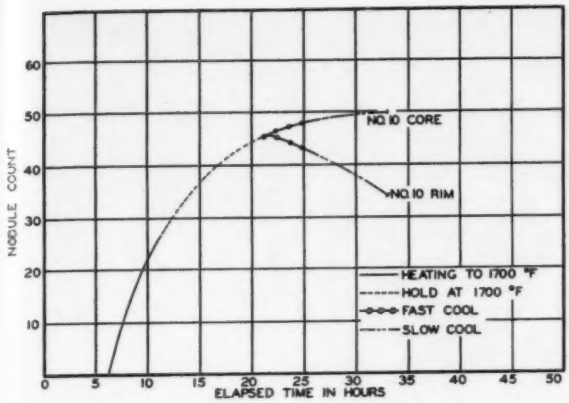


Fig. 6—Nodule development for test No. 10.

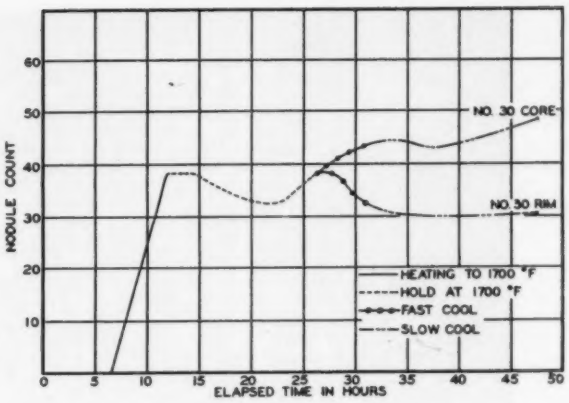


Fig. 7—Nodule development for test No. 30.

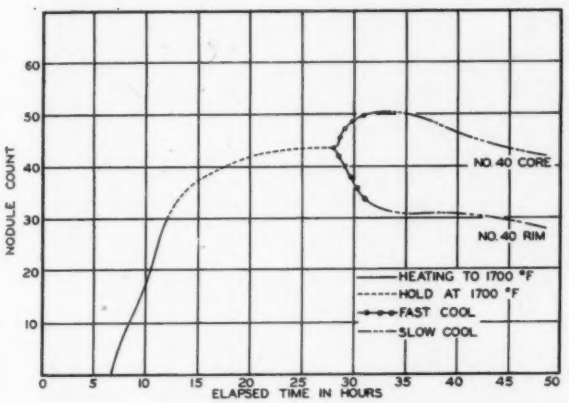


Fig. 8—Nodule development for test No. 40.

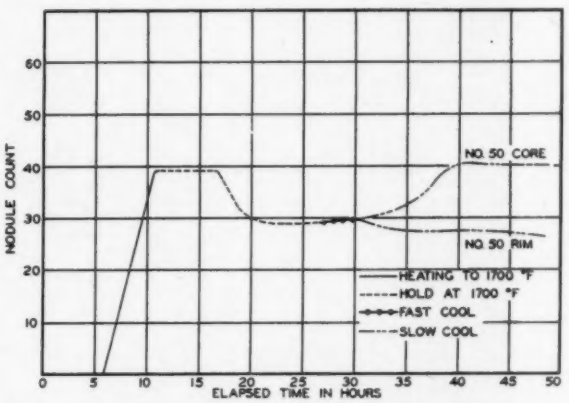


Fig. 9—Nodule development for test No. 50.

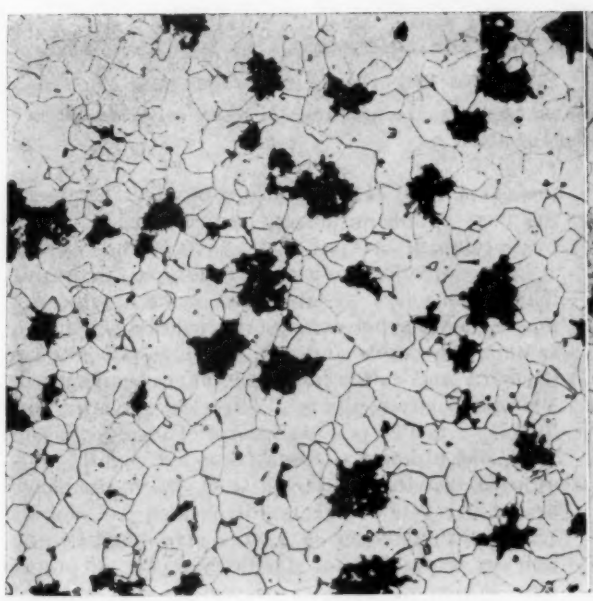


Fig. 10—Center of test bar No. 38 showing complete anneal of core at completion of normal cycle. Mag.—150x.

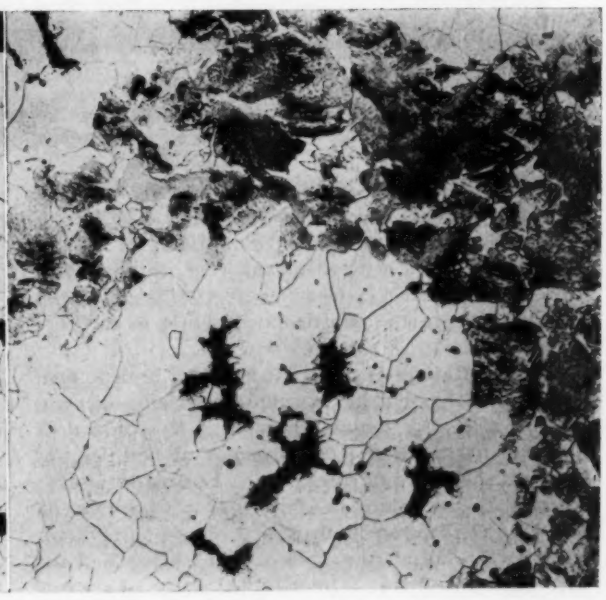
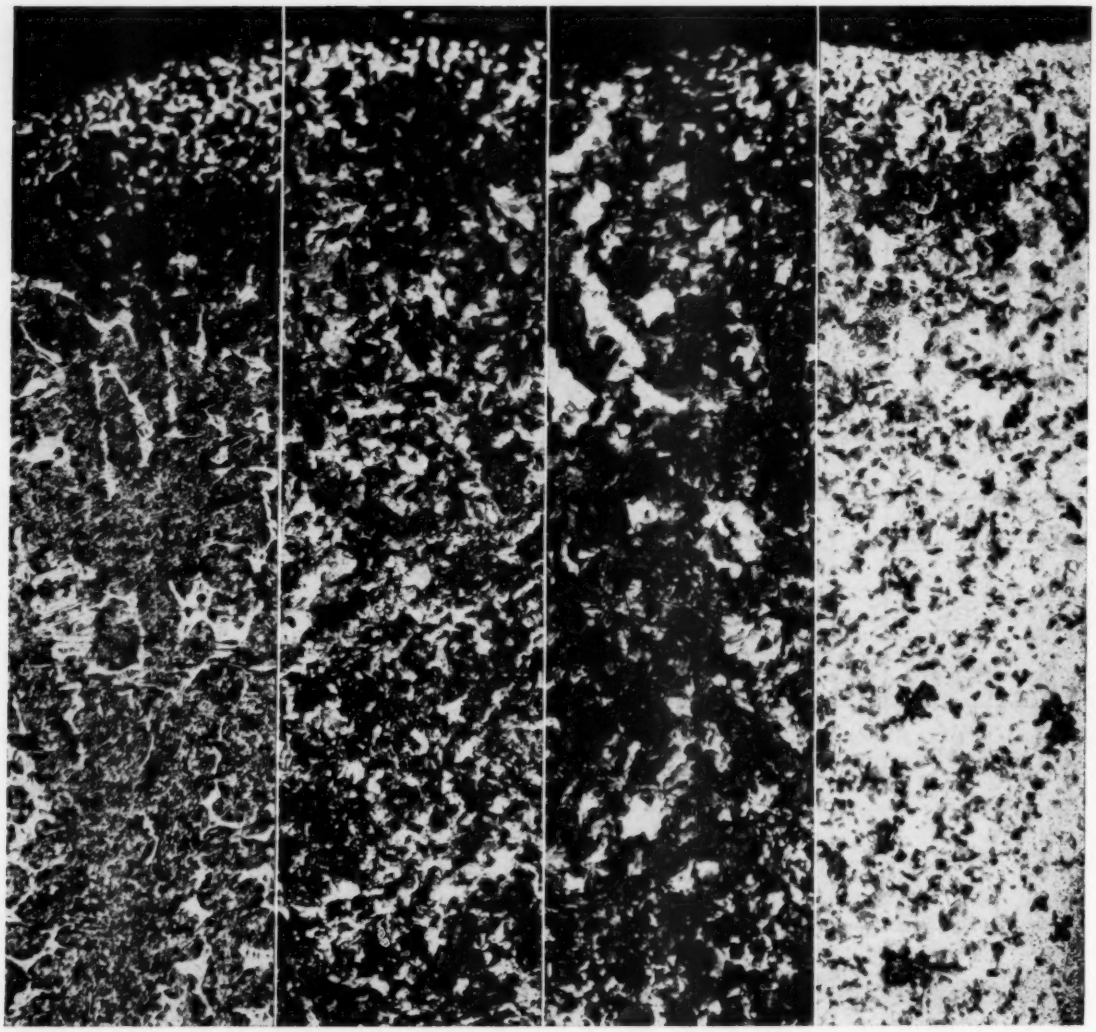


Fig. 11—Center of test bar No. 39 showing considerable pearlite in core of sample rapidly heated to 1700 F and treated in otherwise normal cycle. Mag.—150x.

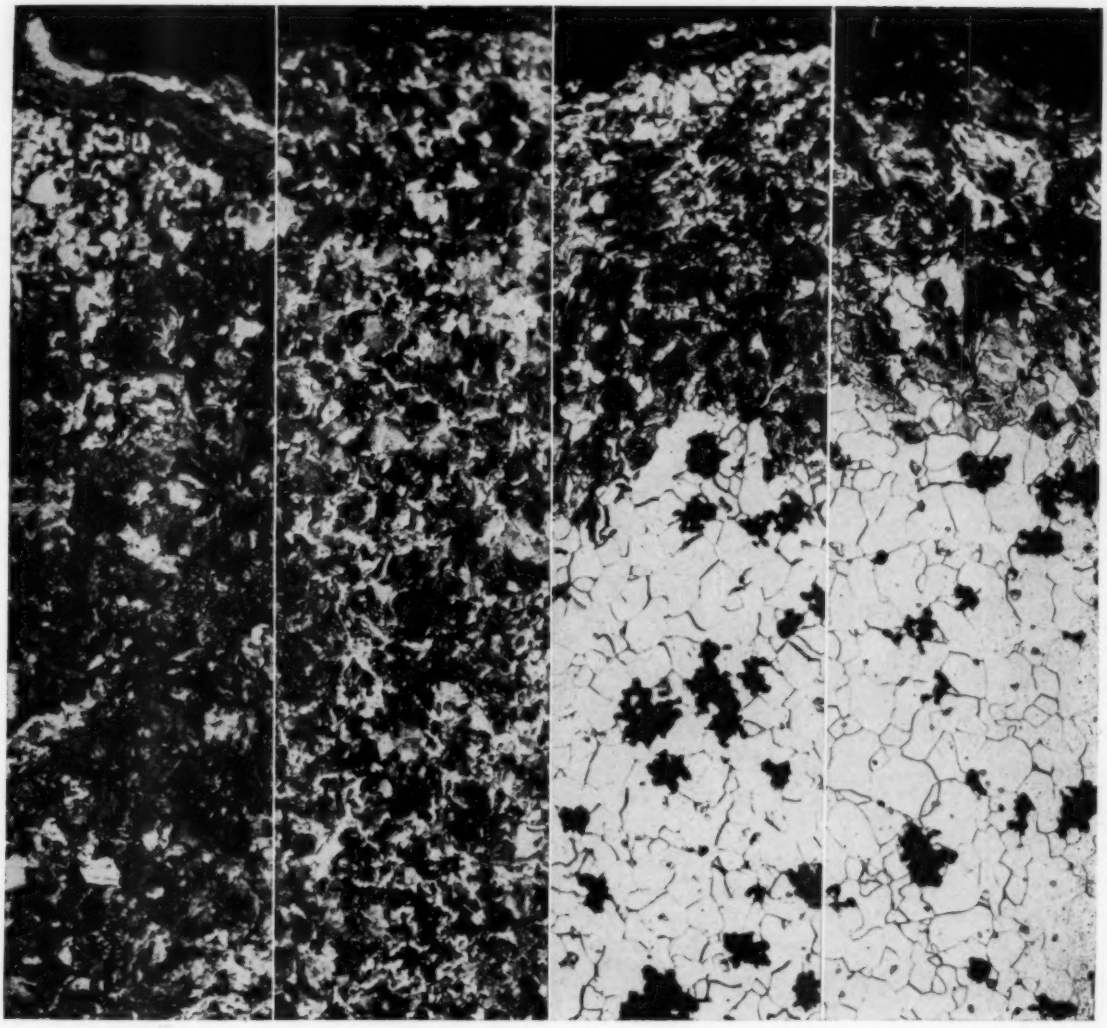


No. 11 No. 12 No. 13 No. 14

Fig. 12A—Edge of No. 10 test samples (no purge) showing progressive structural changes in rim at various stages of anneal. Mag.—150x.

No. 50, a decrease in nodule count in rim and core is coincident with the introduction of air to the atmosphere. In all cases there was no differentiation in the nodule count of the rim and core until the fast cool phase of each cycle started. At this point the nodule count in the rim dropped, while in the core it increased.

Effect of Rate of Heating: It has long been known that rate of heating has a pronounced effect on annealing rate and nodule formation. Not only does slow heating favor the development of nodules but it affects the shape of the nodule. Figures 10 and 11 showing the structure in the center of bars No. 38 and No. 39 illustrate both points. Except for differences in the initial rate of heating, each bar had the same heat treatment. The rounded shape of the nodules produced under slow heating conditions contrasts sharply with the star-shaped nodules of the rapidly heated sample. In addition, substantial amounts of pearlite were present in the matrix of the structure of No. 39. This shows that because of the poor nodule formation, second stage annealing was incomplete, in spite of the fact that the cycle was satisfactory under the normal conditions of heating. The presence of such substantial amounts of pearlite in the core structure would ordinarily be expected to cause a noticeable increase in yield and ultimate strengths and a drop in elongation as compared with a properly annealed iron. The elongation dropped by 4.5 pct to 13.0 pct, but instead of increasing, both yield and ultimate strengths dropped slightly. The drop in core nodule count from 48 in the slowly heated bar to 8 in the rapidly heated bar and the accompanying change in nodule shape and distribution are responsible for this deterioration of properties. While the presence of pearlite in the matrix has a strengthening effect, this was offset by the tendency of large, pointed nodules to weaken the structure. To a certain extent these factors are compensating as far as mechanical properties are concerned.



No. 15 No. 16 No. 17 No. 18

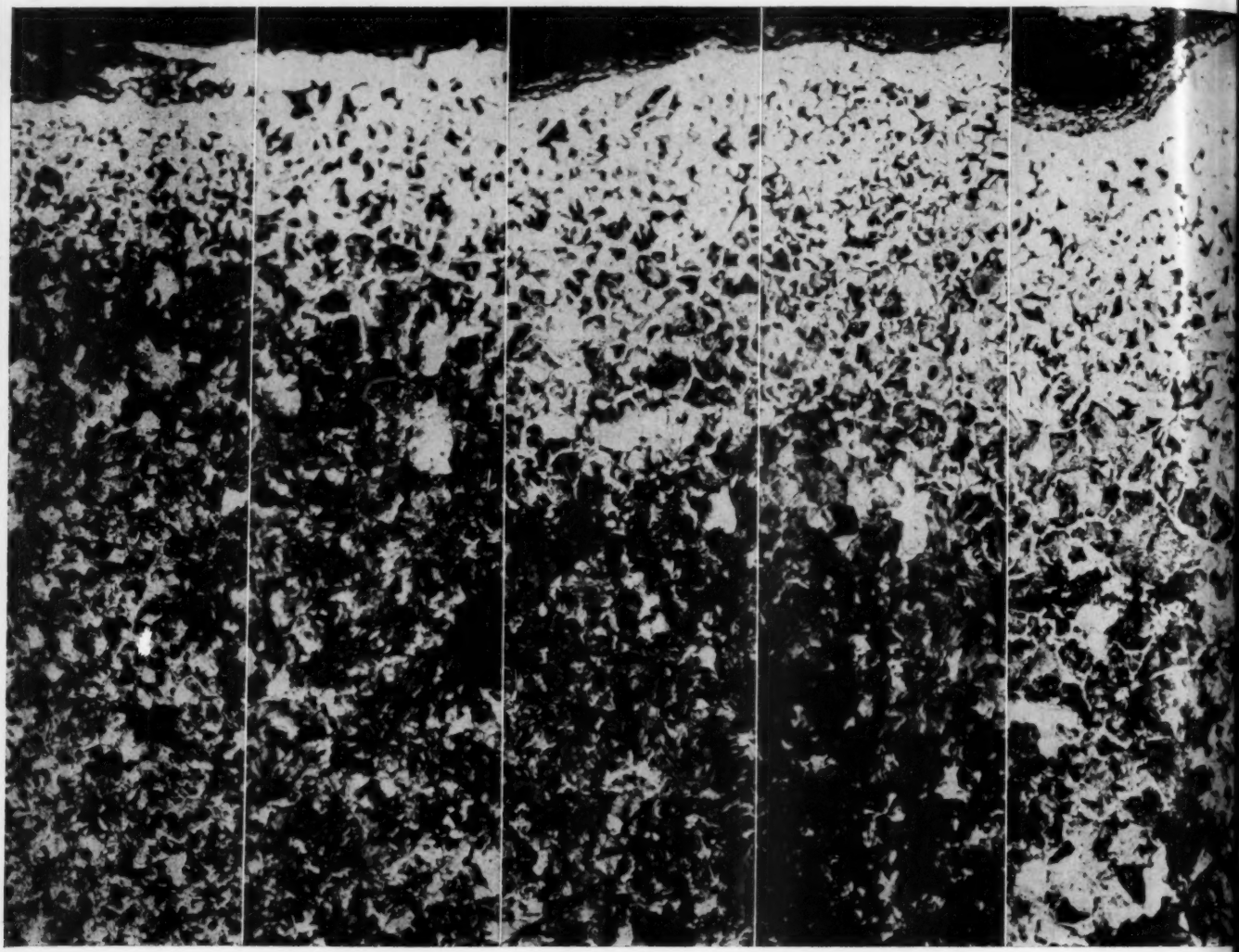
Fig. 12B—Edge of No. 10 test samples (no purge) showing progressive structural changes in rim at various stages of anneal. Mag.—150x.

Effect of Surface Decarburization on Elongation: Because measured elongation of an unmachined test bar can be influenced so markedly by the amount of surface ferrite in the decarburized rim, this mechanical property is not a reliable criterion of metal quality. This was clearly demonstrated by comparison of the properties of bar No. 18 with those of bars No. 39 and No. 59. Bar No. 18, with a fully annealed core structure and a rim containing pearlite to the surface, developed a measured elongation of only 10 pct. Bar No. 39 (Fig. 11) and Bar No. 59, with appreciable amounts of pearlite in their respective core structures and ferritic outside surfaces, developed measured elongations of 13.0 pct and 23.0 pct respectively. On the basis of elongation the No. 39 and No. 59 bars would appear to represent properly annealed castings and Bar No. 18 a doubtful quality.

On machining a significant difference would soon be evident. The structure represented by Bar No. 10 would turn, mill, drill or tap with ease, and time lost for broken tools, resharpening or replacement would be a minimum. By comparison, in the case of castings represented by structures characteristic of Bars No. 39 and No. 59, tool life would be greatly reduced and down time increased.

Discussion

Decarburization Characteristics: It is evident from a study of the gas analyses and microstructures that some measure of control of the intensity and rate of surface decarburization is possible through control of the analysis of the atmosphere gas. Figures 12 and 13 show the progressive changes in the rim on samples from Tests No. 10 and No. 50. Photomicrographs of these tests were selected because they illustrate the minimum and maximum decarburization rates observed. In all of the Test No. 10 photomicrographs the surface decarburization, while active, did not proceed to the point where the ratio of ferrite to pearlite in the outer rim exceeded an approximate 1:1



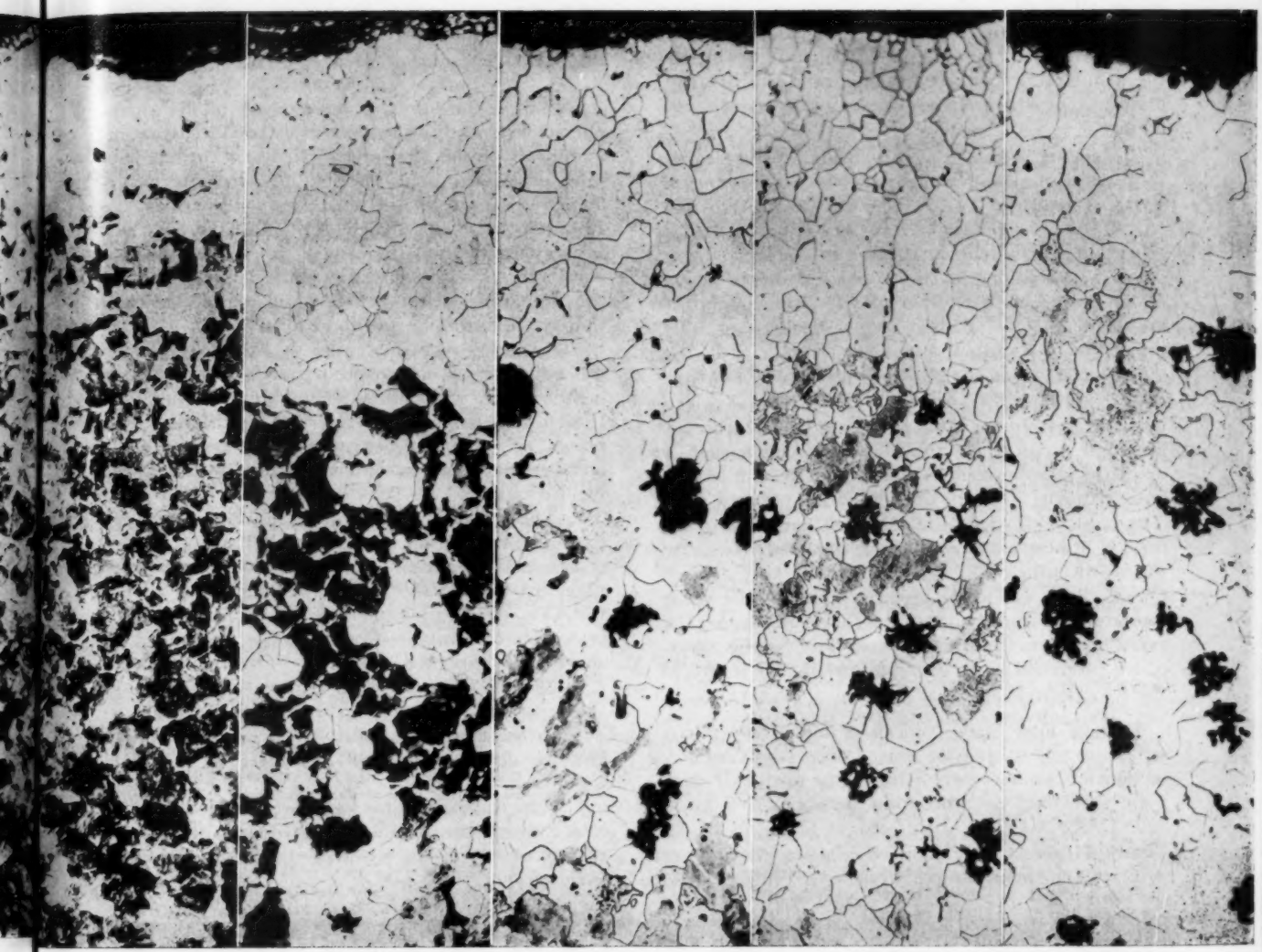
No. 51 No. 52 No. 53 No. 54 No. 55

Fig. 13A—Edge of No. 50 test samples (charcoal and purge to 2/1 to 3/1 CO/CO_2 ratio) showing progressive structural changes in rim at various stages of anneal. Mag. —150x.

ratio. This would correspond to a minimum carbon concentration of about .35 pct. While the penetration of decarburizing effect is progressive, it is not severe at any stage of the cycle. In fact, the severity would appear to be less during the slow cool stages than during the soak stage. In the outer .007 in. of sample No. 11, the surface decarburization occurring during the heating to 1700 F was sufficient to decompose the primary cementite. This is further visual evidence that during the heating from 1500 F to 1700 F and for an hour or two thereafter the rate of carbon loss to the atmosphere is at its peak. After one or two hours the atmosphere is in contact with a .35 carbon iron, not 2.35 carbon as was the case when heating began.

In studying Test No. 50 photomicrographs, the intensity of the decarburization in these samples contrasts with Test No. 10 structures. After only 3 hr at 1700F the atmosphere is in contact with an almost carbon-free alloy, to which carbon is supplied by outward diffusion from the core. While throughout the series there appears to be a smooth and progressive increase in decarburization intensity from Sample No. 51 to Sample No 5.9, total decarburization penetration reached a maximum depth at No. 54 and remained constant thereafter. After the fast cool started, No. 56 showed some tendency for outward diffusion of carbon. Beyond this point and after this migratory carbon had been removed, the ferrite surface layer became inert to further carbon transfer. This inertia coincides with the cooling through the critical range and emphasizes the relative inactivity of alpha iron compared to gamma iron.

The tendency to develop high measured elongation values in un-machined test bars appears to be closely related to the depth of the ferrite layer at the surface of the rim. In making the tensile test, fracturing first takes place at the surface and moves progressively in to the center of the bar. If the surface layer is pearlite, it is the ductility of this constituent



No. 56 No. 56A No. 57 No. 58 No. 59

Fig. 13B—Edge of No. 50 test samples (charcoal and purge to 2/1 to 3/1 CO/CO_2 ratio) showing progressive structural changes in rim at various stages of anneal. Mag. —150x.

which determines the point at which incipient fracture of the bar takes place. Since ferrite has a much higher elongation than pearlite, incipient fracture will be delayed (in comparison to a pearlitic surface), until this constituent has been strained beyond its ultimate strength. In the process it will have deformed to a much greater extent than the pearlitic band would permit before fracture. As a result an unmachined test bar having a reasonable (e.g. .010 in.) surface layer of ferrite is more apt to develop a measured elongation comparable to the machined bar than is the unmachined bar having a pearlite band at the surface.

Since nodules continue to develop as the annealing cycle progresses, and since nodule count drops in the rim areas during the fast cool and slow cool phases, a definite relationship appears to exist between nodule disintegration and a decarburizing atmosphere. In Fig. 6 the Test No. 10 curve (no purge) showed no drop in nodule count of the core at any time, and a drop in rim nodule count only as the fast cool started and the atmosphere became more decarburizing. In the other tests a drop in rim and core nodule count is coincident with air purging of the prevailing atmosphere, a definite decarburizing action.

Cause of Pearlite Rim: The question of "what causes pearlitic rim" now finds simple explanation. When the surface de-nodulization rate is in "balance" with the surface decarburization rate, a fully ferritic rim with no pearlitic band will result. The surface layer will be devoid of temper carbon. Under this rim will be the ferritic matrix with a progressively increasing number of carbon nodules toward the core. When the denodulization rate exceeds the decarburization rate, thus upsetting the "balance", a pearlitic rim will be produced either at the surface or under a ferritic surface layer. As denodulization occurs, the inter-nodular distance is extended and there is no compensating change in the carbon diffusion rate to

insure all of the carbon being able to migrate to a nodule before the lower critical temperature is reached. Carbon atoms isolated between nodules as the temperature drops below the lower critical temperature appear as pearlite on cooling to room temperature.

Previous reference has been made to the decline in physical properties of the bars which were inserted into the furnace at 1700F. The core structure of Bar No. 49 showed some isolated patches of pearlite in an otherwise ferritic matrix, while bars No. 39 and No. 59 showed considerably more pearlite in the center of the bar. This is another manifestation of the inability of the low nodule count structure to fully graphitize in a normal annealing cycle.

In the atmosphere hardening of high carbon tool steel it has long been known that if a neutral or carburizing atmosphere is not available, less decarburization will occur if the piece is treated in a slightly oxidizing atmosphere (1 - 2 pct O_2) than if an oxygen free "reducing" (5 - 10 pct CO) atmosphere is used. The light oxidized skin of the piece hardened in the former atmosphere will inhibit decarburization to the depth which will occur using the latter atmosphere. That this seems to occur in annealing white cast iron is born out by the data shown in Fig. 5. The rapid elimination of oxygen from Test No. 50 atmosphere promoted early decarburization and inhibited the formation of an oxide barrier.

Time of First Stage Anneal: Study of the microstructures reveals that for iron of the analysis studied, sections of test bar size (¾ in. on the grip end) complete first stage graphitization in about 3½ hr at 1700-1740 F. This is illustrated in Figs. 14 and 15. Figure 14 shows only small fragmentary islands of primary cementite after 3 hr at 1700 F. Figure 15 shows that that carbide breakdown is complete after 5 hr. Back interpolation brings us to the 3½ hr figure.

Conclusions

1. Within the limits of the analyses of the atmospheres developed, no evidence was produced that atmospheres of the type investigated can entirely suppress decarburization. Even in the presence of the 60 pct CO gas developed in Test No. 10 between the 13th and 21st hours, the penetration of the decarburizing effect was not diminished. It can only be concluded that for commercial operation and within the chemical limits of the field investigated, decarburization is an inevitable phenomenon.

2. Evolution of carbon from the iron itself during the annealing process, and particularly during the heating from the critical temperature to the high temperature soak, is so great as to be the dominant reason why prepared generator atmospheres are upset when used in large commercial atmosphere annealing furnaces.

3. The annealing atmosphere resulting from oxidation of the carbon in the iron itself may be safely combined with additions of air to produce an atmosphere having a ratio of approximately 2/1 to 3/1 CO/CO_2. This atmosphere when properly handled is capable of maintaining satisfactory rim control for the iron investigated.

4. Nodule formation and elimination play a significant role in determining the depth and character of the resulting rim. Skillful control of this phase, and the decarburizing rate in annealing, is the key to the control of rim.

5. Rate of heating directly affects nodule formation and through this medium has a dominant effect on determination of an adequate minimum annealing cycle.

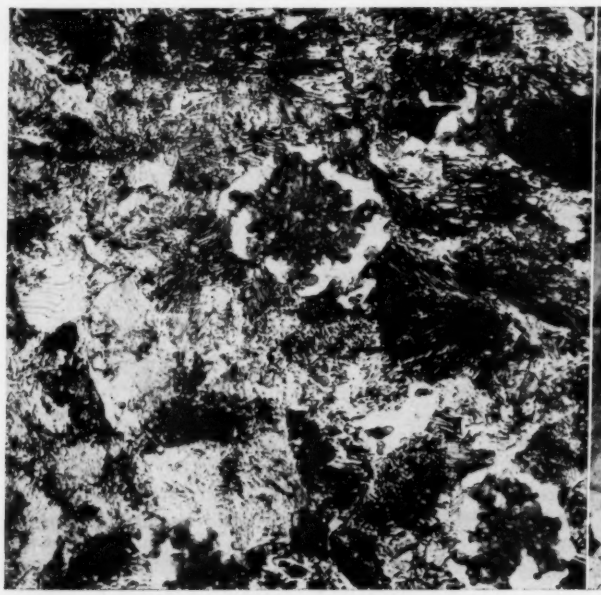


Fig. 14—Center of test bar No. 42 showing small particles of cementite indicating incomplete first stage anneal in 3 hr at 1700 F. Mag.—500x.

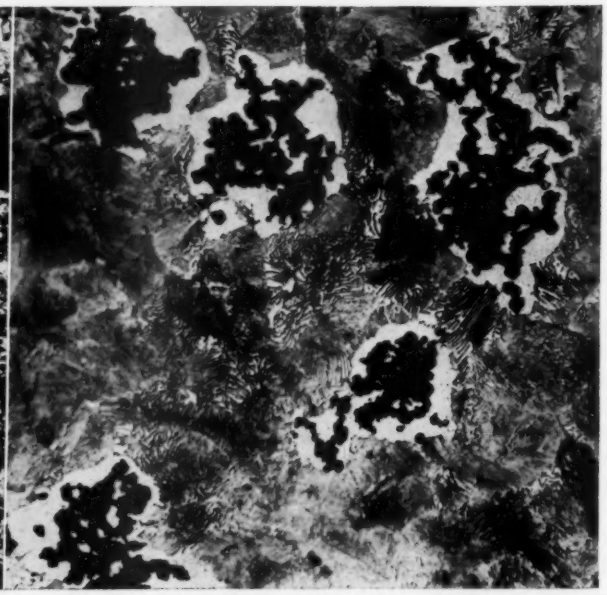


Fig. 15—Center of test bar No. 13 showing complete solution of primary cementite in 5 hr at 1700 F. Mag.—500x.

The authors wish to thank the management of the Albion Malleable Iron Co. for their permission to publish these data and J. H. Eggleston for his valuable assistance.

References

1. W. D. McMillan, "Furnace Atmosphere for Malleable Annealing," A.F.S. Transactions, vol., 58, pp 365-375 (1950).

2. O. E. Cullen and R. J. Light, "Malleable Cast Iron-Annealing Furnaces and Atmospheres," A.F.S. Transactions, vol. 59, pp. 494-500 (1951).

DISCUSSION

Chairman: W. D. McMillan, International Harvester Co., McCormick Works, Chicago.

Co-Chairman: J. E. Dvorak, Eberhardt Mfg. Div., Eastern Malleable Iron Co., Cleveland.

H. A. Schwartz *(Written Discussion):*[1] It may not be out of place in connection with the work of these authors, to point out again that the low carbon rim on he malleable casting is not altogether due to decarburization in anneal, but also, at least in part, to the process of selective freezing. This was illustrated by Fig. 11 of "Solidification of Metals" by this writer in vol. 53, p. 7, Transactions, AFS (1945).

The low carbon area varies in thickness to the freezing rate of the casting, increasing as the time to freeze increases. In 1½-in. slabs the area affected by selective freezing may be 0.04 or 0.05 in. thick.

Milton Tilley *(Written Discussion):*[1] Messrs. Bryce, Hernandez and Rote have executed a valuable piece of work on a subject that has been more or less neglected in the past. It is particularly valuable to the shop operator because the work was done under operating conditions. Conclusions 1, 2, 3 and 5 are valid and have been checked by others.

Conclusion 4 is well taken except where "elimination" of nodules is referred to. If the authors mean to convey the idea that nodules may be formed and then removed, I would question the validity of that statement.

The authors have neglected to mention the fact, as reported by Dr. H. A. Schwartz, that this type of metal as cast freezes differentially as to carbon in the first freezing metal at the edge of the casting. Schwartz has demonstrated that the first few thousandths inches freezes with about 1.00 per cent carbon and the succeeding layers increase up to that of the parent metal. This, he argues, is the reason that annealing in a reducing atmosphere a thin layer of pearlite remains at the extreme outside edge with resulting poor physical properties.

In my opinion the most valuable part of the paper points out that the best physical properties are attained by a decarburizing atmosphere rather than an oxidizing one.

Chairman McMillan *(Written Discussion):* I will digress long enough to mention that the operation of atmosphere annealing furnaces involves a little bit of dry chemistry—gas—solid and inter gas reactions. These are every bit as real and specific as reactions of wet chemistry and can be counted on to give predictable and understandable results. Also, that after a few months of minor adjustments, the control of the furnace atmosphere becomes a relatively simple matter.

Since the first atmosphere type annealing furnaces were installed, probably not over 20 years ago, there have been marked improvements in burner design, furnace construction, operating mechanism and in the conception of the "protective" atmosphere. The first atmospheres were to "prevent scaling" and considered somewhat of an achievement. The improvements, while probably not as dramatic as jet engines and atomic power, are none the less real and have brought this type of equipment into justifiable favor.

As this paper states, "the applications for steel are well understood." The significant differences between the treatment of steel and of hard iron are due to the higher carbon and higher silicon of the hard iron, the longer time at temperature and the range of temperature used in a single cycle or treatment of the hard iron. The last point may be the most troublesome because the one atmosphere must perform over a wide range of temperature from 1300 F to 1700 F and back to 1300 F. For these reasons the steel application can be considered only in principle and not expected to operate without modifications. It is a little difficult to conceive of an atmosphere with sufficient carbon pressure to hold back 2.50 per cent carbon in the face of 1.00 to 1.50 per cent silicon in the metal which is busily rejecting carbon.

The first conclusion in this paper qualifies the statement that decarburization is an inevitable phenomenon by confining it to the chemical limits of the field investigated. From the writer's experience, it appears that decarburization is unavoidable under any of the conditions that obtain in the field of malleable annealing.

While decarburization is unavoidable, it is not undesirable. It should be controlled both with respect to degree and to rate.

This paper is a fine contribution and the writer has no quarrel with the conclusions. However, in the second conclusion, it is stated that prepared atmospheres are "upset." This is certainly true if this means "altered." Because of the generation of CO in the furnace an increase in CO is unavoidable. This increase need not upset the composition to the point that the atmosphere is ineffective or detrimental. By changing the rate of flow of the prepared atmosphere, the CO content may be varied over a wide range. The CO content may be brought to a suitable ratio with CO_2 by the introduction of the prepared atmosphere. This practice operates in somewhat the same manner as the introduction of air. With the introduction of a prepared atmosphere the CO content is lowered by dilution while it is lowered by burning the CO to CO_2 by the introduction of air.

Fortunately, and possibly by chemical or by design, the atmosphere in a continuous furnace is treated to breath fresh air when the furnace is opened for charging and discharging. This to a slight extent has the effect of a purge.

With regard to the rate of heating (Conclusion 5) and the effect on nodule size and shape, there should be a decrease in the heating rate from 1500 to 1700 F as compared to rate up to 1500 F. This difference in rate usually obtains in commercial furnaces because of the limitations of the amount of gas that is usually burned in tubes that are in a high temperature environment.

From the standpoint of machining, surface conditions are probably more important than the degree of anneal in the body of the casting. A little scattered pearlite throughout will have little effect on tool life or machining rate. However, a casting showing a fully annealed structure but with a pearlitic rim of appreciable depth and density will give real trouble.

Mr. Bryce *(Written reply to Messrs. Schwartz and McMillan):* Dr. Schwartz's point that lower carbon metal exists in the surface layers of white cast iron is well substantiated. In this paper we are considering only the ¾-in. round section of a standard test bar, and our observations on this section of white cast iron of our analys show that this low carbon area rarely exceeds 0.010 in. in depth. This consideration therefore, would not seem to have significant bearing on our test results since the decarburization effect due to atmosphere would equal or exceed this 0.010 in. depth before the completion of first stage anneal. In the case of heavier section castings, longer first stage holding times than 3½ hr would of course be necessary. Extended periods at temperatures of 1700 F or higher give considerable opportunity for carbon diffusion. Studies of diffusion rates of carbon in steel at 1700 F incline us to believe that this phenomenon would obscure the effects of low carbon white iron rim.

Mr. McMillan has directed attention to a phase of the atmosphere problem almost invariably underestimated. That is, maintaining an atmosphere of uniform chemical composition which will meet all requirements within the wide temperature range of 1300 F to 1700 F. Differences in CO/CO_2 equilibria and diffusion rates within these temperature ranges are considerable and result in the "upset" referred to in the conclusions. Our use of the word upset does mean "altered."

We have observed the edge conditions of white iron treated in salt baths through the first stage of anneal. Some of these samples showed no apparent drop in carbon concentration at the surface of the specimens. We can visualize conditions under which full annealing could be accomplished without any drop in surface carbon of the piece, but such methods do not appear commercial at the present time. Of course as Dr. Schwartz has mentioned, there is a carbon gradient from edge to center of a white iron casting, and it is doubtful that it could be maintained during salt bath annealing because of natural diffusion reactions.

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[1] Manager of Research, National Malleable & Steel Castings Co., Cleveland.

[2] Metallurgist, National Malleable & Steel Castings Co., Cleveland.

FEEDING RANGE OF JOINED SECTIONS

By

E. T. Myskowski,* H. F. Bishop* and W. S. Pellini†

ABSTRACT

Joined cast steel plate sections of various thicknesses and lengths were investigated to determine the distances which could be fed to soundness. The intereffects of sections of different thickness were evaluated from the known feeding distance of sections cast separately. It was established that the mutual effects of joined dissimilar sections is, in most cases, one of reducing the feeding distance in the larger section and increasing the feeding distance in the smaller section beyond that obtainable in the same sections cast separately. However, very large or very small thickness differences cause these respective effects to be reversed.

Thermal analysis studies were conducted to establish the nature of the solidification conditions which determine the observed effects on feeding distance.

Introduction

It has been shown[1] that the maximum distance which a uniform plate section can be fed to complete soundness by a single riser is equal to 4½ times the casting thickness. Of this distance a length equal to two times the plate thickness adjacent to the riser is rendered sound as a result of thermal gradients produced by the heating effect of the riser while the remaining 2½ times the plate thickness is made sound as a result of gradients developed by the chilling effect of the large volume of sand at the casting extremity. The application of chills to the casting edge[2] increases the feeding distance by approximately 2 in. irrespective of plate thickness (in range of 1-in. to 4-in. plates). The increase is due to a faster solidification rate developed by the casting edge when in contact with the chill. The effect of the chill is not pronounced, inasmuch as a major portion of the heat extracted by the chill following the formation of a solid skin at the end surface represents continued cooling of the already solid metal and accordingly does not contribute to directional solidification. Figure 1 provides a schematic illustration of these relationships.

It should be expected that the above relationships evolved for separately cast plates would be modified in castings representing a series of sections of various thicknesses. In such cases the feeding distance of each individual section should be influenced by the adjoining sections. Consideration of the relationships presented in Fig. 1 suggests that heavier sections should act as risers to the thinner sections and that the thinner sections should act to partially destroy the effects of the thick section casting edge. If the thin section is very thin compared to the thick section it may be visualized that the converse would be true, i.e., the thin section should act as a radiator and serve as an added chill. However, this effect should never be pronounced since at most the thin section could approach the effect of a chill (2 in.) added feeding distance as determined previously.

These highly practical questions regarding the intereffects of sections of various thicknesses have remained a matter of conjecture, since even qualitative data have not been available. The present report represents an extension of the previous basic work on the feeding range of simple sections to the problem of joined sections. Plate sections of various thicknesses and lengths were investigated to evolve formulas for calculation of the extent of soundness of a given combination of joined sections. In addition,

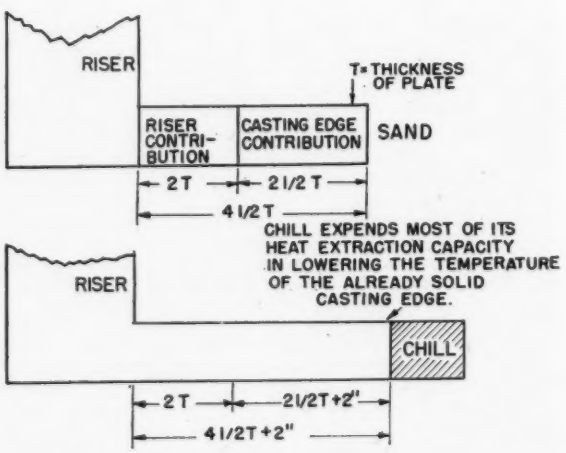


Fig. 1—Contribution of riser and casting edge to maximum distance which can be fed to soundness in uniform plates.

*Metallurgist and †Head, Metal Processing Branch, Naval Research Laboratory, Washington, D. C.

TABLE 1—SUMMARY OF TESTS ON SINGLE JOINTS

		Heavy Section, T, in.	Heavy Section, L, in.	Light Section, T, in.	Light Section, L, in.	Joint Location	Soundness Heavy	Soundness Light	D_h*	D_h*	D_l*
FR	70	2	8	1	5½	Edge	Shrink	OK	7.3	7.5	7
	88	"	7½	"	8½	"	"	Shrink			
	72	"	7	"	6½	"	OK	OK			
	76	"	7	"	7½	"	OK	OK			
	44	"	7	"	7½	"	OK	Shrink			
	44	"	7	"	7½	Center	OK	"			
	48	"	8	"	7	Edge	Shrink	OK			
	22	3	13½	1	4½	Center	Shrink	OK	10.5	10.5	10.5
	72	"	11½	"	6½	Edge	"	"			
	76	"	10½	"	7½	"	OK	"			
	88	"	10½	"	8½	"	"	"			
	94	"	10½	"	9½	"	"	"			
JS	1	"	10½	"	10½	Center	"	Shrink			
	1	"	9½	"	10½	Center	"	OK			
	106	"	10½	"	12	"	Shrink	Shrink			
	106	"	10½	"	12	Edge	"	"			
	47	"	10½	"	9½	Center	"	OK			
JS	3	3	10½	2	10½	Center	Shrink	OK	7.75	7.5	10.5
	7	"	9½	"	12	"	"	Shrink			
	10	"	7	"	10½	"	OK	OK			
	12	"	8	"	10½	"	Shrink	Shrink			
	16	"	8	"	9½	"	OK	OK			
	48	"	8	"	10½	Edge	OK	OK			
FR	107	4	16	1	12	Center	Shrink	OK	13.7	13.5	14
	107	"	16	"	12	Edge	"	"			
JS	2	4	14	1	14	Edge	Shrink	OK			
	2	"	14	"	14	Center	"	"			
	11	"	14	"	15	Edge	"	Shrink			
	11	"	14	"	15	Center	"	"			
	5	"	13	"	16	Edge	OK				
	46	"	13	"	14	Edge	"	"			
	46	"	14	"	13	Center	Shrink	OK			
JS	3	4	14	2	14	Center	Shrink	Shrink	10	10.5	14
	7	"	12	"	16	"	"	"			
	9	"	10	"	14	"	"	"			
FR	108	"	10	"	10	"	OK	OK			
JS	45	"	10	"	13	Edge	"	"			
	45	"	10	"	13	Center	"	"			
JS	4	4	14	3	14	Center	Shrink	Shrink	7.3	7.5	14
	6	"	12	"	14	Edge	"	"			
	8	"	10	"	13	"	"	OK			
	12	"	7½	"	14	"	"	Shrink			
	12	"	7½	"	13	"	OK	OK			

*Calculated by respective formulas (2) (3) (1)

thermal studies were conducted to determine the nature of the solidification conditions in order to add to the present limited understanding of the interrelated mechanism of solidification and feeding.

Experimental Techniques

Casting soundness was evaluated by radiography of longitudinal strips, 1 in. thick, removed from the center of the castings. In selected cases thermal analysis studies were made using techniques described in the earlier reports.[1,2,3] All castings were poured at 2960 F $\pm$ 20 F with induction melted steel containing .25-.35 per cent carbon, .50-.80 per cent manganese, and .30-.50 per cent silicon. Final deoxidation was accomplished by the addition of .10 per cent aluminum to the ladle.

Plate sections of 1, 2, 3 and 4-in. thicknesses were joined together as illustrated in Fig. 2 and the length of each component of the casting was systematically varied in 1-in. steps to determine the maximum which could be made sound. It was established during the early stages of the investigation that whether the small plate was joined at the center of the extremity of the large plate, Fig. 2A, or at the edge, Fig. 2B, the degree of soundness in both plates was the same, so for ease of molding the majority of subsequent joints were made as illustrated in Fig. 2B. Single joint sections involving two plates were first studied in the various combinations possible with the four different thicknesses; then double and triple joints, Fig. 2C and 2D, involving three and four plate combinations were investigated. In all cases the plates were joined in order of decreasing thickness so that conditions were favorable for directional solidification and only one riser at the heaviest section was required for feeding. The test castings were made in width equal to five times the thickness of the heaviest section (to ensure a condition of a "true" plate[1]) and were gated thru the risers. Riser diameters were made equal to three times the maximum section thickness and riser heights were at least 1½ times the riser diameters. Such risers are adequate to prevent under-riser shrinkage and, as shown previously,[1] are fully effective in promoting feeding of the sections; increasing the size of the risers does not result in increased feeding distance.

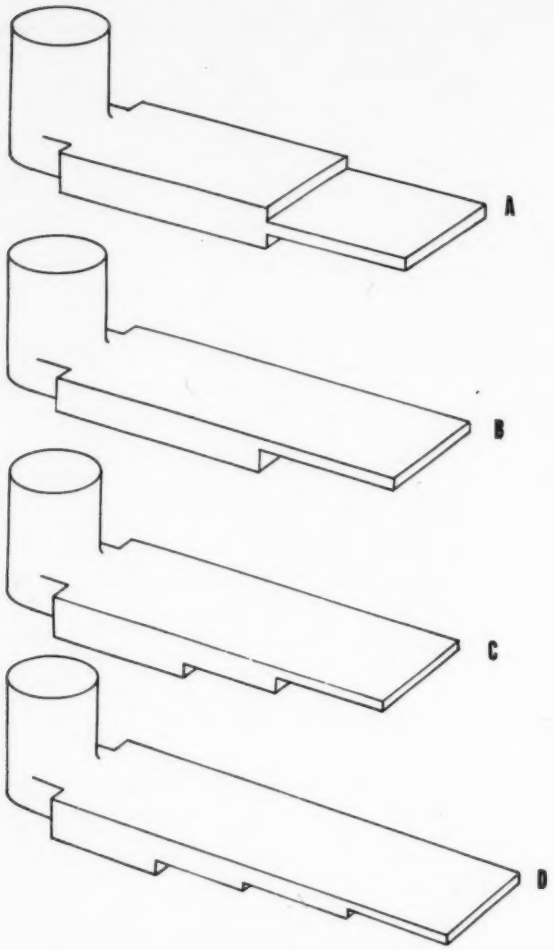


Fig. 2—Method of joining plate sections.

Single Joint Systems

A summary of the tests made on combinations of two sections are listed in Table 1. It is observed that the maximum feeding distance of the smaller or "parasite" plates of a given thickness increases as the thickness of the larger or "parent" plate is increased. For example, a 1-in. plate attached to a 2-in. plate can be made sound for a distance of 7½ in. but when it is attached to a 4-in. plate it can be made sound for a distance of approximately 13 in. Furthermore the feeding distance in all parasite plates, regardless of their thickness, is essentially constant when joined to a parent plate of a given thickness (and sufficient length) as is shown by the fact that the 1, 2 and 3-in. plates joined to 4-in. plates are all made sound for a distance of approximately 13 in. From the data pertaining to the thinner of two joined sections, Table 1 and Fig. 3, the following formula has been derived to calculate feeding distances in such sections:

$$D_l = 3.5\ T_h \quad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots \quad (1)$$

Where D_l = Feeding distance in light section, in.

T_h = Thickness of heavy section, in.

It is apparent that the increased feeding distance ob-

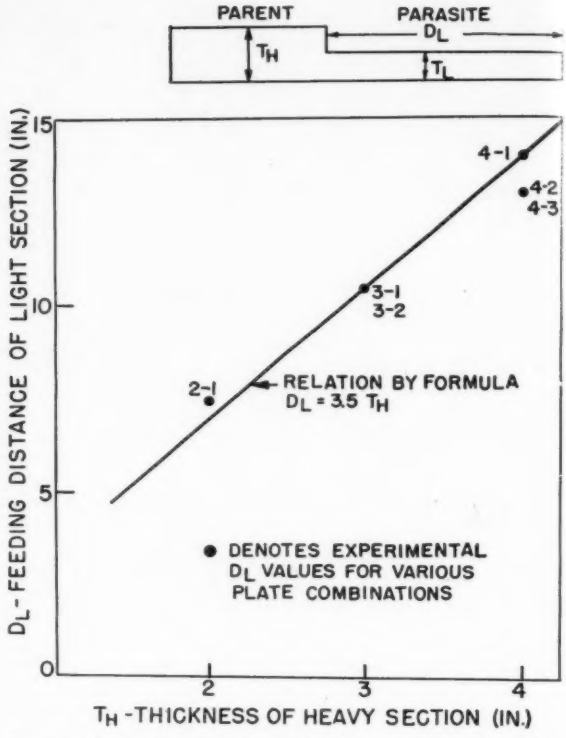
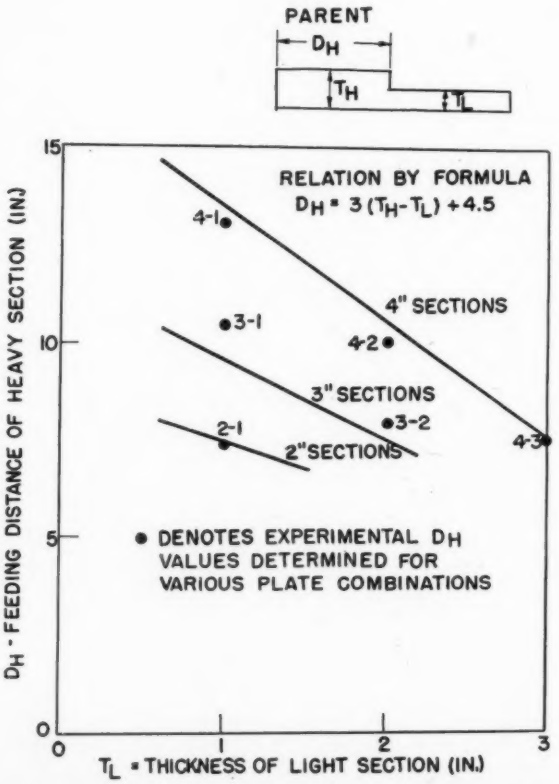


Fig. 3—Illustrating that feeding distance of parasite plates is determined by thickness of parent plates.



is decreased with increased thickness of parasite plate.
Fig. 4—Illustrating that feeding distance of parent plate

tained in light sections when joined to sections of considerably greater thickness is very large, as denoted by the following summary of data abstracted from Table 1.

Feeding Distances, In.

Plate	Cast Singly*	Joined to 2 in.	Joined to 3 in.	Joined to 4 in.
1	4½	7½	10½	14
2	9		10½	13
3	13½			13
4	18			

*From formula $D = 4\frac{1}{2}T$

The increased feeding distance obtained in the lighter section is countered in part by a decrease in the feeding distance of the heavier section, Table 1, Fig. 4. It is noted, for example, that as the thickness of the parasite plate attached to a 4-in. section is increased from 1 to 2 to 3 in., the feeding distance in the 4-in. section decreases in turn from 13 to 10 to 7½ in. The following empirical formula defines the feeding distance of heavy sections:

$$D_h = (4.5\ T_h \times .707^{T_1}) + 1 \quad \ldots\ldots\ldots\ (2)$$

Where D_h = Feeding distance in heavy section, in.

T_h = Thickness of heavy section, in.

T_1 = Thickness of light section, in.

While this formula is somewhat unwieldy for rapid calculations due to the exponential factor, it fits the measured data accurately, as can be noted by the calculations presented in Table 1. To aid in the use of this formula, values for $.707^{T_1}$ over the range of ¼ to 4-in. sections are listed in Table 2.

A simpler formula which is more suitable for practical usage and accurate over the thickness range of 1 to 4 in. is:

$$D_h = 3\ (T_h - T_1) + 4.5 \quad \ldots\ldots\ldots\ (3)$$

Formula (2) predicts that, irrespective of plate thickness, a sufficiently thin parasite plate will exert a chilling effect upon the parent plate to cause feeding distance in the parent plate to be increased by a maximum of 1 in. over that which is possible when it is cast separately (calculated by formula $D = 4\frac{1}{2}\ T$). Formula (3) indicates that parasite plates exert such a chilling effect on the parent plate only when the parent plate thickness is below 3 in. Since there is no apparent physical reason why plates over 3 in. in thickness should react differently to parasite plates than those below 3 in. it is concluded that the range of applicability of formula (3) is restricted to the particular thicknesses investigated. Formula (2) should be used for extension of the data to plates of heavier thickness.

Table 2—Values of $.707^h$

T_1	0.707^{T_1}
¼	.92
½	.84
¾	.77
1	.707
1¼	.65
1½	.59
1¾	.55
2	.50
2¼	.46
2½	.42
2¾	.39
3	.35
3¼	.32
3½	.30
3¾	.27
4	.25

Multiple Joint Systems

The condition of soundness observed in castings comprised of three and four plates of dissimilar lengths and thicknesses are listed in Table 3. As is to be expected, the largest and the smallest plate in each series behave as noted for the single joint combinations; the feeding distances in each case are determined by the thickness of the adjacent plates. The feeding distance in intermediate plates, which act as both a parent and a parasite plate, is governed by

Table 3—Summary of Tests on Multiple Joints

	Section Thicknesses, in.	Section Lengths, in.	Section 1	Section 2	Section 3	Section 4	Calculated Lengths Section 2	Calculated Lengths Section 3
JS-19	3-2-1	8-8½-7	OK	OK	OK		9½	
JS-22	3-2-1	9-9½-7	OK	OK	OK			
JS-22	3-2-1	8-10½-7	OK	Shrink	OK			
JS-43-2	3-2-1	9-8½-8	Shrink	OK	OK			
JS-24	4-3-1	7½-13-9½	OK	OK	OK		13	
431	4-3-1	8½-14-13½	Shrink	Shrink	OK			
431A	4-3-1	7½-14-9½	OK	Shrink	OK			
JS-24	4-3-2	7½-13-9½	OK	Shrink	OK		12	
JS-27	4-3-2	7½-12-9½	OK	OK	OK			
JS-47-1	4-3-2	8½-12-10½	Shrink	Shrink	OK			
JS-44	4-2-1	7½-12-7	OK	OK	OK			
JS-44-1	4-2-1	7½-13-7	OK	Shrink	OK		13	
JS-44-2	4-2-1	7½-14-7	OK	Shrink	OK			
JS-31	4-3-2-1	7½-12-9-7	OK	OK	OK	OK	12	9½
JS-43-1	4-3-2-1	8½-12-10½-8	Shrink	OK	Shrink	Shrink		
JS-45-1	2-1½-1	6-5-5	OK	OK	OK		6	
JS-49-2	2-1½-1	6-7-5	OK	Shrink	OK			
JS-49-3	2-1½-1	6-6-5	OK	OK	OK			
JS-50-1	3-2½-2	6-9½-7	Shrink	Shrink	OK		6½	
JS-50-2	3-2½-2	6-8½-7	Shrink	Shrink	OK			
JS-50-3	3-2½-2	6-7½-7	Shrink	Shrink	OK			
JS-51	3-2½-2	6-6½-7	OK	OK	OK			

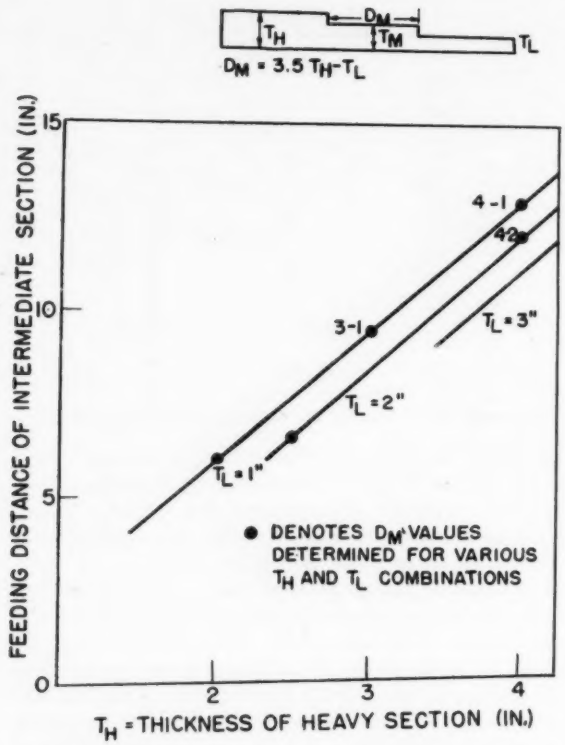


Fig. 5—Illustrating that feeding distance of intermediate sections is determined by thickness of adjoining sections.

the resultant effect of the respective parent plate which tends to increase feeding distance, and of the respective parasite plate which tends to reduce feeding distance. As the result the feeding distance of a plate at an intermediate position falls between the values obtained when located at the riser or at the casting edge extremities of a casting.

From the data of Table 3 and Fig. 5 the following empirical formula was derived for the feeding distance of intermediate plate sections:

$$D_m = 3.5\ T_h - T_1 \quad \ldots\ldots\ldots\ldots\ldots\ldots \quad (4)$$

D_m = Feeding distance in intermediate sections, in.

T_h = Thickness of parent plate, in.

T_1 = Thickness of parasite plate, in.

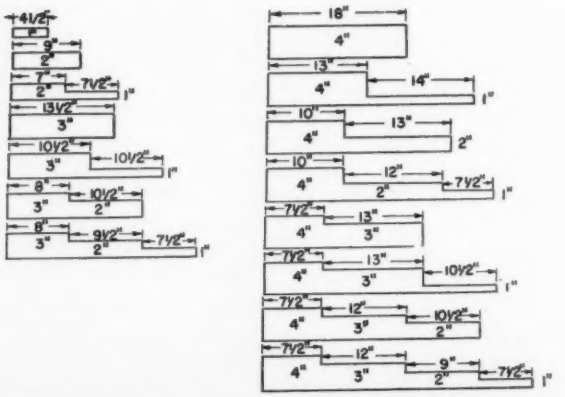


Fig. 6—Critical lengths of members of joined systems.

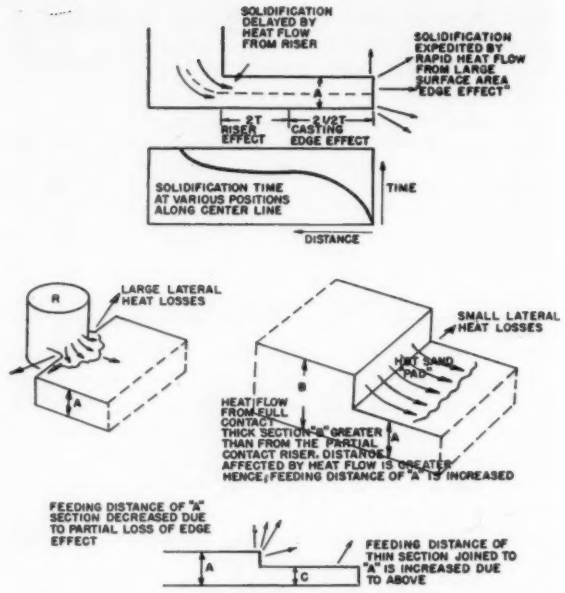


Fig. 7—Schematic drawings of effect of joining plate "A" to thicker section "B" and to thinner section "C."

The summary data obtained for both single and multiple joint sections are presented in Fig. 6 as sketches of the various combinations of joints possible with the four plate sizes studied; the maximum feeding distances are indicated for each section.

General Discussion

The maximum feeding distances in the parasite plates are as great as, or exceed, those which are obtained when the plates are cast separately and fed with conventional risers. This improvement, in the case of the 4 to 1 in. thickness combinations, where the T_h/T_1 ratio is 4, amounts to approximately 200 per cent (4½ in. to 14 in.). As the T_h/T_1 ratio decreases the improvement in feeding distance decreases until in the case of the 4 to 3 in. combination the maximum feeding distance in the 3-in. section is essentially the same as that obtained in separately cast 3-in. section. According to the formula $D_1 = 3.5\ T_h$

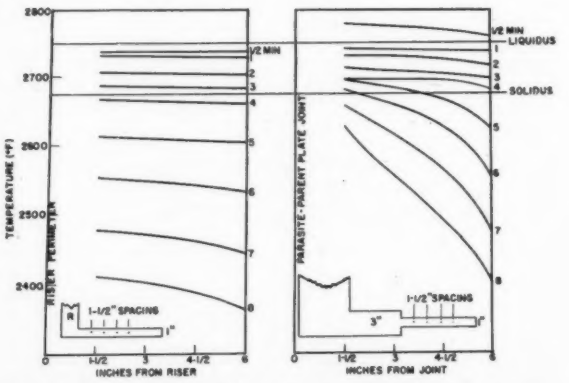


Fig. 8—Thermal gradients in solidifying 1-in. thick plates when fed by conventional riser (left) and 3-in. thick parent plate (right).

the feeding distances in the lighter sections should exceed the 4½ T obtainable in separately cast plates when the T_h/T_1 ratio exceeds 1.4, which is approximately that existing in the 4 to 3 plate combination. Thus when T_h/T_1 exceeds 1.4 the parent plate is more effective in promoting feeding of the parasite plate than a conventional round riser associated with a single plate of the thickness of the parasite. This is ascribed to the more pronounced thermal effects (heating pad effect of the hot sand) produced in the parasite plate by the re-entrant angle which is continuous along the plate width in the case of the joined sections. In the case of the round riser the re-entrant angle is limited to the small contact area, and is moreover of lesser intensity even over this smaller area due to lateral heat loss into cold sand. Figure 7 provides a schematic illustration of the heat flow conditions related to the modifications of feeding distances which have been described.

Figure 8 presents thermal data of the gradients which are developed in a separately cast 1-in. thick plate and the same plate joined to a 3-in. thick plate section. It is apparent that the greater heat concentration at the re-entrant corner of the joined plates is reflected in the development of the more extensive thermal gradients required to increase feeding distances.

As the T_h/T_1 ratio becomes less than 1.4 the effectiveness of the re-entrant angle in promoting more extensive thermal gradients is decreased and the feeding distance in the light section falls below 4½T. There is, at the same time, a simultaneous decrease in the feeding distance in the parent plate and as

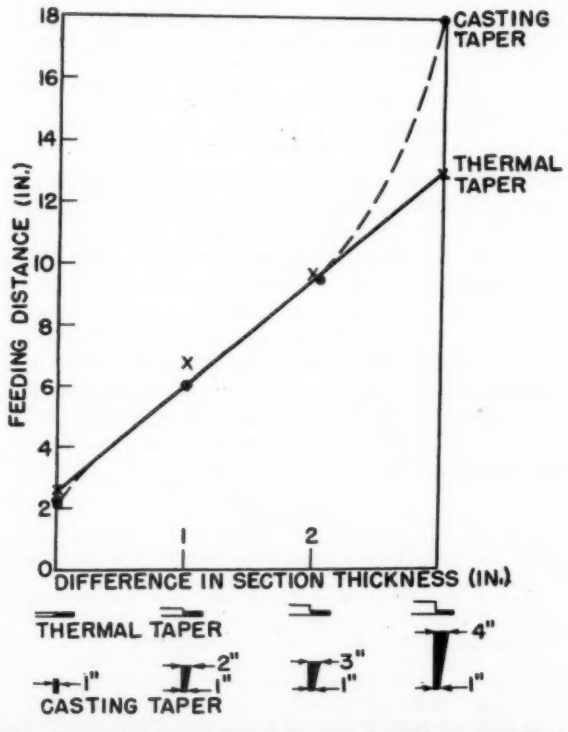


Fig. 9—Relation of thermal taper and casting taper (in.) to the feeding distance of 1-in. plates.

two plate sections approach a common thickness $T_h/T_1 = 1$, the sum of the two feeding distances approaches 4½T; i.e., the value of a separately cast plate.

In cases where the T_h/T_1 ratio is above 1.4, the thermal effect on "thermal taper" developed within a parasite plate by a given height of step at the re-entrant angle is essentially as effective in promoting soundness as is a *like amount of actual taper* on the parasite casting. Figure 9 presents the feeding distances possible in a 1-in. thick parasite plate joined to parent plates of 1, 2 and 3 in. greater thickness. On the same curve are plotted data obtained by Brinson and Duma[4] showing the feeding distances possible in 1-in. sections when the taper added to the sections is equivalent to steps of 1, 2 and 3 in. Agreement is very close except for the case of the most extreme height of step and taper.

Within the thickness limits studied, the parasite plate always acts to decrease the end effect of the parent plate with the result that the feeding distance of the parent plate is always less than the 4½ T which could be obtained in a separately cast plate. However, according to the formula $D_h = (4.5\ T_h \times .707^{T_1}) + 1$ it is deduced that if the parasite plate is made sufficiently thin it should act as a chill and revert to increasing the feeding distance in the parent plate; a maximum increase of 1 in. is predicted as the parasite plate thickness approaches zero. For example, this formula indicates that such a chilling effect will be exerted on a 3-in. thick parent plate when its parasite plate is less than ⅕ in. thick. While such a small chill effect is difficult to demonstrate by radiographic determination of soundness since it lies within the range of reproducibility of the test data, it may be shown by thermal analysis studies. Such studies were made on four 3-in. thick plates molded in one flask and gated thru a common sprue to eliminate metal and temperature variables. One plate was cast separately and to the others were attached parasite plates having thicknesses of 1, ½, and ⅛ in. Thermocouples were located in the 3-in. thick plates at distances of 3, 5 and 7 in. from the joint positions. The gradients developed in each of these castings at various times after pouring are shown in Fig. 10; the progression of the "end of freeze" point (i.e., the times at which various points along the centerline of the casting cool below the solidus temperature) from the end of parent casting are shown in Fig. 11. It can be noted, Fig. 10, that gradients are definitely steepened by the presence of the ⅛-in. thick parasite plate which is reflected in a significant increase in the rate of progression of solidification, Fig. 11. These effects are similar, though to a lesser degree, to those observed in previous studies of chills.[2]

Conclusions

1. The feeding distances of single (separately cast) plates are modified greatly by the addition of thicker (parent) or thinner (parasite) plates. In a series of plates of progressively smaller thicknesses the intermediate plates behave both as parent and parasite types to the two adjoining plates, resulting in fur-

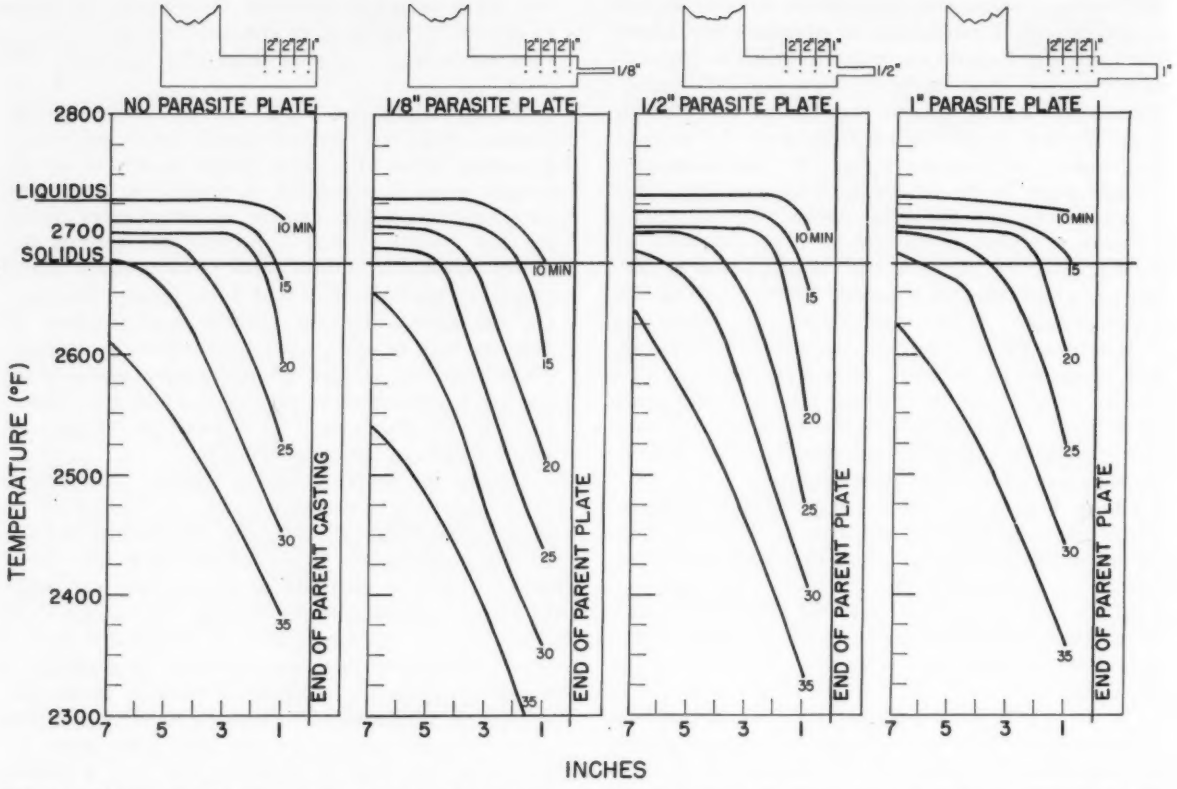


Fig. 10—Thermal gradients produced in 3-in. thick parent plates attached to parasite plates of thicknesses indicated.

ther modification of feeding distances for the intermediate plates.

2. Parasite plates located at the casting end (strictly parasite types) can be fed for a distance which is proportional to the thickness T_h of their adjoining parent plates. The feeding distance (D_1) is expressed by the empirical formula (1) $D_1 = 3.5\ T_h$.

3. Parent plates located next to the riser (strictly parent types) can be fed to soundness for a distance which is inversely proportional to the thickness (T_1) of their adjoining parasite plates. The feeding distance (D_h) of the parent plate is expressed by the empirical formulae:

(2) $D_h = (4.5\ T_h \times .707^{\ T_1}) + 1$ in.

(3) $D_h = 3\ (T_h - T_1) + 4.5$ in. (limited application)

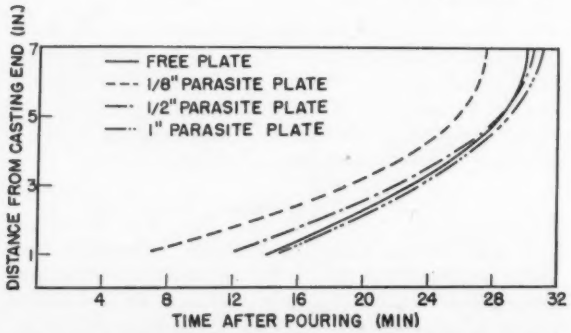


Fig. 11—Progression of "end of freeze" waves from cold end of 3-in. thick castings when separately cast and when attached to parasite plates of various thicknesses.

4. Intermediate plates which serve a dual function of parent and parasite plates can be fed for a distance which is determined by the thickness of the adjoining plates (T_h and T_1). The feeding distance (D_m) of intermediate plates is expressed by the empirical formula (4) $D_m = 3.5\ T_h - T_1$.

5. The mutual effect of joined dissimilar sections is, in most cases, one of reducing feeding distance in the larger section and increasing feeding distance in the small section beyond the $4\frac{1}{2}$T obtainable in the same sections when cast separately. However, very large or very small thickness differences cause these respective effects to be reversed. A T_h/T_1 ratio of 15 or greater will cause the parasite plate to act as a mild chill upon its parent plate and increase feeding distance (very slight effect, however) in the parent plate to above $4\frac{1}{2}$ T; a T_h/T_1 ratio of 1.4 or less will minimize the heating effect of the parent plate upon its parasite plate and decrease feeding distance in the parasite plate to below $4\frac{1}{2}$ T.

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FACTORS INVOLVED IN MAKING A SAND MIXTURE

PRELIMINARY STUDY NO. 1*

By

D. F. Baker** and D. C. Williams†

ABSTRACT

The text of this work deals with the preparation of foundry sand mixtures, the evaluation of the sand mixtures in terms of their mechanical properties, and the distribution of the constituents in the sand mixture. The objective of the work was to explore the possibility of relating the development of green compressive strength in the sand mixture to the energy required to turn the mixer during the process of mixing.

The experimental apparatus consisted of a ribbon-type mixer driven through a variable speed drive by an electric motor. The energy requirement of the driving motor was measured with a recording wattmeter.

Initial tests with clay-and-water mixtures revealed that there was, in this particular mixer, a variation in the mixing action, distribution of the materials. and energy requirement due to the tendency of the materials to adhere to the sides and blades of the mixer. The addition of water to the sand mixture was found to cause a significant increase in the energy required to turn the mixer.

Subsequent tests using sand, water and kerosene revealed that water additions to sand alone cause significant increases in energy requirement, but that kerosene additions to sand alone cause practically no increase in energy requirement and that the presence of one pct kerosene in a sand-water mixture effectively inhibits the increase in energy requirement that would otherwise occur as the result of the presence of the water.

Introduction

The general area of this study has to do with the preparation of sands as used in the foundry industry. In recent years compounded sand mixtures are replacing to a considerable extent the naturally-bonded sand mixtures. In many cases the naturally-bonded sands were mixed by the forces of nature, and the variations in the ratio of the sand, clay-mineral, and organic material as mined were accepted. With the exhaustion of suitable deposits of naturally-bonded sand mixtures, and because of the need in the automotive industry for large quantities of molding sand of uniform properties, the compounded sand mixtures were developed.

In general, the selection of the sands and clay-minerals used in foundry sands has been done empirically, and the blending or mixing of these materials has been a significant and unsolved problem of the operation. The industry is using compounded sand mixtures successfully, but there are many variations between the individual plants in the proportions of the clay, water, sand, the order of addition, the type of mixing equipment, time of mixing, etc. The criteria used for evaluating the sand mixture for control purposes vary from the traditional "feel test" to the more elaborate tests for green, dry, and hot compressive strength, flowability and permeability. There are so many indeterminate factors involved in the bonding action of the sand, clay-mineral and water mixtures that we have been forced to develop tests that supposedly simulate mold conditions in order to evaluate the molding sands. Most significant is the fact that all compounded sand mixtures and core sands must be mixed or prepared.

Many Mixing Problems

Mixing is a common operation in a great many processes, and in the mixing of foundry sands there are many questions for which we do not have answers. We do not know how the relatively small amounts of water and clay-mineral become mixed with the sand, the speed at which the mixing takes place or the factors that affect the speed, nor how to determine when optimum mixing has been achieved. It was expected from the first that a simple solution for these mixing problems would not be found.

The method of approach to the problem was to study the properties of the materials—quartz sand, clay-minerals, and water—as they are involved in mixing and the phenomenon of bonding. A study was made of the technical papers dealing with the properties of these materials and with the bonding action of clay-minerals. Literature from the fields of geology, mineralogy, ceramics and colloidal chemistry was found to contain much information about the theoretical atomic structures and the physical and chemical properties of the clay-minerals and quartz sand.

The standard practice for sand mixing in industry is to mix the dry ingredients and then add the water. The evaluation of the effectiveness of the mixture is

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almost invariably based on the results of the standard green compressive strength test.

The idea of studying the torque or the energy required to turn the mixer for studying the induced shear as a function of green strength in the sand mass was suggested by the discussion of thixotropy (isothermal reversible decrease in consistency with flow) by Lewis, Squires, and Broughton.[1]

From the study of the phenomenon of thixotropy with the bentonite and fire clay suspensions it was thought there might be a difference in the mixing of compounded sand mixtures, depending upon the type of mixer used. It was reasoned that if the sand mass was subjected to a continuous shearing force rather than the intermittent shearing force of a muller-type mixer, the sand mass would be less likely to undergo thixotropy during the mixing process. A ribbon-type mixer seemed to better meet the requirements for continuous shearing action.

It was decided to set up a mixer equipped with a device for measuring energy input when preparing compounded sand mixtures using both bentonite and fire clay. The basic hypotheses to be tested were 1) that after all of the constituents of the sand mixture were in the mixer the torque necessary to turn the mixer would increase with time as the mixing progressed, 2) that the change in order of addition of the clay and water would give a different maximum value of torque, and 3) that different time-torque relations would be found for bentonite and fire clay.

It was recognized from the first that the limitations of the equipment and apparatus would have to be determined after once set up, and that the results of initial tests might dictate the future course of the project. The emphasis of the project was to be placed on the investigation of the relationship of torque to mixing on an exploratory basis.

Equipment and Apparatus

A small ribbon-type mixer illustrated in Fig. 8, was connected to a source of power through a variable speed drive. A recording watt meter was connected to the motor.

The only reference found in the literature suggesting that the energy requirement of the mixing had been used as a method of evaluating green compressive strength of the sand mixture was reported by Sheehan[2] in connection with a paper on urea formaldehyde core binders. In the discussion period following Sheehan's presentation of the paper, Mr. Sheehan reported that a paddle-type mixer was used in preparing core sand mixtures, and that an ammeter had been connected to the motor as a guide to the operator to prevent overloading the motor. He reports that the ammeter reading served as a measure of the "toughness" of the sand mixture. It seemed possible, therefore, to use the energy requirement of the motor as an indication of the relative changes in torque required to turn the mixer.

[1] W. K. Lewis, L. Squires, and G. Broughton, *Industrial Chemistry of Colloidal and Amorphous Materials*, pp. 243-249, The MacMillan Co., New York, 1943.

[2] J. J. Sheehan, "Synthetic Resins as Core Binders," *The British Cast Iron Research Association Journal of Research and Development*, vol. 3, no. 3, p. 228, Dec., 1949.

The uncertain and exploratory nature of the investigation did not, in the authors opinion, warrant special materials such as graded sands, etc., and it was decided to use the foundry laboratory materials that were available. The sand was No. 100 damp Glassrock sand. For the sake of uniformity, all of the sand used in the investigations (approx. 1 ton) was spread on the laboratory floor and air dried, and then stored in a canvas-covered bin from which it was used during the investigation. A particle-size distribution curve representing an average of the results of four determinations is shown in Fig. 1. Western bentonite was used, fire clay was obtained from Southern Ohio, and tap water was considered satisfactory.

Procedure and Results

The first step in the investigation was to determine the behavior of the mixer and the motor under load and no-load conditions with dry, clay-free sand. No detectable change in mixer speed was found in going from no-load to mixer capacity. It was decided that the optimum batch size was between 30 and 50 lb,

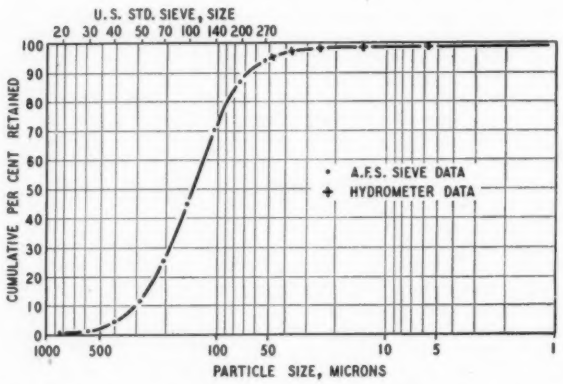


Fig. 1—Particle size distribution of No. 100 damp Glassrock sand.

and that the optimum speed was between 70 and 110 rpm. Before starting a series of tests the mixer was run at a constant speed with a load of dry sand. Following the warm-up period the mixer was emptied and cleaned.

The procedure for the sand-clay-water mixtures is outlined as follows:

1) The mixer motor is started and the mixer speed set to 110 rpm.

2) The electrical circuit to the wattmeter is closed.

3) 47 lb of sand are dumped in the mixer.

4) The clay or the water, depending upon the order of addition, was added. The clay addition was made by dumping the clay into the mixer from a long pan in such a way that the clay was distributed uniformly and parallel to the axis of rotation of the mixer blades. Water was added by pouring it into the mixer while moving the container back and forth along the axis of rotation. It required about 9 sec to add 3 lb of water.)

5) The other constituent, either clay or water, was added 3 min after step 4) had been started.

The wattmeter tape speeds were usually 1/10 in. per min, but a few batches were run on tape speeds of 3 in. per min. Figures 2 and 3 are rectangular coordinate reproductions of typical tape records of the sand-clay-water batches. The particular batch represented by Fig. 2 was made, following the steps in the outline of the previous paragraph, as follows:

1) Start motor—mixer speed 110 rpm.
2) Close circuit to wattmeter.
3) Dump in 47 lb sand.
4) Dump in 3 lb bentonite.
5) Add 4 lb water 3 min after step 4).

The batch represented by Fig. 3 was made in exactly the same way except that steps 4) and 5)—the order of addition of the clay and water—were reversed. The tape speed for both batches was 1/10 in. per min.

The other sand-clay-water mixtures gave similar wattmeter recordings, although the batch size, mixer speeds, tape speeds, type of clay, and percentages of clay and of water were varied. A study of the wattmeter tape revealed the following information:

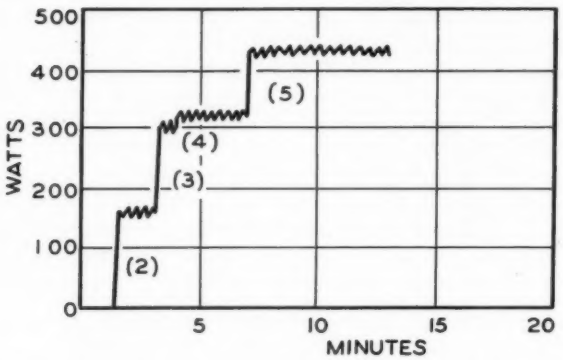


Fig. 2—Energy requirement of motor for—sand 3), clay 4), water 5)—order of addition.

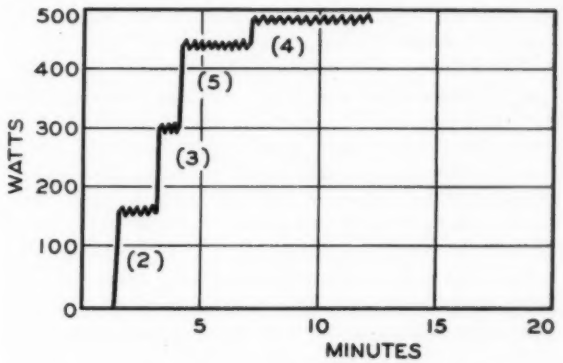


Fig. 3—Energy requirement of motor for—sand 3), water 5), clay 4)—order of addition.

a) The addition of the water caused an immediate and significant increase in energy requirement regardless of the order of addition. The increase in energy requirement due to the addition of water to the sand alone was always equal to or greater than the increase caused by the addition of water to the sand-clay mixture. There was no detectable build up of energy requirement after all of the water was in the mixer, even on tape speeds of 3 in. per min.

b) The energy requirement increase due to the addition of clay was somewhat greater if the clay was added after the water, but was always less than the increase caused by the addition of water.

c) In the case of all of the batches in which the factors or steps were identical, except for the order of addition, the batches to which the water was added first always had an equal or higher ultimate energy requirement than those to which the clay was added before the water.

The reason for the significant increase in energy requirement due to the addition of water is not clear. The fact that the energy increased immediately as the water was added suggests, first, that the increase in energy requirement was due only to the weight of the material in the mixer. However, if the water was added to the sand-clay mixture the energy increase was somewhat less than the energy increase for the addition of water to the sand alone. This suggested that factors other than the mass of the materials in the mixer were involved in changing the energy requirement. It is conceivable that the clay particles were able in some way to take up part of the water and thus eliminate the effectiveness of part of the water as far as the energy requirement is concerned.

Observation of the mixing action in the case of the sand-clay-water mixture brought to light at least one factor of some significance. The materials in the mixer behaved in a different manner, depending upon the order of addition. If the water was added first, the blades and sides of the mixer became coated with a sand-water mixture to which the sand-clay-water mixture (after the addition of the clay) did not tend to adhere as much as in the case of the clay addition to the sand before the water. The net result was that the addition of clay before the water gave a mixing action that was rather incomplete, in that a somewhat greater proportion of the material was not in the mixing action. The fact that there was a greater proportion of the material out of the mixing action for batches in which the clay was added before the water may explain in part the lower final energy requirement for batches of that order of addition.

Because of the significance of the energy requirement increase due to the water addition it was decided to direct the experimental work to a study of the water-sand mixture. The reason for the large increase in energy requirement due to the water addition is not clear. There are a number of factors which may have a part in the explanation of the effect of water in a water-sand mixture. A few of the factors which may be involved in the increase in energy required to turn the mixer as water is added to sand are as follows:

a) The total mass in the mixer increases.

b) The bulk density of the sand-water mass decreases initially.

c) The coefficient of friction between the sand grains may change.

d) The gas-solid interface system is replaced by a complex gas-liquid-solid interface system in which a

complex factor of surface tension may be involved.

A sand grain is thought by some authors to carry a negative surface charge. The exact nature of the source of the charge or a quantitative measure of the charge has not been discovered by a search of the literature. The molecules in the polar liquid (water) are thought by Davies[3] to become oriented with the surface of the sand grain in a series of molecular layers. After a limited amount of water is added—the limit depending upon the strength of the surface charge on the sand grains, the temperature of the mass, and the total surface or fineness of the sand—additional water is not held in oriented molecular layers, but is held in a random manner about the sand grain and oriented molecules by surface tension.

It was considered worthwhile to investigate the change in energy required to turn the mixer by changing specific conditions in the mixture and noting changes in energy requirement, if any. It was expected that the amount of energy required to turn the mixer would change if the temperature of the mixture was changed, if the residual negative charge on the grain was altered, and if a liquid having a much lower dipole moment than water was substituted for water in the mixture.

The investigation of the effect of the condition changes mentioned above is a realistic and practical approach in that the foundryman must cope with such changes in daily mixing operations. In operations using compounded sand mixtures the sand is often remixed before it has had time to cool. Hot sand is difficult to temper and to bond in order to obtain an effective molding sand. It is thought by many that it is impossible to rebond sand that has been subjected to a temperature exceeding 2200F, and some claim that the critical temperature is much lower.

The sand grain is thought to lose the ability to be rebonded, because heat in some way alters the residual negative charge on the sand grains. The use of liquids less polar than water is significant in the mixing operation in that core sands are prepared with various more or less non-polar core oils. Although the bonding action of oil-bonded sands is different from that of the clay-bonded sands, the problem of mixing the oil-bonded core sands is a significant factor.

It was not expected that the results of the investigation of the effects of the changed conditions in the mixture upon the energy required to turn the mixer would explain completely the mechanics of the mixture. It was hoped some new information might be gained about the factors involved in the mixing and bonding of the materials.

A series of test batches of sand and water were made. The steps in the experimental procedure are as follows:

1) Start motor—mixer speed 96 rpm.
2) Close circuit to wattmeter.
3) Dump 50 lb sand in mixer.
4) Add 1 lb water to batch each min.

[3] W. Davies, *Foundry Sand Control*, p. 59, Lund Humphries and Company Limited, London and Bradford, 1950.

The result of the procedure outlined above is shown in Fig. 4. The first pct water addition (1 lb) causes an immediate increase in energy requirement of about 50 watts. Accompanying the increase in energy requirements there is a noticeable decrease in the bulk density of the mixture. Each succeeding water addition, up to the 8th to 10th increment of water addition, results in a smaller, but definite, increase in energy requirement than the preceding addition. The bulk density of the mass begins to increase after the initial decrease. At or about the time the energy requirement is at a maximum the voids in the mass become filled with water and the mass then takes on the characteristics of a highly viscous liquid.

In order to examine the effect of temperature changes on the energy required to turn the mixer, batches were made in the same way using heated water, heated sand, and both heated water and heated sand. The sand was heated in an electric oven to

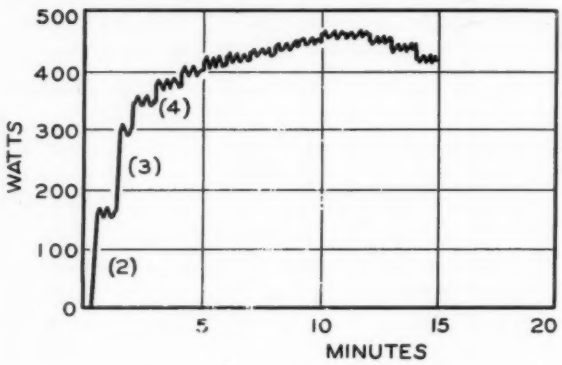


Fig. 4—Energy requirement of motor for sand 3) to which one lb of water 4) is added each minute.

about 250F. The water was heated in a bucket over a gas flame to the boiling point. The temperature of the mixture was measured at the end of the test in each case. The temperature at the end of the run for each batch was as follows:

Batch No. 1. Sand 75F — water 60F — temperature at end, 80F.
Batch No. 2. Sand 250F — water 60F — temperature at end, 130F.
Batch No. 3. Sand 75F — water 170 - 212 F — temperature at end, 110F.
Batch No. 4. Sand 250F — water 170 - 212F — temperature at end, 105F.

The energy requirement curves for batches 2 and 3 were not significantly different from that of batch 1. There was a notable change in the curve obtained when using both heated sand and heated water. Fig. 5 shows that the initial water addition and subsequent water additions do not cause as great an increase in the energy requirement.

There are a number of factors which may be responsible for the difference in energy requirement between batch 1 and 4. In accordance with the fixed water theory the number of oriented molecular layers of water would be greatly reduced by the increase in temperature, and at the same time the viscosity of

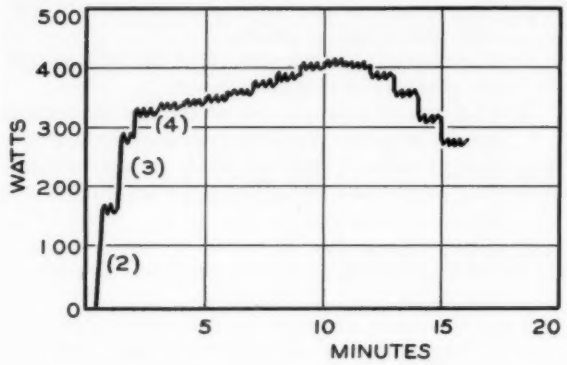


Fig. 5—Energy requirement of motor for hot sand 3) to which one lb of hot water 4) is added each minute.

the water decreases tension at the sand-air-water interface may decrease.

The effect of water additions to the sand mixture at regular time intervals had been noted. It was decided to make test batches involving a relatively non-polar liquid. A commercial grade kerosene was chosen as the non-polar liquid to be used. Three batches involving kerosene were made. In the first, kerosene was added to 50 lb of dry sand at the rate of one lb per minute. The total increase in energy requirement during the entire run was less than 20 watts compared to an increase of almost 170 watts when water was added under the same conditions. There was comparatively little increase in the energy requirement due to the addition of the kerosene.

The second batch involving kerosene was made in accordance with the following procedure:

1) Motor started—mixer speed 110 rpm.
2) Closed circuit to wattmeter.
3) Added 50 lb sand.
4) Added ½ lb kerosene (zero time).
5) Added 1 lb water at 3, 4, 8, 11, 12-19 min.

It was noted that the energy requirement does not increase in the same way that it did when water was added alone. The initial water additions cause little or no increase in the energy requirement. The increment water additions near the maximum or peak values of energy requirement caused greater changes

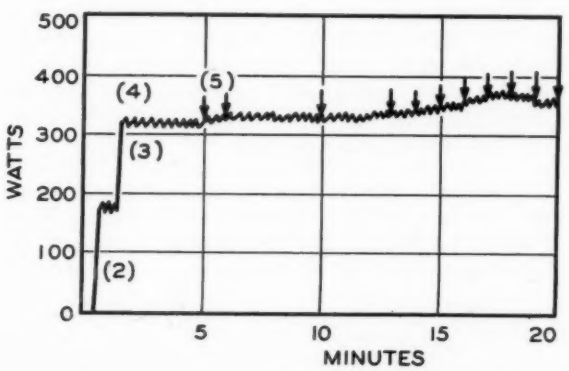


Fig. 6—Energy requirement of motor for sand 3) to which is added ½ lb kerosene 4) followed by one lb water 5) additions.

in energy requirement on both sides of the peak of the curve than the initial water additions. Figure 6 shows the interpretation of the results of the wattmeter recording.

The third test batch involving kerosene was run to find the changes in energy requirement that would occur if a relatively large quantity of water was added to the sand followed by the addition of a small quantity of kerosene. Figure 7 shows the rather striking results of the test batch. In this batch 5 lb of water was added to 50 lb of sand, and after 3 min ½ lb of kerosene was added. The time required to add the water was about 20 sec (by stop watch) and the time required to add the kerosene was about 5 seconds.

The energy requirement increased directly as the water was added to about the same level that five separate 1-lb water additions would give, and that the addition of the kerosene caused an immediate drop in energy requirement to a level equal to the energy

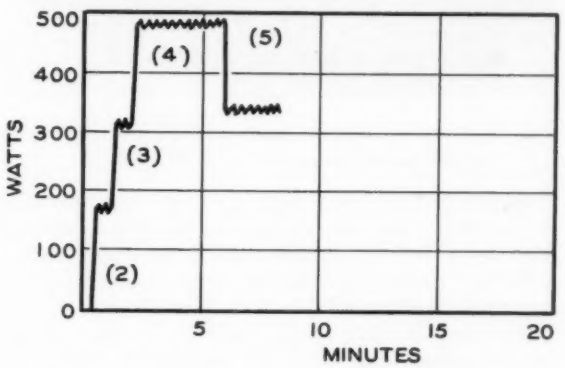


Fig. 7—Energy requirement of motor for sand 3) to which 5 lb of water 4) is added followed by the addition of ½ lb of kerosene 5).

level attained in the equivalent mixture of the batch in which the kerosene was added first.

Summary and Recommendations

Observation of the mixing action involving sand-clay-water mixtures does bring to attention the fact that for uniform distribution of the materials in the mass, all of the mass must be consistently and uniformly involved in the mixing action. The significance of the fact that batches made up with the sand-water-clay order of addition had a higher ultimate energy requirement than batches with the sand-clay-water order of addition is difficult to evaluate because of the difference in mixing action. One obvious reason for the difference in the energy requirement was that a greater portion of the sand mixture (sand-clay-water order of addition) was not engaged in the mixing action.

A study of the wattmeter record tapes reveals that water in the mixture contributes more to the energy requirement of the mixer in proportion to its weight than either the clay or the sand. The effect of the water addition upon energy requirement is very rapid, and to a considerable extent independent of the order of addition. No detectable lag was noted be-

tween the time the water was added to the batch and the change in the position of the recording pen, even on recording speeds of 3 in. per min. There was no appreciable increase in energy requirement after both clay and water were added that could be attributed to the development of green strength due to the mixing action.

The results of the experiments involving sand and water are extremely difficult to evaluate. It is possible to speculate that in the batch using hot water and hot sand only, the lower energy requirement was due to the change in viscosity of the water resulting

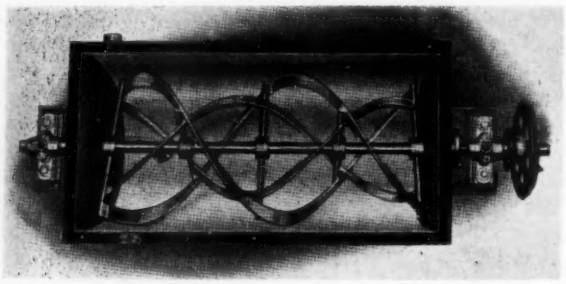


Fig. 8—Interior view of small ribbon-type mixer with double spiral blade used in the investigation.

from the change in temperature. The "rigid or fixed water" theory would also apply in that it is contended that fewer molecular layers of oriented water molecules are held at the surface of the sand grains at higher energy levels.

Kerosene in sand and in sand-water mixtures has a pronounced effect on the amount of energy required to mix the mass. Kerosene with sand causes little increase in energy requirement above that required to mix the sand alone. If kerosene is added to the sand, subsequent additions of water cause little change in energy requirement and the addition of kerosene to a sand-water mixture essentially eliminates the energy requirement attributed to the water.

The wattmeter reading gives a summation of all of the energy requirements of the motor, driving mechanism, and mixing action. In addition to the energy required to overcome the friction and other losses in the mechanical system, there are many factors in the mixing action of the sand mass which may contribute to the total energy requirement. A number of considerations which may be of significance in quantitatively evaluating the energy required in mixing are as follows:

1) The total surface of the sand mass.
2) The density of the mass, particle size distribution, and shape of grains.
3) The character of the surface of the sand grains, i.e., presence of surface films, etc.
4) The density and viscosity of the liquids present and their polar characteristics and surface tension in contact with the solids in the mixture.
5) The nature and strengths of the bonding of the forces in the mixture.
6) The method and direction of application of the forces of induced shear and the frequency of the application of such forces.

Further work is necessary in order to evaluate the use of energy input to the mixer as a tool for evaluating the mixing action and/or the properties of the mixture. The aim of future investigations should be to learn more of the phenomenon of the mixing and bonding of the materials and to find a better and faster method of mixing the materials and of evaluating the final mixture in terms of its ultimate use.

It is conceivable that the technique of measuring the energy input to the mixer could be used as a tool for studying the forces of adhesion in green molding sand or that such technique could be used as a device for quality control of foundry sand mixtures.

Further investigations might be devoted to the evaluation of factors which seem to be significant but as yet have not been explained or evaluated. The following are suggestions for future investigations of mixing and bonding foundry sand mixtures:

1) Make a comprehensive study of the literature for information about the phenomena of surface films, surface tension, surface charge, interfacial phases, and wetting that apply to the mixing and bonding of materials in foundry sand mixtures.

2) Investigate the energy requirements and mixing action of other types of mixers and/or variations of the ribbon-type mixer in the mixing of foundry sand materials.

3) Use graded sands and sands which have been subjected to high temperature or other actions which may alter the surface of the sand grain or surface film on the sand grains, in order to evaluate the effects of the total surface of the sand mass with respect to factors related to the surfaces and the liquid-solid interfaces.

The significance of the technique of the measurement of energy input to the mixer as a tool for investigating the problem of mixing foundry materials has not been determined. The limited exploratory investigation for this report has brought up far more questions than it has answered.

MAGNESIUM SAND CASTING ALLOYS CONTAINING THORIUM*

By

K. E. Nelson**

Applications for sand-cast magnesium alloys at elevated temperatures have been, primarily, parts used in aircraft jet engines. The favorable strength to weight ratio of magnesium alloys at operating temperatures has been, in part, responsible for the successful operation of such applications. The jet engine designs are continually being changed to give increased power and this leads to higher operating temperatures for engine parts.

In order to continue to have the advantages of magnesium available as far as stressed, elevated-temperature applications are concerned, it has been necessary to explore intensively the field of elements alloyable with magnesium and to evaluate the properties of new magnesium alloys with a view to their use at these temperatures. From such a program have come several alloys worthy of development.

The magnesium-aluminum-zinc system of alloys suitable for some applications at elevated temperatures were partially replaced by the various alloys in the magnesium-rare earth metal-zirconium system. This transition has been reviewed by Nelson and Strieter.[1] In 1943 the Alien Property Custodian published two United States patent applications covering magnesium-thorium-zirconium base alloys.[2,3] Subsequent publications by Sauerwald[4] and Leontis[5,6] further report on the properties of these alloys and their superiority to the magnesium-rare earth metal-zirconium alloys at elevated temperatures.

The addition of zirconium to magnesium-thorium alloys as reported by Leontis[6] markedly reduces the grain size and improves both castability and tensile properties without impairing creep resistance at elevated temperatures. Leontis also determined that thorium additions of about 3 per cent and maximum zirconium contents, in the range of 0.7 per cent, result in an alloy which would be suitable for many stressed applications at these elevated temperatures. A United States patent recently issued to Magnesium Elektron Limited covers a series of magnesium-thorium-zinc-zirconium alloys.[7] In a paper presented at the 1952 Magnesium Association meeting Ball[8] presented data on a sand casting alloy termed ZT1 (magnesium plus 3 per cent thorium plus 2.5 per cent zinc plus 0.7 per cent zirconium) which has the same composition as HZ32A-T5 alloy discussed in this paper. An article in a recent publication discusses the properties of this creep resistant alloy.[9]

The magnesium-thorium system of alloys are being thoroughly evaluated by The Dow Chemical Company. The information in this paper presents results of these studies available at the present time. It is expected that these data will be found useful by the many people interested in applications requiring resistance to creep at elevated temperatures.

One objective of this paper is to compare the tensile and creep properties of separately cast test bars of magnesium-thorium-zirconium alloys with and without the addition of zinc. Room temperature tensile properties of bars sectioned from test castings of these two compositions are also compared. A second objective is to evaluate the tensile and creep properties of bars sectioned from castings of two ternary magnesium-thorium-zirconium alloys in a manner described by Nelson and Strieter.[10] For the purpose of definition in this paper, the alloys to be evaluated may be listed using the proposed ASTM designations for magnesium alloys as follows:

HK31A-T6	Magnesium plus 3 per cent thorium plus 0.7 per cent zirconium
HK21A-T6	Magnesium plus 2 per cent thorium plus 0.7 per cent zirconium
HZ32A-T5	Magnesium plus 3 per cent thorium plus 2.5 per cent zinc plus 0.7 per cent zirconium

T5 and T6 are temper designations. The T5 treatment is an artificial precipitation aging consisting of 16 hr at 600 F. T6 refers to a solution heat treatment plus artificial precipitation aging of the alloy. In this case the treatment consists of 2 hr at 1050 F, air cool, and age for 16 hr at 400 F.

Procedure

The alloying of thorium to the magnesium was accomplished as described by Leontis[5] with slight modifications. In addition to introducing thorium in the

* This work was sponsored, in part, by the Flight Research Laboratory, Wright Air Development Center.

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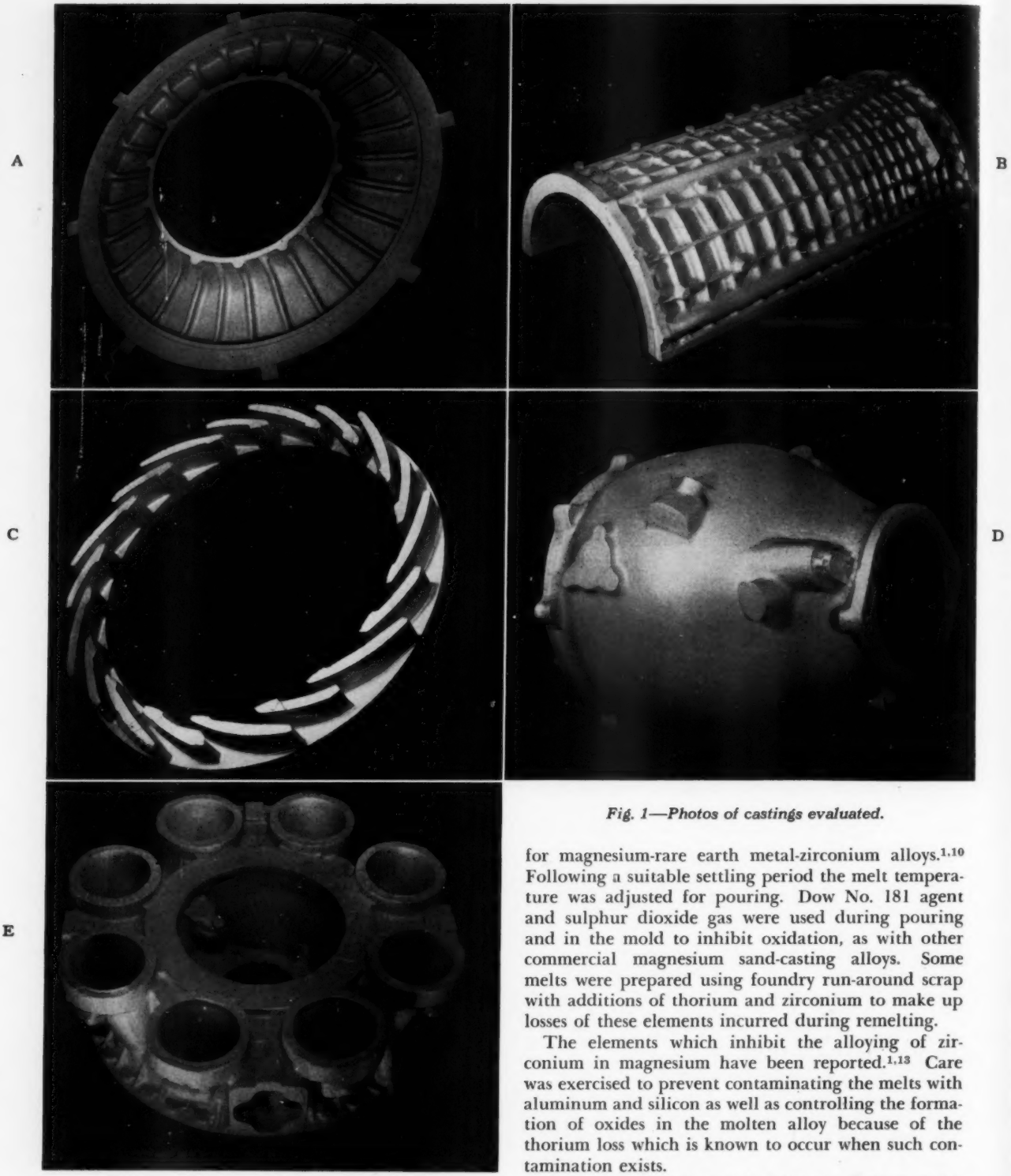


Fig. 1—Photos of castings evaluated.

metallic form, it was, in several cases, added as a hardener of magnesium-thorium. The zirconium was alloyed using the commercial hardener containing magnesium and 30 to 50 per cent zirconium described by McDonald[11] and Saunders and Strieter.[12] The fluxing practices for melting and refining were the same as for magnesium-rare earth metal-zirconium alloys.[1,10] Following a suitable settling period the melt temperature was adjusted for pouring. Dow No. 181 agent and sulphur dioxide gas were used during pouring and in the mold to inhibit oxidation, as with other commercial magnesium sand-casting alloys. Some melts were prepared using foundry run-around scrap with additions of thorium and zirconium to make up losses of these elements incurred during remelting.

The elements which inhibit the alloying of zirconium in magnesium have been reported.[1,13] Care was exercised to prevent contaminating the melts with aluminum and silicon as well as controlling the formation of oxides in the molten alloy because of the thorium loss which is known to occur when such contamination exists.

Five patterns were used to make the castings which were evaluated. The HK21A alloy was poured using two of these patterns while four were used with the HK31A alloy. The pouring temperature of the metal, and the gating and risering of the casting were the same as those used by the foundry in pouring EK30A alloy (magnesium plus 3 per cent rare earth metal

TABLE 1—ANALYSIS AND GRAIN SIZE OF CASTINGS EVALUATED

Composition	Castings Poured	Analysis							Range Average Grain Diam., In.	
		Th, %	Zn, %	Soluble Zr, %	Insoluble Zr, %	Fe, %	Mn, %	Ni, %	Separately Cast Test Bars[1]	Castings[2]
HK31A-T6	Separately Cast Bars	3.56	<0.01	0.72	0.03	0.002	0.020	<0.001	—	—
HZ32A-T5	Separately Cast Bars	3.29	2.40	0.82	0.06	0.002	0.026	<0.001	—	—
HK31A-T6	Separately Cast Bars and Special Casting	3.12	<0.01	0.78	0.04	0.002	0.018	<0.001	—	—
HZ32A-T5	"	3.15	2.34	0.87	0.03	0.001	0.026	<0.001	—	—
HK21A-T6	A	2.24	<0.01	0.63	0.05	<0.001	0.056	<0.001	0.003	0.0025 —0.004
HK31A-T6	A	2.57	<0.01	0.68	0.04	0.002	0.035	<0.001	0.003 —0.008	0.003 —0.004
HK31A-T6	B	2.65	<0.01	0.70	0.04	0.002	0.060	<0.001	0.003	0.0025 —0.005
HK21A-T6	C	1.84	<0.01	0.63	0.07	0.002	0.062	<0.001	0.003 —0.0045	0.003 —0.0045
HK31A-T6	D	2.65	<0.01	0.70	0.04	0.002	0.060	<0.001	—	0.002 —0.015
HK31A-T6	E-1	2.57	<0.01	0.68	0.04	0.002	0.035	<0.001	—	0.003 —0.015
HK31A-T6	E-2	3.55	<0.01	0.69	0.02	0.002	0.012	<0.001	0.002 —0.003	0.002 —0.006

1. Each range represents from 3 to 6 average grain size ratings.
2. Each range represents from 19 to 24 average grain size ratings.
In some cases even though the range showed fine average grain sizes, the samples from the casting had scattered grains as coarse as 0.015 in.

plus 0.25 per cent zirconium) production castings. In addition to these castings, separately cast ½-in. diam standard tensile bars were cast from each melt. The castings were heat treated in production furnaces along with the separately cast bars using the procedure described in the introduction to this paper. Standard separately-cast test bars of HK31A and HZ32A alloys were poured and heat treated using laboratory equipment. The test castings, designed to produce conditions of cooling similar to those found in production parts, were also made in the laboratory. These castings weighing 10 lb were heat treated prior to sectioning. The solution treatment was done under a protective atmosphere of greater than 1 per cent SO_2 gas. The precipitation treatment was done in air. After heat treating, each casting was sectioned as completely as possible from thick and thin sections in order to obtain a complete picture of its room and elevated-temperature tensile properties and elevated-temperature creep strength. Figure 1 shows castings similar to the ones used to compare the ternary magnesium-thorium-zirconium alloys in this program.

The thorium, zinc and zirconium contents of each melt were analyzed by wet chemical methods. The zirconium contents are designated as "soluble" or "insoluble" and refer to that portion present in the alloy which is soluble or insoluble in dilute acid. The usual impurities, including iron, were determined spectroscopically. The analysis for each melt is given in Table 1.

The casting sections were machined to conform to standard ASTM specified shapes for tensile testing, or, when this was not possible, to dimensions listed as acceptable to the ASTM. Specimens machined for creep testing were either 0.505 or 0.450 in. in diameter with a 2-in. gage length or ½ in. by ¼ in. in cross-section with a 2-in. gage length.

TABLE 2—EFFECT OF TESTING TEMPERATURE ON SEPARATELY CAST TEST BAR TENSILE PROPERTIES OF HK31A-T6 AND HZ32A-T5 ALLOYS

Composition	Testing Temperature, F	Properties[1]		
		% Elongation in 2 in.	Yield Strength X1000 psi	Tensile Strength X1000 psi
HK31A-T6	70	8	18	37
HZ32A-T5	"	10	15	32
HK31A-T6	400	17	14	26
HZ32A-T5	"	33	10	18
HK31A-T6	500	19	14	24
HZ32A-T5	"	39	9	14
HK31A-T6	550	18	13	22
HZ32A-T5	"	38	8	13
HK31A-T6	600	22	12	20
HZ32A-T5	"	38	8	12
HK31A-T6	662	21	10	17
HZ32A-T5	"	28	8	12
HK31A-T6	700	26	9	14
HZ32A-T5	"	29	7	11

[1] These values are averages of three tests at each temperature.

The methods of tension and creep testing have been discussed in detail in previous publications.[10,14,15]

Hardness values were also obtained in a manner previously described.[10] The alternate immersion corrosion data reported were obtained using a technique described by Hanawalt, Nelson, and Peloubet.[16] It consisted of immersion of finely machined and de-

greased specimens in a 3 per cent NaCl water solution for ½ min followed by 2½ min in air in a 90 F room. The alternate immersion was carried on continuously for 14 days.

Comparison of HK31A-T6 and HZ32A-T5 Alloys

The room and elevated temperature tensile properties of separately-cast test bars of HK31A-T6 and HZ32A-T5 alloys prepared and heat treated in the laboratory are shown in Table 2. The short-time tensile properties of HK31A-T6 alloy are superior to those of HZ32A-T5 alloy at all testing temperatures.

A comparison of the room temperature tensile properties of separately-cast test bars with bars sectioned from a special test casting poured from the same batch of metal is made for the two compositions in Table 3. The superiority of the HK31A-T6 alloy in short-time tensile properties is again apparent.

TABLE 3—COMPARISON OF ROOM TEMPERATURE TENSILE PROPERTIES OF SEPARATELY CAST TEST BARS WITH BARS SECTIONED FROM A TEST CASTING

Composition	Test Bar Source		Number Bars Tested	% Elon. in 2 in.	Tensile Properties: Yield Strength X1000 psi	Tensile Properties: Tensile Strength X1000 psi
HK31A-T6	Sep. Cast Bars	Average	2	8	18	34
"	Sep. Cast Bars	Minimum		6	18	32
"	Test Casting	Average	50	5	16	30
"	Test Casting	Minimum		1	14	22
HZ32A-T5	Sep. Cast Bars	Average	2	10	17	33
"	Sep. Cast Bars	Minimum		7	16	33
"	Test Casting	Average	80	4	16	28
"	Test Casting	Minimum		<0.5	12	20

In order to provide information which would be helpful for design purposes as well as to compare the two alloys, creep tests were conducted at several levels of stress at 400, 500, 550, 600, and 662 F for times up to 1000 hr.

Two parameters of creep were used to make this comparison:

1. Creep limit based on 0.2 per cent total extension in a given time.
2. Creep limit based on 0.5 per cent total extension in a given time.

These parameters were obtained by interpolation of log stress *vs* log extension plots of the original data as described in earlier papers.[14,15] Figures 2 through 6 compare the two alloys for various time intervals for the five testing temperatures.

It can be seen that HK31A-T6 alloy is superior to HZ32A-T5 alloy at 400 F and for the higher extensions of creep at 500 F for time through 1000 hr. The times at which HZ32A-T5 becomes superior to HK31A-T6 at higher testing temperatures can be determined from the curves. HZ32A-T5 alloy is superior for times of 100 hr or longer at temperatures of 550 F and higher. Actually in 1000 hr HZ32A-T5 at 600 and 662 F is comparable to HK31A-T6 at 550 and 600 F, respectively.

There are applications in air-frame parts such as missiles, and in some parts in jet engines, where the critical parameter, as far as design purposes are con-

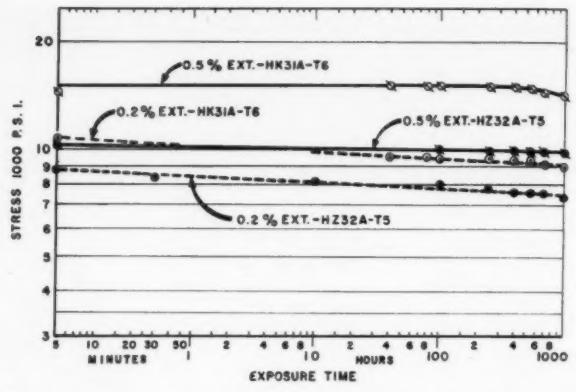


Fig. 2—Stress for 0.5 pct and 0.2 pct total extension at 400 F. Separately cast test bars. (The abscissa, exposure time, actually is creep testing time.)

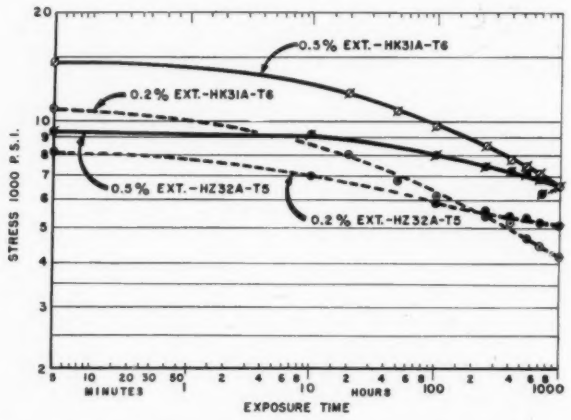


Fig. 3—Stress for 0.5 pct and 0.2 pct total extension at 500 F. Separately cast test bars. (The abscissa, exposure time, actually is creep testing time.)

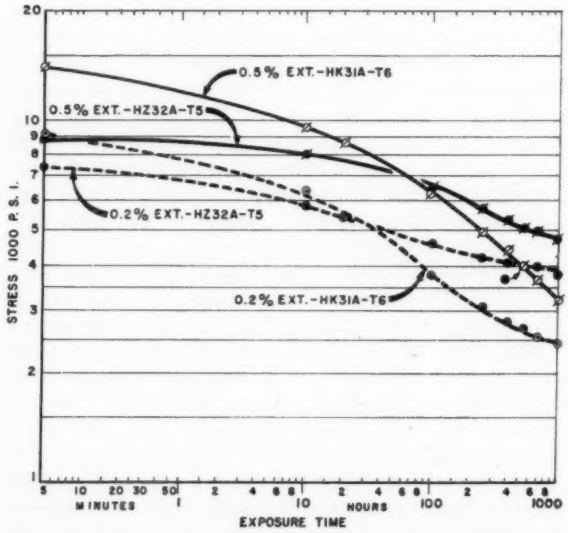


Fig. 4—Stress for 0.5 pct and 0.2 pct total extension at 550 F. Separately cast test bars. (The abscissa, exposure time, actually is creep testing time.)

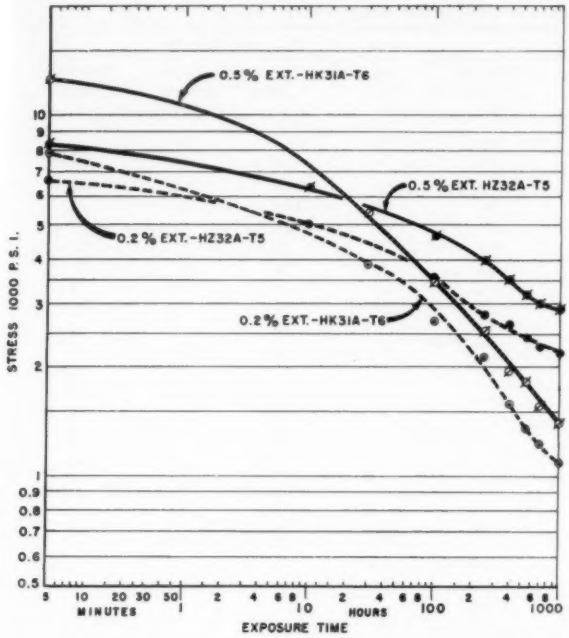


Fig. 5—Stress for 0.5 pct and 0.2 pct total extension at 600 F. Separately cast test bars. (The abscissa, exposure time, actually is creep testing time.)

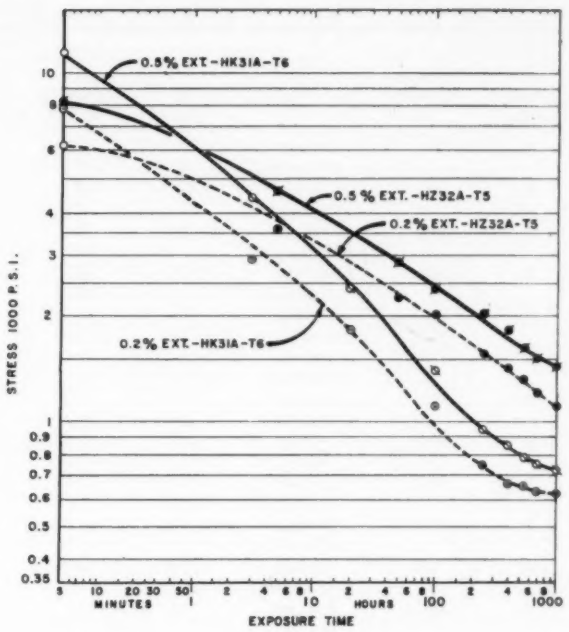


Fig. 6—Stress for 0.5 pct and 0.2 pct total extension at 662 F. Separately cast test bars. (The abscissa, exposure time, actually is creep testing time.)

cerned, is maximum stress for a short time at relatively high temperatures. Under such conditions, if a choice were to be made between HK31A-T6 alloy and HZ32A-T5 alloy, it would be necessary for the designer to make an educated guess or have basic information on stresses, temperatures, and time involved. With this information it would be possible to choose the optimum alloy composition for a given application.

In general the tensile and creep properties reported by the British investigators for ZT1 alloy[9] agree with the results obtained on the HZ32A-T5 alloy.

Property Evaluation Made on Ternary Magnesium-Thorium-Zirconium Alloy Castings

The average grain sizes in the reduced section of the standard ½-in. diam tensile bars and in bars sectioned from the castings were measured using the comparison method described by George.[17] The analyses and grain sizes of the castings are presented in Table 1.

The minimum and the mean tensile properties at room temperature, 400 F, 500 F, 600 F, and 650 F of test bars machined from individual castings are shown in Table 4. As the thorium content increases from 1.84 per cent to 3.55 per cent, the yield strength increases. The effect of casting size, which influences cooling rates, on the properties of a 2.57 per cent thorium alloy can be seen by comparing the properties of castings B and D. As would be expected, the properties in casting D which has the thinner wall sections are somewhat higher than those in casting B poured from the same melt. The coarse grain size found in castings D, E-1, and E-2 did not appear to affect the tensile properties of bars sectioned from the castings.

The tensile results of the bars sectioned from castings were analyzed statistically as described in an earlier paper.[10] The average and standard deviation values are listed for the two thorium alloys at the five test temperatures in Table 5. Also listed are the number of values used for each determination. In general the data from which the values in Table 5 were obtained formed a fairly normal distribution. In a few cases, however, there was some question of this, the data indicating one or more factors out of control. The mean values were then used. The bar graph in Fig. 7 shows the effect of testing temperature on the tensile properties of the two alloys. It may be noted that the average room temperature properties obtained from bars sectioned from HK31A-T6 castings in Table 5 compare very closely to those reported in Table 3 for the special HK31A-T6 casting.

The tensile properties of the separately cast test bars which were poured and heat treated with the castings are presented in Table 6. In general these values are as good as, or better than, those of bars sectioned from castings of the same melt. It can be seen that the values for separately-cast bars of HK31A-T6 alloy reported in Tables 2 and 3 are higher than those in Table 6. It is felt that the control of the heat treatment of those treated in the laboratory was better than those treated using production equipment, thus resulting in superior properties.

Average tensile properties of bars sectioned from castings of EK30A-T6, EK31A-T6, and EZ33A-T5 alloys reported in another paper[10] are compared with similar data from the two ternary magnesium-thorium-zirconium alloys in Table 7. The increased elongation and ultimate tensile strength of the thorium alloys over the rare-earth metal alloys at 70 F enhances the value of the thorium-containing system. In yield

TABLE 4—TENSILE PROPERTIES OF BARS SECTIONED FROM INDIVIDUAL CASTINGS

Casting Number	Testing Temp., F		HK21A-T6 No. of Bars	HK21A-T6 % Elong. in 2 in.	HK21A-T6 Yield Strength, X1000 psi	HK21A-T6 Tensile Strength, X1000 psi	HK31A-T6 No. of Bars	HK31A-T6 % Elon. in 2 in.	HK31A-T6 Yield Strength, X1000 psi	HK31A-T6 Tensile Strength, X1000 psi
A	70	Average	17	8	15	33	23	7	16	33
		Minimum		7	14	31		4	15	29
A	400	Average	18	18	12	23	19	19	12	24
		Minimum		8	11	21		14	12	22
A	500	Average	20	23	13	21	2	23	11	22
		Minimum		12	11	19		22	11	22
A	600	Average	19	25	10	18	19	24	11	20
		Minimum		21	8	16		18	10	19
A	650	Average	20	28	9	15	2	18	9	15
		Minimum		21	8	14		12	9	13
B	70	Average	—	—	—	—	14	6	14	30
		Minimum						4	14	25
B	400	Average	—	—	—	—	11	17	12	23
		Minimum						12	11	22
B	500	Average	—	—	—	—	2	18	11	20
		Minimum						17	11	20
B	600	Average	—	—	—	—	9	22	10	18
		Minimum						18	10	17
B	650	Average	—	—	—	—	2	23	8	15
		Minimum						23	8	15
C	70	Average	18	8	13	32	—	—	—	—
		Minimum		7	12	19				
C	400	Average	14	20	11	20	—	—	—	—
		Minimum		17	10	19				
C	500	Average	16	24	11	19	—	—	—	—
		Minimum		16	10	18				
C	600	Average	16	22	10	17	—	—	—	—
		Minimum		14	9	16				
C	650	Average	16	23	7	14	—	—	—	—
		Minimum		14	7	14				
D	70	Average	—	—	—	—	29	6	17	30
		Minimum						3	14	26
D	400	Average	—	—	—	—	12	18	13	23
		Minimum						14	12	22
D	500	Average	—	—	—	—	2	20	12	21
		Minimum						20	12	20
D	600	Average	—	—	—	—	4	19	11	18
		Minimum						16	11	17
E-1	70	Average	—	—	—	—	32	6	16	31
		Minimum						3	14	26
E-1	400	Average	—	—	—	—	13	17	13	23
		Minimum						12	12	23
E-1	600	Average	—	—	—	—	13	21	11	19
		Minimum						17	10	18
E-2	70	Average	—	—	—	—	31	4	17	31
		Minimum						2	16	26
E-2	400	Average	—	—	—	—	11	10	14	26
		Minimum						8	13	25
E-2	600	Average	—	—	—	—	13	17	12	20
		Minimum						10	11	18

TABLE 5—AVERAGE TENSILE PROPERTIES AND STANDARD DEVIATION OF BARS SECTIONED FROM HK21A-T6 AND HK31A-T6 CASTINGS

Testing Temp., F		HK21A-T6 % Elong. in 2 in.	HK21A-T6 Yield Strength, X1000 psi	HK21A-T6 Tensile Strength, X1000 psi	HK31A-T6 % Elong. in 2 in.	HK31A-T6 Yield Strength, X1000 psi	HK31A-T6 Tensile Strength, X1000 psi
70	Average	7.7	13.9	32.1	5.5	16.2	30.8
	Std. Deviation	0.9	1.1	1.1	1.6	1.1	2.4
	No. of Bars	35	35	35	129	128	129
400	Average	19.2	11.6	21.7	16.0	12.8	23.9
	Std. Deviation	—	0.9	1.2	3.0	0.8	1.3
	No. of Bars	32	32	32	66	68	66
500	Average	23.4	11.6	20.1	20.0	11.6	22.1
	Std. Deviation	—	0.8	1.2	—	—	—
	No. of Bars	36	36	36	6	6	6
600	Average	23.8	9.8	17.4	20.8	10.8	19.1
	Std. Deviation	2.2	0.5	0.9	3.8	0.5	0.7
	No. of Bars	35	35	35	58	58	58
650	Average	25.6	8.0	14.7	20.7	8.6	15.0
	Std. Deviation	5.0	0.7	0.7	—	—	—
	No. of Bars	36	36	36	4	4	4

Note: Where no standard deviation is listed the average value for that property is the arithmetic average of the individual values.

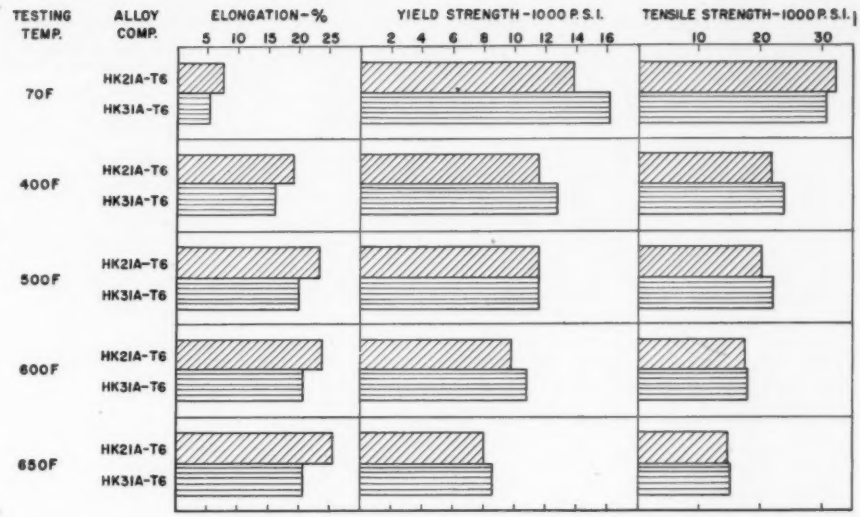


Fig. 7—Effect of temperature on tensile properties of bars sectioned from Mg-Th-Zr castings.

strength, the thorium alloys are slightly better than the rare earth metal alloys. In tensile properties at temperatures above 500 F, thorium additions to magnesium-zirconium produce compositions superior to the magnesium-rare earth metal alloys. Figure 8 shows the effect of testing temperature on the tensile properties of bars sectioned from EK30A-T6 and HK31A-T6 alloy castings.

Samples were taken from sections of a few of the castings for Brinell hardness tests. The average values for each casting are shown in Table 8. Casting C, which has the lowest thorium content and the lowest tensile properties, also has the lowest Brinell hardness number.

The 100-hr creep limits of bars from each HK21A-T6 and HK31A-T6 casting tested at 400, 500, 600, and 650 F are shown in Table 9. The geometry of casting D was such that it was impractical to obtain creep

TABLE 6—COMPARISON OF TENSILE PROPERTIES OF SEPARATELY CAST TEST BARS POURED AND HEAT TREATED WITH MAGNESIUM-THORIUM-ZIRCONIUM CASTINGS

Castings Poured	Analysis, % Th	Sol. Zr	Insol. Zr	No. Bars	Test Temp., F	% Elong. in 2 in.	Yield Strength, X1000 psi	Tensile Strength, X1000 psi
A	2.24	0.63	0.05	2	70	9.0	15.6	34.7
				2	400	17.5	11.5	21.3
				2	500	23.8	11.8	20.5
				2	600	27.5	11.2	18.6
C	1.84	0.63	0.07	2	70	9.0	13.8	32.2
				2	400	19.2	10.4	19.7
				3	500	25.8	11.0	19.1
				2	600	29.0	9.8	17.7
				3	650	36.3	7.8	14.3
	Average of 2 melts from above			4	70	9.0	14.7	33.4
				4	400	18.4	11.0	20.5
				5	500	25.0	11.3	19.7
				4	600	28.2	10.5	18.2
				3	650	36.3	7.8	14.3
A and E-1	2.57	0.68	0.04	3	70	6.3	15.3	30.8
				3	400	18.8	11.1	21.7
				3	500	20.3	11.3	20.4
				3	600	20.5	11.3	19.6
B and D	2.65	0.63	0.07	2	70	7.2	16.5	33.1
				2	400	18.2	13.2	23.4
				2	500	20.8	13.2	21.5
				2	600	15.2	11.0	17.6
				2	650	19.0	9.6	15.2
E-2	3.55	0.69	0.02	2	70	6.0	16.8	32.8
				2	400	12.8	14.4	22.8
				2	500	18.0	13.8	21.3
				2	600	21.0	12.4	19.1
				4	650	21.0	10.8	17.6
	Average of 3 melts from above			7	70	6.5	16.1	32.0
				7	400	16.9	12.6	22.5
				7	500	19.8	12.6	21.0
				7	600	19.1	11.5	18.9
				6	650	20.3	10.4	16.8

TABLE 7—COMPARISON OF AVERAGE PROPERTIES OF BARS SECTIONED FROM MAGNESIUM-RARE EARTH METAL-ZIRCONIUM AND TERNARY MAGNESIUM-THORIUM-ZIRCONIUM ALLOY CASTINGS

Composition	Testing Temp., F	Number Bars	% Elong. in 2 in.	Yield Strength X1000 psi	Tensile Strength X1000 psi
EK30A-T6	70	175	1	13.3	17.5
EK31A-T6	"	165	3.5	15.3	22.1
EZ33A-T5	"	166	2.5	14.3	19.8
HK21A-T6	"	35	7.7	13.9	32.1
HK31A-T6	"	129	5.5	16.2	30.8
EK30A-T6	400	93	7.5	11.0	16.0
EK31A-T6	"	89	12.0	12.2	16.7
EZ33A-T5	"	90	14.0	9.8	18.0
HK21A-T6	"	32	19.2	11.6	21.7
HK31A-T6	"	66	16.0	12.8	23.9
HK21A-T6	500	36	23.4	11.6	20.1
HK31A-T6	"	6	20.0	11.6	22.1
EK30A-T6	600	80	39.	6.6	11.4
EK31A-T6	"	89	51	7.1	11.2
EZ33A-T5	"	102	44	6.7	11.3
HK21A-T6	"	35	23.8	9.8	17.4
HK31A-T6	"	58	20.8	10.8	19.1
HK21A-T6	650	36	25.6	8.0	14.7
HK31A-T6	"	4	20.7	8.6	15.0

limits on bars sectioned from this part. From three to four tests were necessary in order to obtain a curve suitable for obtaining the four reported creep limits. The type data obtained were the same as those reported on the magnesium-rare earth metal casting evaluation.[10] These may be defined as follows:

1. Limiting stress to give 0.1 per cent creep extension in 100 hr.
2. Limiting stress to give 0.1 per cent creep plus plastic extension in 100 hr.
3. Limiting stress to give 0.2 per cent total extension in 100 hr.
4. Limiting stress to give 0.5 per cent total extension in 100 hr.

The data in Table 9 indicate that, at a given testing temperature, there is no difference in creep resistance

TABLE 8—BRINELL HARDNESS VALUES OF TERNARY MAGNESIUM-THORIUM-ZIRCONIUM ALLOY CASTINGS

Composition	Casting Number	Average Brinell Hardness Number
HK21A-T6	A	54.4
HK31A-T6	A	56.5
HK31A-T6	B	55.8
HK21A-T6	C	49.7

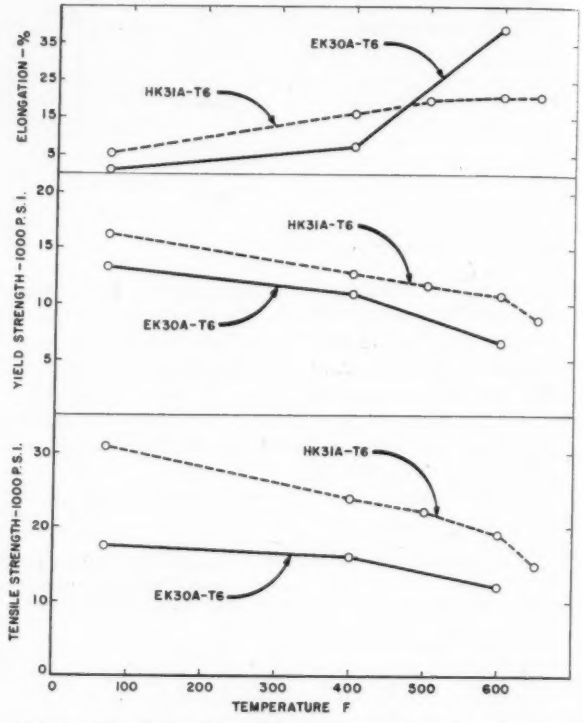


Fig. 8—Effect of testing temperature on tensile properties of bars sectioned from HK31A-T6 and EK30A-T6 alloy castings.

TABLE 9—RANGE—100-HR CREEP LIMITS OF BARS SECTIONED FROM INDIVIDUAL CASTINGS

Composition	Casting No.	Testing Temp., F	Number Curves	Range-Creep Limits, 1000 psi			
				0.1% Creep Extension	0.1% Creep + Plastic Extension	0.2% Total Extension	0.5% Total Extension
HK21A-T6	A	400	2	16.3-16.9	10.6-10.7	9.0-9.2	12.6-12.8
		500	2	5.9-6.2	6.1-6.2	5.7-6.1	8.2-8.9
		600	2	2.56-2.70	2.55-2.67	2.69-2.90	3.21-3.65
		650	2	1.09-1.22	1.07-1.20	1.28-1.31	1.49-1.52
HK31A-T6	A	400	2	18.9-19.2	10.3	8.7-8.79	12.7-13.1
		500	1	8.2	7.2	6.6	10.1
		600	2	2.20-2.29	2.18-2.29	2.31-3.07	4.14-4.59
		650	1	1.43	1.47	1.54	1.90
HK31A-T6	B	400	2	16.0-17.8	9.9-10.1	8.65-9.1	11.4-12.6
		500	1	7.0	6.8	6.2	9.6
		600	1	2.12	2.06	2.63	3.78
		650	1	1.24	1.23	1.39	1.64
HK21A-T6	C	400	4	16.5-18.5	9.2-10.3	8.7-8.9	11.5-12.4
		500	4	6.9-7.3	6.3-6.9	6.4-6.7	8.6-9.1
		600	4	2.35-2.79	2.32-2.76	2.62-3.11	3.38-3.89
		650	4	0.86-1.53	0.85-1.52	1.08-1.57	1.45-1.78
HK31A-T6	E-1	400	2	16.4-20.2	10.3-10.9	8.8-9.2	12.9-14.5
		600	2	2.10-3.04	2.09-3.02	2.40-3.41	3.23-4.59
HK31A-T6	E-2	400	2	16.2-18.5	11.0-11.3	9.0-9.6	14.1-14.5
		600	2	3.28	3.28	2.16-3.50	3.45-4.50

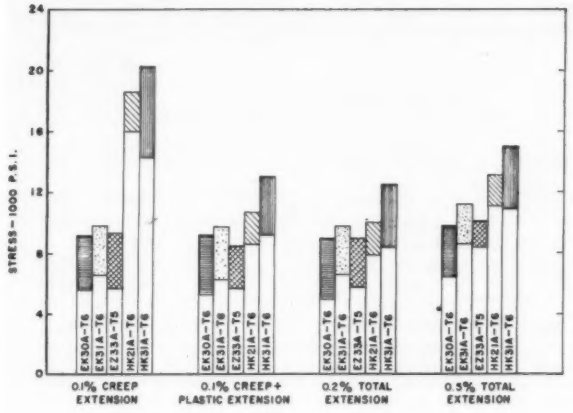


Fig. 9—Range, 100-hr creep limits. Bars sectioned from castings. Tested at 400 F.

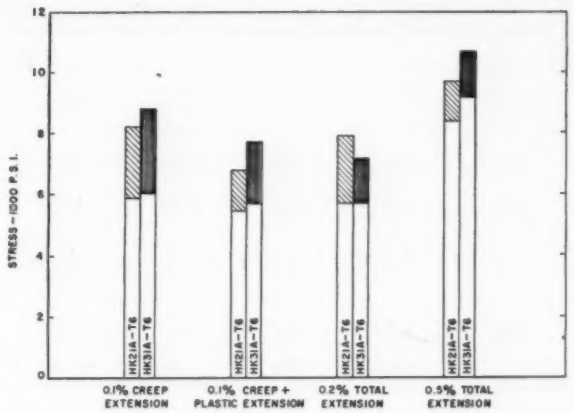


Fig. 10—Range, 100-hr creep limits. Bars sectioned from castings. Tested at 500 F.

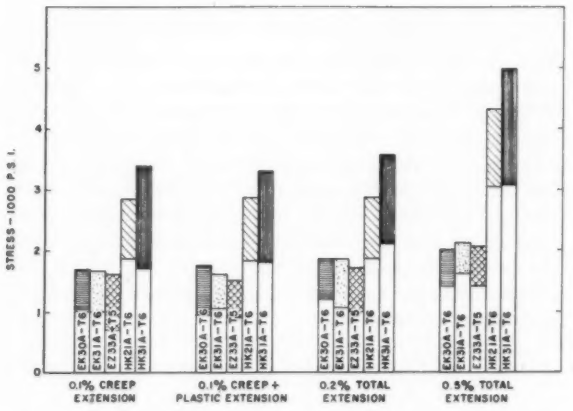


Fig. 11—Range, 100-hr creep limits. Bars sectioned from castings. Tested at 600 F.

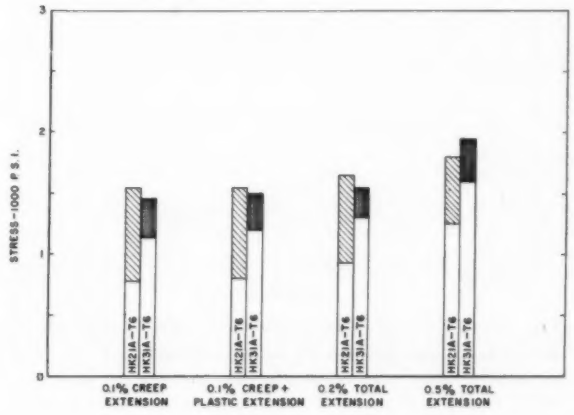


Fig. 12—Range, 100-hr creep limits. Bars sectioned from castings. Tested at 650 F.

between the two alloy compositions or from one casting to the other.

The stress for all bars of a given composition tested at a given temperature was plotted against the per cent extension in 100 hr for three parameters of creep: creep extension, creep plus plastic extension, and total extension. These data were then handled in the same manner as for the magnesium-rare earth metal-zirconium alloy castings.

The range of creep limits of the castings poured in the two compositions are compared with similar, previously reported data on EK30A-T6, EK31A-T6, and EZ33A-T5[10] for four parameters of creep at 400 F and 600 F in Figs. 9 and 11. Data on the two magnesium-thorium-zirconium alloys at 500 F and 650 F are compared in Figs. 10 and 12. These results indicate:

1. At 400 F the thorium alloys are slightly superior to the rare earth metal alloys.
2. At 500 F the thorium alloys are comparable to the rare earth metal alloys at 400 F.
3. At 600 F the thorium alloys are decidedly superior to the rare earth metal alloys.
4. At 650 F the thorium alloys are comparable to the rare earth metal alloys at 600 F.
5. The 100-hr creep limits shown for HK31A-T6 separately-cast bars in Figs. 2, 3, and 5 fall within the ranges shown for bars sectioned from HK31A-T6 castings in Figs. 9 through 11.

In order to compare the effect of temperature on the creep resistance of the two systems of alloys, a plot was made showing the limiting stress which would give 0.5 per cent total extension in 100 hr for several temperatures for EK30A-T6 and HK31A-T6. The values plotted in Fig. 13 were the center of the ranges for the two alloys shown in Figs. 9 through 12. The 500 F value for EK30A-T6 is the 0.5 per cent creep limit reported by Leontis on separately-cast test bars[17] and aids in comparing the two alloys.

Castings similar to those reported above have been made in HZ32A-T5 alloy and an evaluation to compare tensile and creep results on bars sectioned from these castings is being made. The data are not available at the present time. It can be expected, based upon our experience in comparing similar data on separately-cast bars and bars sectioned from castings on magnesium-rare earth metal and magnesium-thorium alloys, that HZ32A-T5 will show the same superiority in creep resistance at elevated tempera-

TABLE 10—ALTERNATE IMMERSION CORROSION TESTS IN 3% NaCl SOLUTION. 14 DAY TEST.

Analysis						Sample Source	No. of Tests	Range In Corrosion Rate (Mg/cm²/day)
Th, %	Soluble Zr, %	Insoluble Zr, %	Fe, %	Mn, %	Ni, %			
2.24	0.63	0.05	<0.001	0.056	<0.001	Separately Cast Test Bars	4	0.193 - 0.215
2.57	0.68	0.04	0.002	0.035	<0.001	Separately Cast Test Bars	8	0.230 - 0.692
"	"	"	"	"	"	Casting A	4	0.113 - 0.295
2.65	0.70	0.04	0.002	0.060	<0.001	Separately Cast Test Bars	4	0.207 - 0.229
"	"	"	"	"	"	Casting B	4	0.223 - 0.314
1.84	0.63	0.07	0.002	0.062	<0.001	Separately Cast Test Bars	4	0.189 - 0.223
3.55	0.69	0.02	0.002	0.012	<0.001	Separately Cast Test Bars	6	0.228 - 0.405

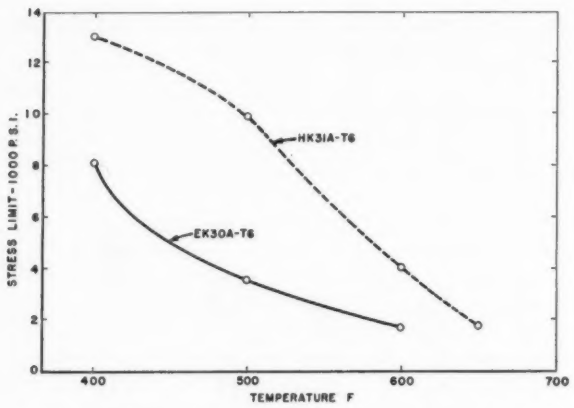


Fig. 13—Effect of temperature on creep resistance of HK31A-T6 and EK30A-T6 alloy castings. 0.5 pct total extension in 100 hr.

tures for long periods of time in castings that was found when comparing with HK31A-T6 separately-cast test bars.

Miscellaneous—Several samples machined from both separately cast test bars and castings in the ternary magnesium-thorium-zirconium alloys were tested in 3 per cent NaCl solution using the very severe alternate immersion method of testing. The results of these tests are presented in Table 10. The remarkable feature which these alloys exhibit is the near absence of pitting on the surface of the test piece. This fact, coupled with the low corrosion rates, indicates that the alloy, when chemically treated and painted, should withstand quite severe corrosive conditions before breaking down.

The ternary magnesium-thorium-zirconium and quaternary magnesium-thorium-zinc-zirconium alloys have been checked as regards chemical treatment. They are similar to the magnesium-rare earth metal-zirconium alloys in this respect. The following treatments can be satisfactorily applied:

1. Dow No. 1 chrome pickle (MIL-M-3171 Type I)
2. Dow No. 9 galvanic anodize (MIL-M-3171 Type IV)
3. Dow No. 10 sealed chrome pickle (MIL-M-3171 Type II)

The Dow No. 7 treatment (MIL-M-3171 Type III) cannot be used on the ternary alloy but the addition of zinc to produce HZ32A alloy results in a composition amenable to this treatment.

A factor which should be considered in a discussion on comparative features of various alloys is the linear shrinkage obtained in the mold upon freezing. Two different castings (castings A and B in Fig. 1) were used in obtaining data for such a comparison. The shrinkage figures in Table 11 represent the difference in the measurements of the pattern and the solidified casting at given locations. These measurements were made across relatively non-restrained areas of the casting. Casting design and the location of sand cores can change these values but the relative differences should remain the same. The ternary magnesium-thorium-zirconium alloys are similar to the ternary magnesium-rare earth metal-zirconium alloys as far as shrinkage is concerned.

TABLE 11—COMPARISON OF SHRINKAGE OF VARIOUS MAGNESIUM ALLOYS CAST IN SAND MOLDS

	Casting A		Casting B	
Composition	Average Shrinkage (in./ft)	% Shrinkage Over AZ92A Alloy	Average Shrinkage, (in./ft)	% Shrinkage Over AZ92A Alloy
AZ92A	0.141	—	0.133	—
EZ33A	0.186	32	0.169	27
EK30A	0.203	44	0.193	43
HK21A	0.198	40	——	—
HK31A	——	—	0.190	43

It has been established that the chemical toxicity of thorium metal or its alloys is low and presents no problem. Radiation striking the body from external sources of thorium in the form of magnesium-thorium alloy is believed to be less dangerous than that from a radium-painted watch dial. The normal precautions observed in dust control on any of the magnesium alloys, such as adequate collection and the use of dust respirators, should be followed in grinding magnesium-thorium metal. No interference either in the sending or receiving of radio or radar signals is expected from any part containing a magnesium-thorium alloy.

Conclusion

Three classes of magnesium sand casting alloys currently being produced for use in applications such as jet engines are (1) magnesium-aluminum-zinc; (2) magnesium-rare earth metal-zirconium, and (3) mag-

nesium-thorium-zirconium. These classes of magnesium alloys can be listed in increasing order of creep resistance as follows:

1. Magnesium-aluminum-zinc system.
2. Magnesium-rare earth metal-zirconium system.
3. Magnesium-thorium-zirconium system.

The various alloys within each of the first two systems are equivalent in resistance to creep. In the third system the addition of 2.5 per cent zinc to HK31A alloy to produce HZ32A alloy results in maximum creep resistance for long exposure times at temperatures of 500 F or for long or short time exposure at higher temperatures. Limitations of stress and time of operation at a given temperature determine the specific alloy composition for each application.

Both the HK type alloys and HZ32A alloy can be chemically treated to provide protection during shipment and storage, and a satisfactory paint base can be applied using the standard procedures recommended for commercial sand-cast magnesium alloys.

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DISCUSSION

Chairman: A. T. PETERS, The Dow Chemical Co., Bay City, Mich.

Co-Chairman: W. D. DANKS, Howard Foundry Co., Chicago.

Recorder: M. V. CHAMBERLIN, The Dow Chemical Co., Bay City, Mich.

W. C. NEWHAMS:[1] 1. Are thorium salts being used to alloy thorium with magnesium?

2. What dangers are involved in handling the thorium metal, the thorium salts or the master alloy of 25 per cent thorium and 75 per cent magnesium?

3. What is the required minimum and typical zirconium contents of the magnesium-thorium alloys?

MR. NELSON: 1. It is possible to reduce thorium halides with molten Mg to form Mg-Th alloys.

2. As indicated in the paper there is no danger in handling Mg-Th alloys in alloying, sawing, or machining operations provided the normal precautions recommended for all Mg alloys are practiced. It can be expected that Th metal can be considered in the same class. Due to the toxic nature of the salts it is recommended that adequate precautions be taken to prevent their introduction into the body. This would mean good ventilation, the use of respirators, and the reduction in exposure by the use of clean, dust-free clothing.

3. We have not established minimum Zr contents. However, it has been suggested by MEL in Great Britain that 0.55 per cent minimum Zr should be maintained. A typical Zr content would be 0.70 per cent. In both of the above figures we are referring to soluble Zr as defined in the paper.

P. D. FROST:[2] Have you tried making a hardener from metallic thorium, rather than from a salt? Have you been able to meet the nominal composition by introducing unalloyed metallic thorium directly into the heat?

MR. NELSON: Mg-Th hardeners have been produced from metallic Th. We have had reasonable success meeting nominal compositions using metallic Th as the addition agent. However, at the present time and with our present "know-how" we feel it much more expedient to produce hardeners for alloying purposes rather than adding Th directly to the pots.

R. A. WELLS:[3] Mr. Nelson indicated that a master alloy-type hardener is prepared, using thorium salts, to be used in the preparation of magnesium alloys containing thorium. We would be interested in knowing what thorium salts are used, and in what form they are used; i.e., are any proprietary salt mixtures available for this use?

MR. NELSON: As indicated in the reply to Mr. Newhams, thorium halides can be reduced by Mg. We are not in a position to discuss this fact any further. Proprietary salt mixtures have not been made available as far as we know. Mg-Th hardeners have been made commercially available. In selling and buying Th-containing material it is required by the AEC that those involved are licensed by the AEC.

W. J. KLAYER:[4] What is the foundry cost of the Mg-Th alloy?

MR. NELSON: This is a rather difficult question to which a specific answer can be given because operation costs vary from foundry to foundry. Each foundry knowing their operational costs and the costs of the raw materials can calculate these total foundry costs. The cost of the Th in the form of Mg-Th hardener will be around $30.00 per pound of Th.

MEMBER: 1. Can the loss of the thorium from the molten alloy be predicted by the length of the holding time?

2. How can thorium contamination of an alloy be removed?

MR. NELSON: 1. Losses in Th in well prepared melts should be due only to losses caused by oxidation of the magnesium. In controlled melts this oxidation should be at a minimum at all times. It can be expected that the higher holding temperatures will result in greater Th losses due to increased oxidation of the metal. We have held Mg-3.5Th melts at 1500 F for up to 7 hr with only a 4 per cent loss in the Th content.

2. If a Mg-Al-Zn alloy is contaminated with Th the best procedure would be to stir the melt thoroughly at 1400 F, cool it to around 1250 F and hold in a quiescent condition for 20 to 30 min. The top three quarters of the melt can then be salvaged by decanting without agitation leaving the contaminated metal in the crucible to be discarded into a sludge pan.

L. BADONE:[5] Mr. Nelson is to be congratulated on an excellent paper describing the potential advantage of the thorium-containing alloys for elevated temperature use in such applications as aircraft engines. From a foundry point of view we are interested in how these alloys could be expected to behave when poured into molds intended for aircraft quality castings. This could be vaguely described as the castability of magnesium-thorium-zirconium alloys. We do know that when the rare earth-containing alloys are compared with the aluminum-containing alloys there is quite a difference in foundry behavior. Compared to the aluminum-containing alloys the magnesium-rare earth-zirconium alloys of the EZ33 type are much more prone to oxidation during pouring and more sensitive to turbulence in the gating system because of this increased tendency towards oxidation. Consequently much greater care and attention has to be taken in the gating and pouring of the rare earth-containing alloys.

A compensation of the rare earth-containing alloys has been that they are less subject to shrinkage than the older aluminum-

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[5] Plant Metallurgist, Light Alloys Ltd., Haley, Ont., Canada.

containing alloys and consequently the feeding of castings has generally been an easier matter.

We would therefore like to know whether Mr. Nelson can compare these characteristics for the thorium-containing with those for the rare earth-containing and aluminum-containing alloys.

MR. NELSON: Mr. Badone has done a very excellent job of condensing the comparative castability of Mg-Al-Zn and Mg-RE-Zr alloys. The Th-containing alloys are similar to the RE-containing alloys in that they have a decreased shrinkage tendency over Al-containing alloys. It appears that the tendency of the Mg-Th-Zr alloys to oxidation is increased over that of the Mg-RE-Zr alloys. Therefore, even more attention must be given to gating and pouring of these alloys.

L. G. DAY: [6] Owing to the difference in chemical composition between the well-known magnesium alloys containing aluminum and zinc and magnesium-zinc-thorium alloys, there will also be a difference between the constitutional diagrams for these alloys particularly with reference to the liquidus and solidus lines. There may also be a difference in the latent heat of fusion and the total heat of the alloys. In view of these potential differences, has Mr. Nelson observed any difference in the fluidity or mold filling properties between the thorium-bearing alloys and the magnesium-aluminum-zinc alloys, and if so, has it been found necessary to cast the thorium-bearing alloys at higher temperatures than those normally employed for the magnesium-aluminum-zinc alloys?

MR. NELSON: We have poured castings in Mg-RE and Mg-Th alloys using the same pouring temperatures and gating techniques and find no difference in pouring rates. It has been observed, in some casting designs, that the Mg-RE alloys are less fluid than the Mg-Al-Zn alloys.

C. C. HITCHCOCK: [7] Does the presence of thorium in alloys of the EK30 type have the characteristic of raising the heat treat temperature and if so, is the SO_2 atmosphere sufficient at the high heat treat temperature?

MR. NELSON: The solidus of the Mg-Th-containing alloys is raised above that of the Mg-Al-Zn alloys and accordingly is comparable to the EK30 type alloy. In good air-tight heat treat furnaces it has been established that a minimum of 1 per cent SO_2 will adequately inhibit oxidation of the lower melting compound during the solution treatment.

M. E. BROOKS: [8] Our foundry has found it necessary, in order to prevent pitting of alloys of the EK30A type, to increase the SO_2 content of the heat treat furnace atmosphere from a minimum of 1 per cent to a minimum of 2 per cent.

WALTER BONSACK: [9] In Fig. 3 the author compares the creep strength of the two alloys at 500 F. The two curves indicate that although alloy HK31A-T6 has higher initial strength than the zinc-containing alloy, HZ32A-T5, it loses it faster and after 1000 hr has even lower strength. I would like to point out that this presentation might be misleading because two different alloys are compared in two different heat treated conditions, i.e., T6 and T5 condition. The rapid and greater loss of properties when alloys are solution heat treated is also common in aluminum alloys. In the T5 condition, the loss is usually smaller and occurs at higher temperatures.

I would like to know how alloy HK31A behaves in the T5 condition, and if the other alloy can be solution heat treated, how it acts in the T6 condition.

MR. NELSON: HK31A alloy has been spot checked in creep at 600 F after aging for 16 hr at 600 F—the same treatment given HZ32A-T5. The indications are that there is no difference in 1000 hrs creep resistance between HK31A aged for 16 hr at 600 F and that solution treated and then aged at 400 F. The T6 temper results in the optimum combination of tensile and creep strengths.

The possibility of improving HZ32A by a solution treatment prior to aging has been investigated by testing in short time tension and in creep for 100 hr. No beneficial effect of the solution treatment was observed.

W. E. SICHA: [10] 1. In connection with the creep data, will you define the term "Total Extension"?

2. It is my understanding that the properties of the magnesium-thorium alloys at elevated temperatures were for specimens heated at the testing temperature for short time periods prior to testing. What is the effect of longer heating periods on the tensile properties?

MR. NELSON: We have used three parameters in describing creep results. These are:

1. Creep extension.
2. Creep plus plastic extension.
3. Total extension.

Although we have attached engineering significance to all three in the past based upon what we could obtain from the field, it now appears that total extension might be the most valuable tool.

Creep extension refers only to that extension obtained after the load has been completely applied. The initial deformation during loading is neglected. Starting with a zero load and until the full load is applied two types of extension can take place—elastic and plastic extension. For loads well below the yield stress, the initial extension can be considered to be elastic only and can be calculated approximately by using the standard value of the modulus of elasticity at room temperature, for example, 6.5 million psi. For stresses in the neighborhood of the yield strength an additional 0.2 per cent should be added. These higher stresses result in permanent extension due to loading and is termed plastic extension. Creep plus plastic extension then is the elongation obtained after loading plus the permanent extension obtained during loading. Total extension is a summation of the total loading extension (elastic plus plastic) and the extension taking place after the load has been applied.

Work is not completed on the effect of longer exposure time at testing temperatures on tensile and creep properties. It can be expected, based upon the reported creep data, that at the lower temperatures the effect will be negligible. As the exposure and testing temperature is increased, some decrease in strength properties and creep resistance may be expected.

MR. NELSON *(Author's Closure)*: This paper presents considerable laboratory data on a new system of magnesium alloys to be used in stressed applications operating at elevated temperatures. Some of these data have been confirmed, based upon tests performed on bars sectioned from prototype castings. Tests are now underway to ascertain that the alloys will perform satisfactorily in test cells and later on in the field. When adequately proven out, a system of alloys will be offered which will make available the application of magnesium for use at temperatures as high as 650 F.

In the new Mg-Th-Zr system of alloys, as in all new developments, certain factors will require additional experience and possibly development before they can be handled in the foundry as easily as the older commercial alloys. It is expected that such problems will not be unsurmountable and that these alloys will be in production within a relatively short time.

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SOME FACTORS AFFECTING SPHERICAL TYPE MACRO-GAS POROSITY IN 85-5-5-5 BRONZE

By

R. B. Fischer*

ABSTRACT

The effect of mold materials, binders and coatings on the liberation of gas in 85-5-5-5 bronze when poured into dry sand mold cavities was investigated. Melting methods, degassifying and deoxidizing practices were also studied. The observations were made on a specially designed casting and the results recorded by photograph. More than 250 test castings have been examined.

Spherical type macro-gas porosity has been observed on 85-5-5-5 bronze poured into oil bonded silica sand molds. Gassing is the result of a metal-mold reaction or direct absorption of gas which is liberated upon solidification of the metal.

Many commercial sand binders were tested. Ounce metal poured in pitch-bonded molds showed no spherical type macroporosity. Cement bonded and natural sand molds produced castings practically free of the macro-gas porosity. Molds made with phenolic, urea, wood resins, etc., gassed ounce metal to various degrees of intensity. Graphite coatings containing pitch and a special coating containing zircon filler in a resin-volatile solvent vehicle inhibited or reduced the gas porosity. These coatings gave poorer casting surfaces.

Lithium was found to be an effective degassifier but was characterized by higher metal shrinkage. Deoxidizers were tested and their relation to shrinkage is shown on a special test casting. Metal composition also influences macro gas-porosity.

Introduction

Delivery of molten metal of proper composition to the mold with a minimum gas content is recognized to be of primary importance in producing sound copper-base alloy castings. Gas absorption by molten bronzes from furnace atmospheres is widely known to be a potential source of porosity. Foundrymen are becoming aware that even a heat of high melt quality can be gassed when the metal contacts the mold. Therefore, the effects of the mold material on the quality of copper-base alloys is receiving more attention.

It is natural that copper-base alloys should be "gassed" from mold materials because most all molds and cores used by foundrymen contain water and/or organic binders which "give off" gas and are a source for the formation of gas at elevated temperatures. The mold gases can be of the same type as those encountered in the furnace atmosphere during melting. The mold is an ideal mixing unit or container for gas absorption or reactions to take place because the metal, especially the first metal, that enters is in a turbulent state. The intimate mixing of metal and gas caused by turbulence is most likely to occur in the pouring basin or gating system but can occur elsewhere in the mold if the casting is of intricate design. Whether gas absorption or reaction happens is dependent on many factors, but among those to be discussed are the composition of the metal, melting methods, type of mold material and coating, degassification and deoxidation.

The data presented are mostly confined to dry sand molds and, although various copper-base alloys were poured for comparison, the intent was to concentrate on 85-5-5-5 bronze because it is one of the common commercial alloys.

The defect observed on production castings and the test casting is referred to as spherical gas porosity or macroporosity, and is easily seen on a cut or fractured section. The holes are usually close to the surface and are not visible until surface metal is removed. Gamma-ray radiography will show up extreme conditions as illustrated by the ounce metal pump casing in Fig. 1. This casing is typical of many castings containing the defect described when ounce metal is poured into oil bonded and baked silica sand molds. Acid metal bronze (89.5 pct copper, 8.5 tin, 2 lead, deoxidized with approximately 0.07 pct phosphorus) poured into the same mold material does not show this type of defect but it is a more expensive alloy. The ingot price is approximately 25 pct more than the 85-5-5-5 alloy. The main purpose of the investigation was to find methods of eliminating the spherical type macro-gas porosity in ounce metal.

Experimental Procedure

Figure 2 shows the principal dimensions of the test casting. The pouring height was established to simulate the gating of some typical "trouble maker" castings. The dimensions of the "bob" (3⅝-in. dia. x 9-in. height) were selected to give a test casting that would be sensitive to the liberation of the spherical type of gas. The 2-in. round stick allowed metal to flow through the "bob" and provided for some venting since it was open to the air. Some indication of the gassiness of the metal could be ascertained by correlating the solidification shrinkage in the top portion of the stick. If pronounced shrinkage was noted, it

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generally could be concluded that the "bob" was either free of the macro-gas porosity or contained only a small quantity. The top half of the "bob" was sectioned by cutting with a saw or cut-off wheel. The cut surface was inspected for the presence of macro-gas porosity and photographed for record. This method was used because of the difficulty of using any other technique for identification of the type of defect. It was not necessary to examine the entire "bob" because all gas evolution of the macro type was located only in the upper half. It is recognized that some of the photographs are not clear. They were selected from our practical record file and could not be specially prepared for reproduction in this paper as the samples were no longer available.

Except where noted, the heats were made with ingot metal (85-5-5-5 bronze; ASTM Spec. B30, Alloy 4A), to the following specification limits:

	Cu	Sn	Pb	Zn	Ni	Fe	Sb	S	Al	Si	P
Min.	84.0	4.25	4.0	4.5							
Max.	86.0	6.00	5.75	6.0	0.75	0.25	0.25	0.08	None	0.003	0.03

Heats were melted in four different type furnaces: 1) indirect-arc; 2) coke-fired; 3) oil-fired; 4) high-frequency induction.

Most of the heats were melted in the indirect arc type furnace. Standard melting practice prevailed except in a few instances when melting procedure was varied for comparison. After melting and superheating to approximately 2300 F, max., the metal was tapped from the furnace into a pouring ladle of approximately 400 lb-capacity. A zinc addition amounting to approximately 10 pct of the zinc in the ingot was plunged into the metal when the temperature dropped to 2200 F or below to make up for loss during melting.

The 85-5-5-5 alloy melts were deoxidized with a ladle addition of 2 oz 15 pct phosphor-copper shot per 100 lb of metal. The temperature of the metal in the ladle just before pouring the molds was approximately 2100 F, unless otherwise indicated. Temperatures were measured with a portable potentiometer. The chromel-alumel thermocouple wire was enclosed in a steel tube with a graphite immersion tip. The pouring time for the test casting is 10-13 sec.

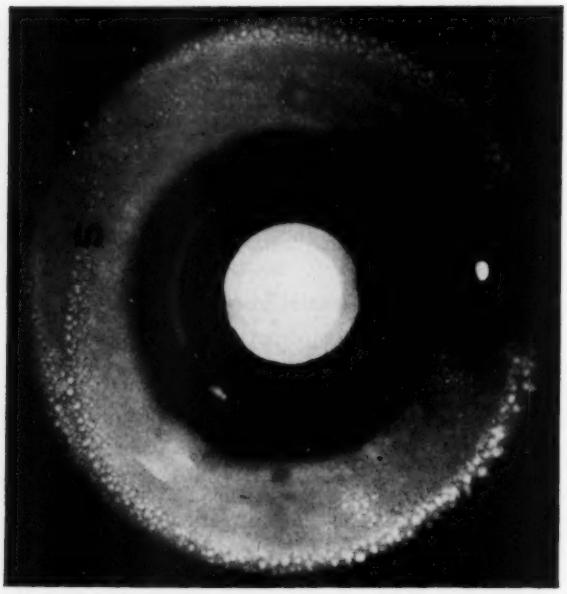


Fig. 1—Radiograph of 85-5-5-5 bronze pump casing showing gas porosity. Mold and core material—oil bonded and baked No. 60 silica sand. Mold and core coating—graphite (water-base) core wash. Casting weight 112 lb.

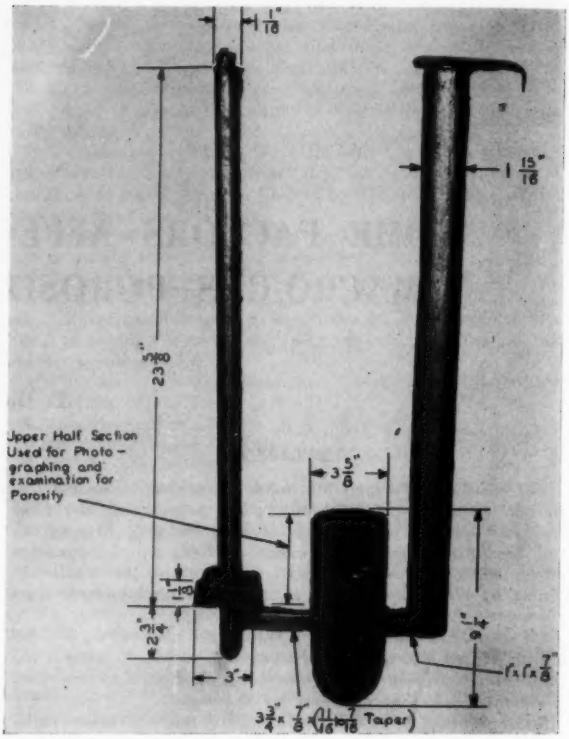


Fig. 2—Macroporosity test casting and principal dimensions.

Effect of Melting Practice

Gas porosity was observed in all castings when the 85-5-5-5 alloy was poured into the standard oil bonded and baked core sand molds regardless of the melting unit or melting practice used. The core sand mix for this group of experiments was as follows:

No. 60 N.J. washed silica sand
Oil-sand ratio, 1-80
Cereal 0.5 pct by weight
Cereal 0.25 pct by weight
Coating—graphite core wash (water base)

The molds were baked at approximately 450 F.

Figure 3 (casting No. 32) shows a macro-etched section of entire "bob" portion of the test casting, which is typical for castings poured from the following melting units:

1) Indirect-arc furnace. Over 25 heats were melted under standard conditions; all heats showed the macroporosity.

2) Coke-fired furnace, no atmosphere control.

3) Oil-fired furnace; this unit was operated under oxidizing and reducing atmospheres without any significant difference noted in macroporosity. The reduc-

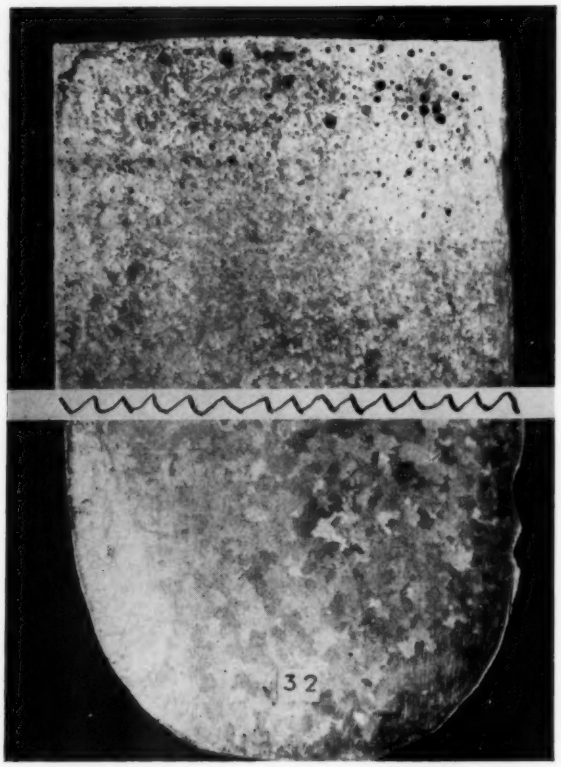


Fig. 3—Macro-etched section of the entire "bob" portion of the test casting showing spherical type macro-gas porosity typical of 85-5-5-5 bronze. Melted in 1) indirect-arc furnace; 2) coke-fired furnace; 3) oil-fired furnace (reducing and oxidizing atmosphere. Mold material—standard oil bonded silica sand. Mold coating—standard graphite wash. Deoxidation—0.02 pct phosphorus.

ing flame was judged by its yellow color and the fact that it blackened a piece of zinc passed through the flame. The oxidizing flame was judged by its green color and did not blacken the zinc.

Figure 4 (casting No. 116) shows the same type of

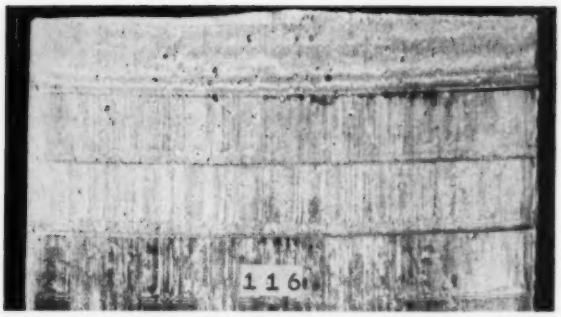


Fig. 4—Alloy 85-5-5-5; melting furnace, high-frequency induction; mold material, standard oil-bonded silica sand; mold coating, standard graphite wash; pouring temp., 2060 F; deoxidized, 0.02 pct phosphorus.

macro-gas porosity obtained with a high-frequency induction melting unit.

Three virgin metal heats were melted in the coke-fired furnace, and all three heats showed the gas condition.

Two coke-fired ingot heats, treated with 1 lb and 2 lb copper mill scale per 100 lb of metal during melting, followed by deoxidizing with 0.02 pct phosphorus in the ladle, did not show improvement.

One coke-fired ingot heat was melted with a flux cover consisting of 33 pct borax, 48 pct sand, 19 pct copper mill scale (1 lb flux/100 lb metal) followed by deoxidation with 0.02 pct phosphorus, and this did not show improvement.

There was some evidence that an increase in the phosphorus content aggravated the gas condition. However, it is common practice to add 1-2 oz of phosphor-copper per 100 lb of metal to increase fluidity and give cleaner dross-free castings.

It was concluded that the castings were being gassed from the mold material since none of the methods of melting employed produced 85-5-5-5 bronze castings free from the macro-gas porosity.

Various copper-base alloys were poured, using the standard test casting and standard core practice, for

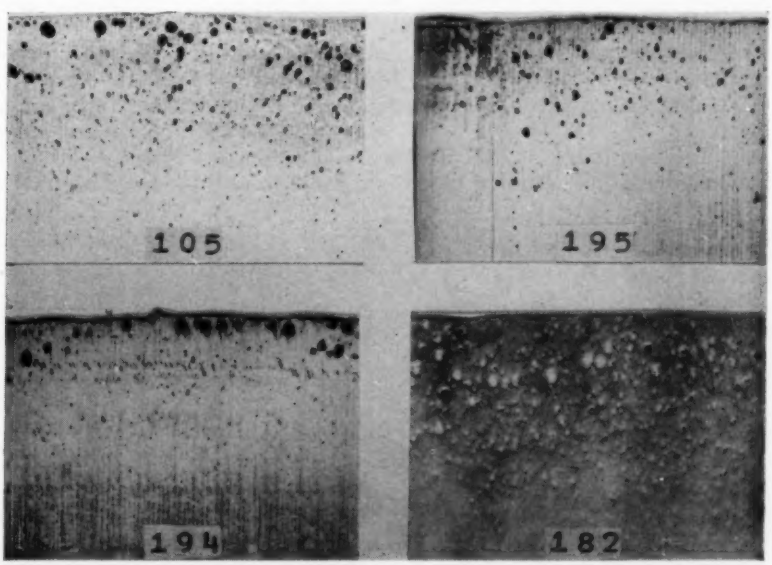


Fig. 5—Test No. 105—alloy 85-5-5-5; No. 195 — alloy 83-4-6-7; No. 194 — alloy 81-3-7-9; No. 182 — low zinc-nickel bearing bronze, 87.5 Cu, 5.5 Sn, 4.75 Pb, 0.50 Zn, 1.75 Ni. Melting furnace, indirect arc; mold material, standard oil-bonded silica sand; mold coating, standard graphite wash; pouring temperature, 2100 F; deoxidized, 0.02 pct phosphorus, 85-5-5-5 alloy; and 0.03 pct phosphorus, low zinc-nickel bronze.

comparison of gassing with the 85-5-5-5 alloy. The results indicate that alloy composition influences macroporosity.

Table 1 shows the nominal compositions of the alloys tested. Typical results of the gassing propensity of these alloys are shown in Figs. 5, 6, 7 and 8.

The acid metal bronze (casting No. 139) and 11 pct tin bronze (casting No. 193, Fig. 7) both practically zinc free, did not show the macro-gas porosity. If the acid metal bronze is poured into the core sand mold without deoxidizing with phosphorus (normal addition is 0.08 pct phosphorus) a considerable amount of macro-gas porosity occurs. This is shown later under discussion of degassifiers and deoxidizers.

The silicon brass (casting No. 115, Fig. 8), aluminum-manganese brass (casting No. 135, Fig. 8) and low aluminum, low iron manganese brass (casting No. 131, Fig. 8) were free from the macro-gas porosity. These alloys have a short solidification range and are characterized by high shrinkage. All the other alloys tested were gassed. Navy "M," the bearing bronze alloy, and the special alloys with 1 pct and 2 pct zinc were gassed to a lesser degree.

Effect of Binders and Mold Materials

Gas porosity was observed on all 85-5-5-5 alloy test castings regardless of the type of bonding oil used. Bonding oils were tested from four different companies. One company submitted five different brands. In addition to brand name oils, straight linseed and tung

TABLE 1—COMPOSITION OF ALLOYS POURED INTO THE STANDARD OIL BONDED AND BAKED NO. 60 SILICA SAND MOLD

Alloys	Designation	Composition, %									Melting Unit	Deoxidation Practice*	Observation of Test Casting for Gas	Fig. No.	Casting No.
		Cu	Sn	Pb	Zn	Ni	P	Si	Mn	Al					
Ounce Metal 85-5-5-5	ASTM B30-4A	85	5	5	5	—	—	—	—	—	Oil fired, indirect-arc, induction, Coke fired	2 oz. P-Cu shot/100 lb metal	Present	5	105
83-4-6-7	ASTM B30-4B	83	4	6	7	—	—	—	—	—	Indirect arc	2 oz. P-Cu shot/100 lb metal	Present	5	195
81-3-7-9	ASTM B30-5A	81	3	7	9	—	—	—	—	—	Indirect arc	2 oz. P-Cu shot/100 lb metal	Present	5	194
Navy "M" 88-6-1.5-4.5	ASTM B30-2A	88	6	1.5	4.5	—	—	—	—	—	Indirect arc	2 oz. P-Cu shot/100 lb metal	Present	6	190
Special—2% Zn	—	88	5	5	2	—	—	—	—	—	Indirect arc	2 oz. P-Cu shot/100 lb metal	Present	6	34
Special—1% Zn	—	89	5	5	1	—	—	—	—	—	Indirect arc	4-6 oz. P-Cu/ 100 lb	Present	6	42
Low Zinc Nickel Bronze	—	87.5	5.5	4.75	0.50	1.75	—	—	—	—	Indirect arc	3-8 oz. P-Cu/ 100 lb	Present	5	182
Acid Metal Bronze	—	89.5	8.5	2.0	—	—	—	—	—	—	Indirect arc	8 oz. P-Cu/ 100 lb	No gas visible	7	139
11% Tin Bronze	—	89	11	—	—	—	0.10 0.30	—	—	—	Oil Fired	1 lb. P-Cu/ 100 lb	No gas visible	7	193
Bearing Bronze	—	83.5	8	8	—	—	0.05 0.19	—	—	—	Oil Fired	8 oz. P-Cu/ 100 lb	Present	6	209
Silicon Brass		81.4	—	—	13.8			4.8	—	—	Induction	none	No gas visible High shrinkage	8	115
Al-Mn Brass		83			10			5	1	1	Induction	none	No gas visible High shrinkage	8	135
Low Al— Low Fe Mn Brass											Induction	none	No gas visible High shrinkage	8	131

* 15 pct grade phosphor-copper shot—ladle addition.

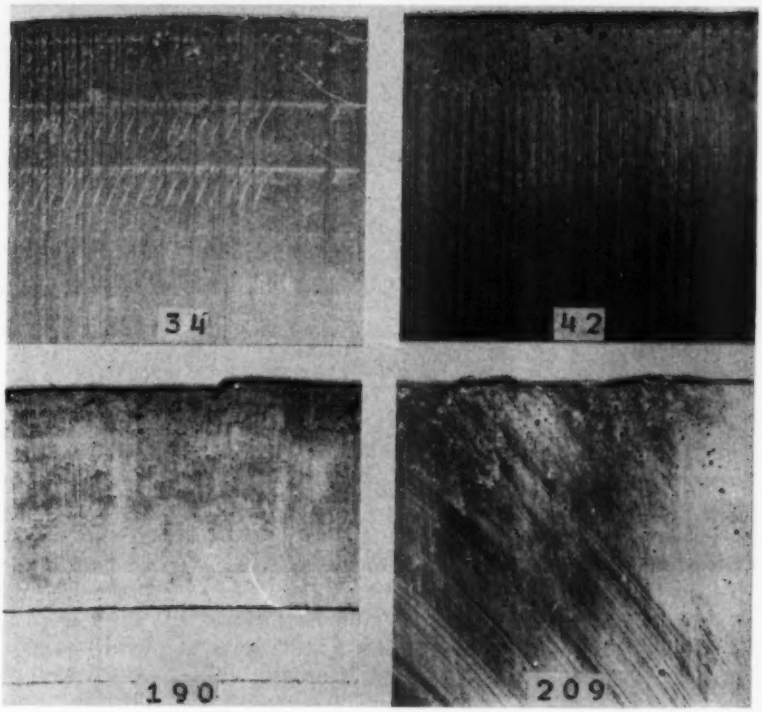


Fig. 6—Test No. 34—alloy 88-5-5-2; melted in indirect-arc furnace; poured at 2100 F; deoxidized, 0.02 pct P. Test No. 42—alloy 89-5-5-1; melted, indirect-arc furnace; poured 2100 F; deoxidized 0.04 pct P. Test No. 190—Navy "M" 88-6-1.5-4.5; melted, indirect-arc furnace; poured, 2100 F; deoxidized, 0.02 pct P. Test No. 209—Bearing bronze, 84-8-8 (0.05-0.19 P); melted, oil-fired furnace; poured, 2050 F; deoxidized, 0.08 pct P. All test bars poured in oil-bonded silica sand molds, standard graphite wash.

oil were also tested. These offered no improvement, and results are shown in Fig. 9.

Several tests were made leaving the cereal binders out of the regular mixture in order to show that the oil is the major contributing factor to the gassing of the 85-5-5-5 alloy. Typical results are shown in Fig. 10 (castings Nos. 68 and 69). It is known that CO_2, CO, H_2, CH_4 (methane), and C_2H_4 (ethylene) are given off when linseed oil or commercial oils are burned in air. It would be difficult to determine the nature or percentage of gases evolved from the mold into which the metal was poured. But hydrogen is probably available and has been found by many investigators to be a major cause of unsoundness in copper-base alloys. It is logical, therefore, that the macroporosity observed could be the result of hydrogen absorption in the metal or a reaction with the metal.

It was early noted that when 85-5-5-5 alloy was poured in a pitch-bonded mold that the macro-gas porosity was not visible. Figure 11 shows the results of pouring ounce metal into pitch-bonded, pitch and oil bonded, and regular oil-silica sand molds. It is believed that the reducing atmosphere resulting from the decomposition of the pitch was the major factor in the differences observed in these castings. The surface of the castings produced in the pitch-bonded molds, however, was considerably poorer. More shrinkage was observed in the 2 in. round stick portion of the test casting in the pitch-bonded mold, which was considered an indication that less gas was contained in the metal on solidification.

Cement bonded and natural green sand (Albany No. 125) molds showed very little macro-gas porosity. The gassing tendency of ounce metal poured in a natural sand mold is compared with oil bonded silica sand molds in Fig. 12. Shrinkage tears were observed in the castings poured in natural sand. Casting No. 47, poured in the oil-sand mold at approximately 2100 F, shows more gas than casting No. 48 poured at 2000 F. All four castings were poured from the same ladle of metal.

Ounce metal poured into a 10 pct cement bonded No. 60 silica sand mold is compared with oil-bonded

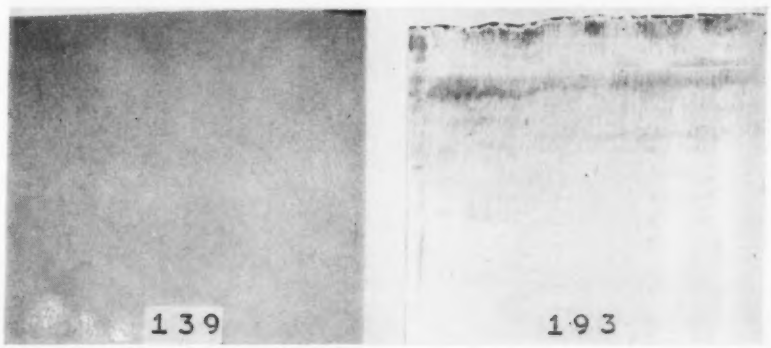


Fig. 7—Test No. 139—acid metal bronze (89.5 Cu, 8.5 Sn, 2.0 Pb); melted, indirect-arc furnace; poured, 2100 F; deoxidized, 0.07 pct P. Test No. 193—Tin bronze (89 Cu, 11 Sn, 0.10-0.30 P; melted, oil-fired furnace; poured, 2130 F; deoxidized, 0.16 pct P. Both test bars poured in oil-bonded silica sand molds, standard graphite wash.

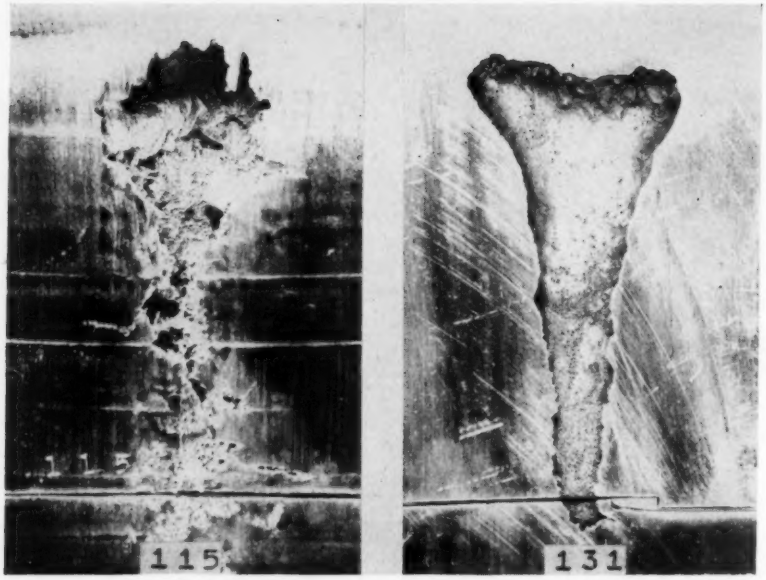




Fig. 8—Test bars melted in induction furnace; poured in oil-bonded silica sand molds; standard graphite wash; not deoxidized.

Test Bar No. 115—Silicon brass (81.4 Cu, 13.8 Zn, 4.8 Si). Pouring temp., 2000 F.

Test Bar No. 131—Low Al-low Fe-Mn brass. Pouring temp., 1730 F.

Test Bar No. 135—Al-Mn brass (83 Cu, 10 Zn, 5 Si, 1 Mn, 1 Al). Pouring temp., 1680 F.

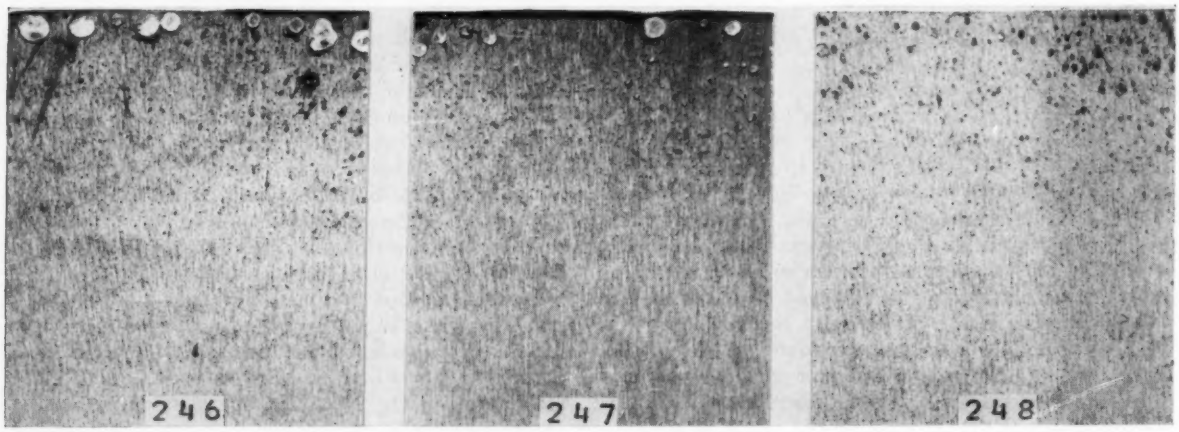


Fig. 9—Alloy 85-5-5-5 test bars melted in indirect-arc furnace; poured in molds coated with standard graphite wash; pouring temp., 2100 F; deoxidized, 0.02 pct P.

Test Bar No. 246—Poured in oil bonded silica sand mold.

Test Bar No. 247—Poured in linseed oil-bonded silica sand mold (1-80 ratio).

Test Bar No. 248—Poured in tung oil-bonded silica sand mold (1-80 ratio).

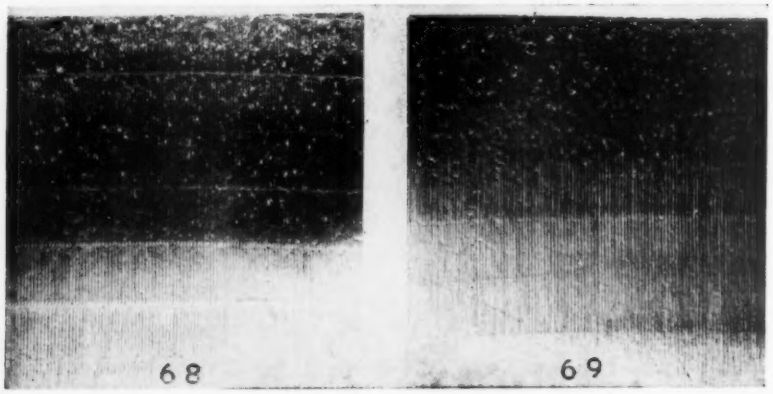


Fig. 10—Alloy 85-5-5-5 test bars melted in indirect-arc furnace; pouring temp. approx. 2100 F; deoxidized, 0.02 pct P.

Test Bar No. 68—Molded in oil and sand only (1-80 ratio).

Test Bar No. 69—Molded in standard oil sand mix (0.5 pct and 0.25 pct of two cereal binders (by weight).

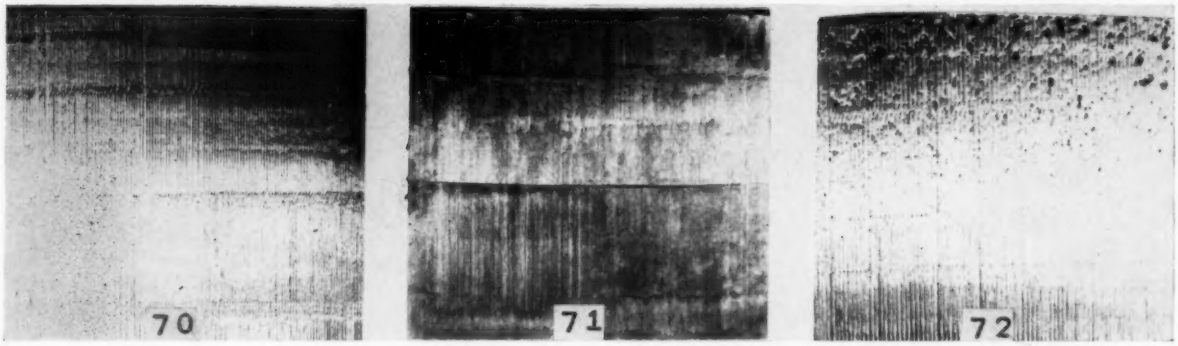


Fig. 11—Alloy 85-5-5-5 test bars melted in indirect-arc furnace; mold coating, standard graphite wash; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P.
Test Bar No. 70—Molded in pitch-bonded silica sand (4.5 pct pitch by weight).
Test Bar No. 71—Molded in pitch- and oil-bonded silica sand (1-150 oil ratio, 2 pct pitch by weight).
Test Bar No. 72—Molded in standard oil-bonded silica sand.

sand in Fig. 13. The casting poured in the oil-bonded mold shows severe gas porosity. The cement bonded mold with an oil-bonded core sand pouring basin (casting No. 164) also showed gas, whereas, the casting poured with the mold and pouring basin made completely of cement bonded sand (casting No. 166) did not show gas. The contact of the molten metal with the oil sand used for the pouring basin was sufficient to gas the metal.

The liquid and powdered phenolic resins (1-2 pct added to the sand mix) did not offer any improvement over the commercial oils as binders in preventing the spherical type macro-gas porosity. Phenolic resins from four different suppliers were tested. The addition of pitch compound, 5 pct by weight, to the phenolic resin mixture eliminated the macro-type gas. Lesser quantities of pitch in the order of 2-3 pct by weight gave variable results depending on the brand of phenolic resin used. Figure 14 shows a typical illustration.

The urea formaldehyde type resins gave less macro-gas than the phenol type, and less pitch was required to eliminate the gas porosity. Figure 15 shows the results. Casting No. 213 with 3 pct pitch (by weight) in the sand mix shows no visible gas but considerable shrinkage.

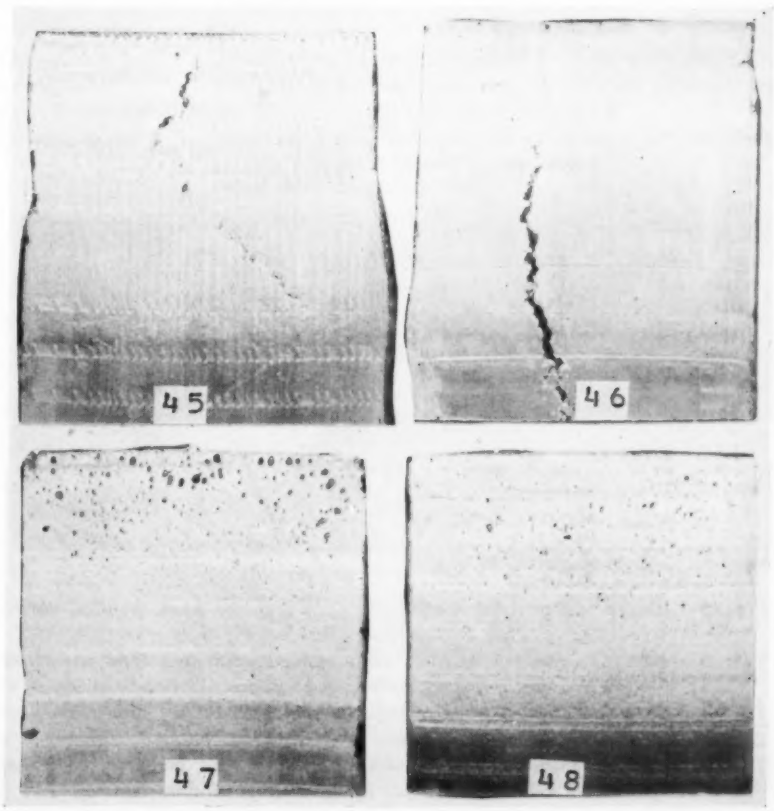


Fig. 12—Alloy 85-5-5-5 test bars melted in indirect-arc furnace; deoxidized, 0.02 pct P.
Test Bar No. 45—Molded in natural green sand (Albany No. 125); pouring temp., 2125 F.
Test Bar No. 46—Molded in natural green sand (Albany No. 125); pouring temp., 2000 F.
Test Bar No. 47—Molded in standard oil-bonded silica sand; pouring temp., 2090 F.
Test Bar No. 48—Molded in standard oil-bonded silica sand; pouring temp., 2000 F.

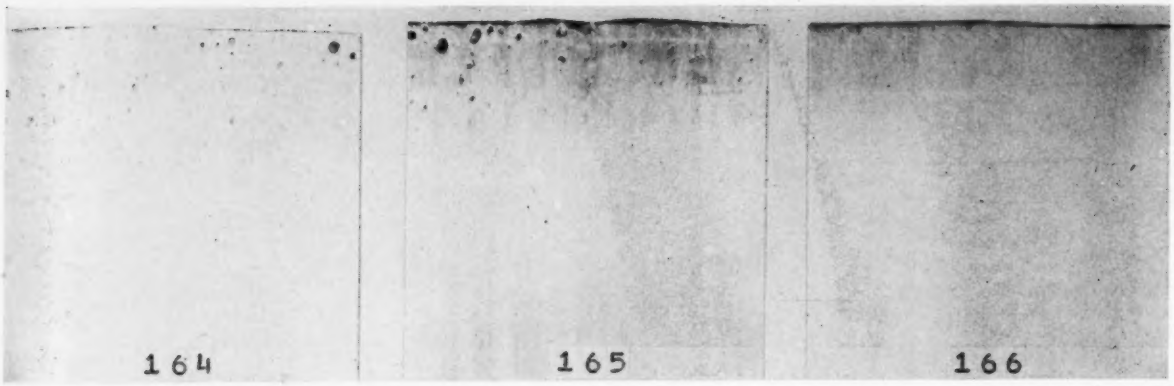


Fig. 13—Alloy 85-5-5-5 melted in indirect-arc furnace; pouring temp., 2100 F; deoxidized, 0.02 pct P.
Test Bar No. 164—Molded in 10 pct cement bonded silica sand; standard oil sand pouring basin.*
Test Bar No. 165—Molded in standard oil-bonded silica sand (mold and pouring basin).
Test Bar No. 166—Molded in 10 pct cement bonded silica sand (mold and pouring basin).*
**Cement molds air dried 6 days; oven dried 12 hr at 250 F.*

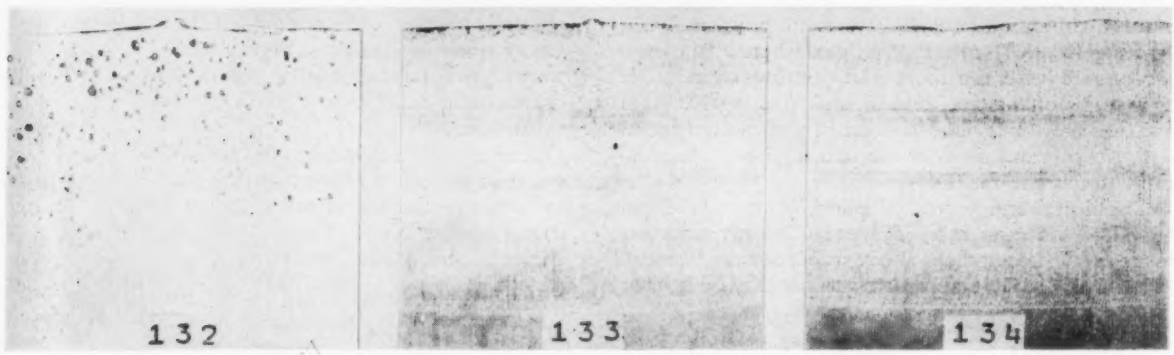


Fig. 14—Alloy 85-5-5-5 melted in indirect-arc furnace; mold coating, standard graphite wash; pouring temp., 2100 F; deoxdized, 0.02 pct P.
Test Bar No. 132—Silica sand bonded with 2 pct powdered phenolic resin.
Test Bar No. 133—Silica sand bonded with 2 pct powdered phenolic resin plus 3 pct pitch compound.
Test Bar No. 134—Silica sand bonded with 2 pct powdered phenolic resin plus 5 pct pitch compound.

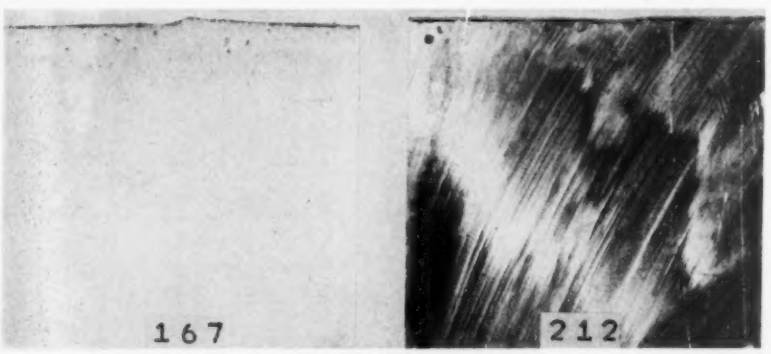




Fig. 15—Alloy 85-5-5-5; mold coating, standard graphite wash; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P.
Test Bar No. 167—Molded in silica sand bonded with urea type powdered resin; melted, indirect-arc furnace.
Test Bar No. 212—Molded in silica sand bonded with 1.5 pct urea resin plus 0.5 pct cereal binder plus 1 pct pitch; melted, oil-fired furnace.
Test Bar No. 213—Molded in silica sand bonded with 1.5 pct urea resin plus 0.5 pct cereal binder plus 3 pct pitch; melted, oil-fired furnace.



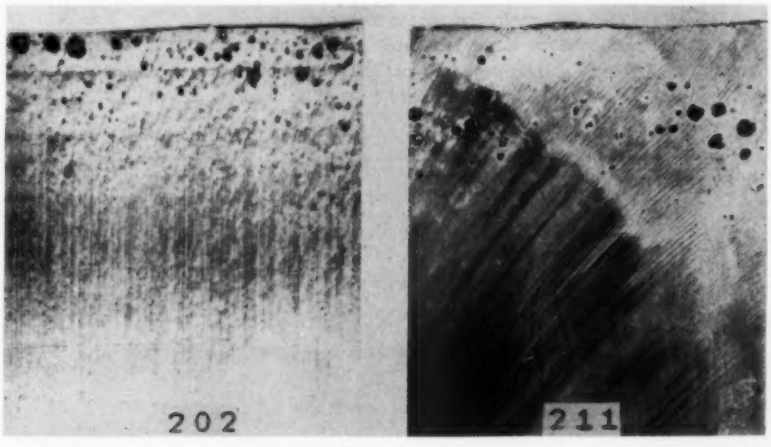


Fig. 16 — Alloy 85-5-5-5; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P.

Test Bar No. 63—Mold, silica sand bonded with 3 pct (by weight) natural pine wood resin; melt, indirect-arc furnace.

Test Bar No. 75—Mold, silica sand bonded with 2½ pct organic, vegetable, and hydrocarbon powdered binder; melt, coke-fired furnace.

Test Bar No. 170—Mold, silica sand bonded with 1 pct (by weight) liquid hemi-celullose extract. Melt, indirect-arc furnace.

Test Bar No. 202—Mold, silica sand bonded with powdered dextrinized clay binder (8.5 pct by vol.); melt, indirect-arc furnace.

Test Bar No. 211—Mold, silica sand bonded with 3 pct mineral and organic type binder. Melt, oil-fired furnace.

Several other types of commercial binders were tested. The natural pine wood thermoplastic resin was more free from the macro-gas porosity than the others tested. The results are shown in Fig. 16.

Effect of Mold Coating

The atmosphere produced in a pitch compound bonded mold was beneficial in eliminating the macro-gas porosity. Based on this observation, pitch compound was added to the regular graphite (water base) mold coating in varying percentages to observe if similar benefits from pitch could be incorporated into a core or mold coating. The specially prepared coatings were applied over the regular oil bonded silica sand mold, usually while warm. The coating was torch dried if applied on a mold which had been allowed to approach room temperature. Care was taken not to overheat the coating as the pitch will burn.

Figure 17 shows the beneficial effect of 25 and 50 pct pitch additions (by weight) to the standard graphite wash. The coating (approx. 35° Baume) was applied to all surfaces in contact with the metal. The results show that a pitch addition to the coating will eliminate the macro-gas porosity in the 85-5-5-5 alloy. Coatings containing pitch give a poorer casting surface and this corroborates a similar observation made on pitch-bonded molds. Further tests made with lesser quantities of pitch in the coating are illustrated by Fig. 18. It would appear that quantities less than 15 pct by weight would not be effective in eliminating the gas porosity. However, this could be a variable depending on the Baume or thickness of the coating applied. Figure 19 shows the importance of coating the pouring basin and downgate.

A special investigation was conducted in 1944 on metal flow through gates. It was observed that a considerable amount of turbulence occurs in the pouring basin and downgates. Difficulty was encountered in keeping long downgates filled with metal. It was noted that the metal stream actually pulled apart near the bottom of the mold. These observations were made by taking high-speed movies while pouring Woods' metal (a low melting point-high specific gravity alloy) at a temperature of approximately 205-210 F into a gypsum cement mold faced with lucite. This investigation clearly demonstrated that pouring basins and downgates could be very vulnerable spots for gassing metal because of the intimate mixing of gas and metal.

Sea coal (25 and 50 pct by weight) was also added to the regular wash for comparison with pitch. It can be seen that sea coal is not as effective as pitch (Fig. 20). Also, refer to castings Nos. 95 and 96 of Fig. 17 for comparison.

The gas porosity was eliminated by applying a coal tar-pitch base paint (bitumastic solution) on the regular mold. The surface of the casting was rough.

A rubber cement base type of spray coating was not successful in overcoming the gas porosity. Other inert

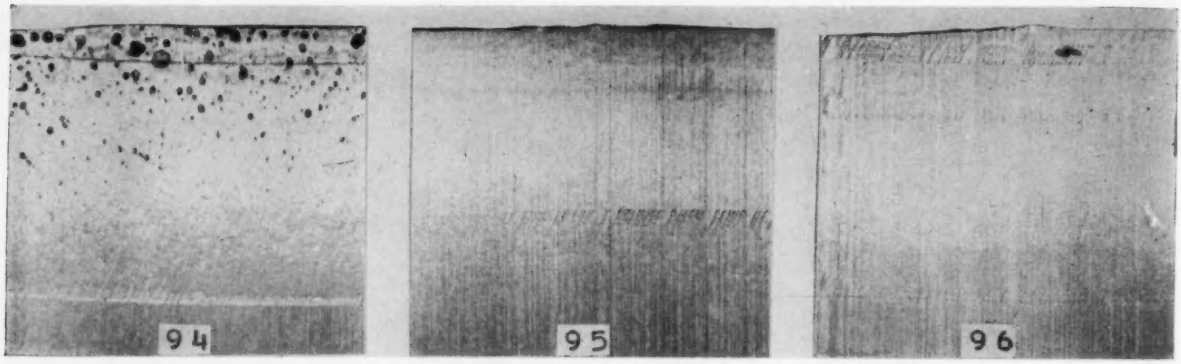
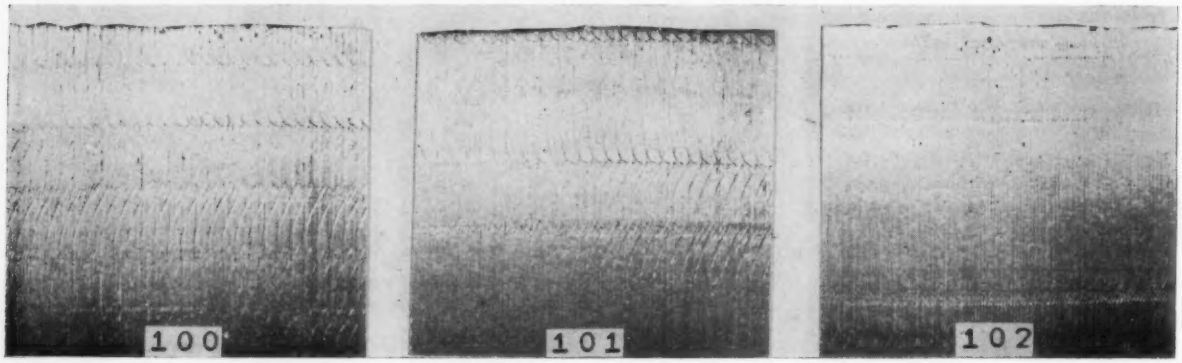


Fig. 17—Alloy 85-5-5-5; melt, indirect-arc furnace; mold, standard oil-bonded silica sand; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P.
Test Bar No. 94—Mold coating, standard graphite wash.
Test Bar No. 95—Mold coating, 50 pct graphite wash, 50 pct pitch by weight.
Test Bar No. 96—Mold coating, 75 pct graphite wash, 25 pct pitch by weight.



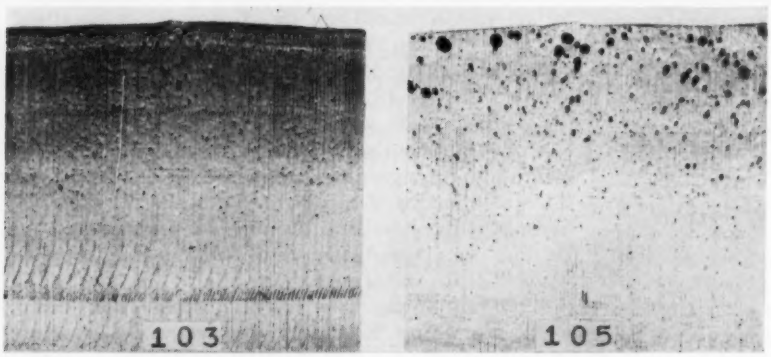


Fig. 18—Alloy 85-5-5-5; melt, indirect-arc furnace; mold, standard oil-bonded silica sand; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P.
Test Bar No. 100—Mold coating, 80 pct graphite wash, 20 pct pitch by weight.
Test Bar No. 101—Mold coating, 85 pct graphite wash, 15 pct pitch by weight.
Test Bar No. 102—Mold coating, 90 pct graphite wash, 10 pct pitch by weight.
Test Bar No. 103—Mold coating, 95 pct graphite wash, 15 pct pitch by weight.
Test Bar No. 105—Mold coating, standard graphite wash.

types of coating did not offer any improvement over the graphite coating.

A specially prepared coating containing the following ingredients reduced the gas in the 85-5-5-5 alloy and eliminated the gas in Navy "M" and the bearing bronze alloy (84-8-8-0) on the test casting using the regular oil bonded silica sand mix (Figs. 21 and 22).

Core and mold coating mixture (Baume of 55-60):
42 qt proprietary material (said to be thermoplastic resin carried in a volatile solvent)
28 qt zircon flour
4 qt zircon wash
7 qt water

This resin-bearing vehicle produces an atmosphere which helps inhibit the macro-gas porosity.

This coating is being used on certain regular production castings of Navy "M" metal and the bearing bronze (copper 84.0, tin 8.0, lead 8.0 and phosphorus 0.05/0.19) alloy which are also susceptible to gassing in oil bonded silica sand molds. This coating did not work too satisfactorily on the 85-5-5-5 production castings because ounce metal is more susceptible to macro-type gas.

Figure 23 shows gas porosity in a bearing bronze regulator body production casting poured in Albany sand with oil bonded zircon sand cores. The gas porosity in this casting was overcome by using the special zircon coating over oil bonded zircon sand cores and

a small quantity of pitch (2-3 pct in a synthetic sand mix (carbon analysis of the sand averages 1.80 pct carbon). The combination of pitch in the molding sand and special coating on the cores produced a mold atmosphere that prevented the macro-gas porosity. The external surface of the castings is not as good but is satisfactory.

In general, it can be concluded that a rougher casting surface has resulted from the use of mold coatings or mold materials that have eliminated the gas porosity. This may be caused by soot deposits due to type of reducing atmosphere produced. A turbulent metal condition appears to intensify the roughness which is especially prevalent at or near the gate entrance to the casting.

Effect of Degassifiers and Deoxidizers

The preliminary experiments on ounce metal indi-

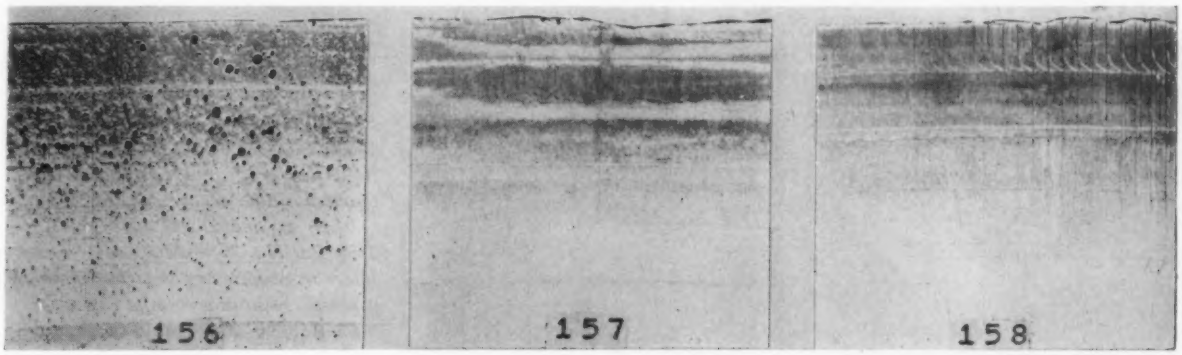


Fig. 19—Alloy 85-5-5-5; melt, indirect-arc furnace; mold, standard oil-bonded silica sand; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P.
Test Bar No. 156—Mold coating, standard graphite wash.
Test Bar No. 157—Mold coat, 75 pct graphite wash, 25 pct pitch by weight (entire mold coated).
Test Bar No. 158—Mold coat, 75 pct graphite wash, 25 pct pitch by weight (downgate and pouring basin only).



Fig. 20—Alloy 85-5-5-5; melt, indirect-arc furnace; mold, standard oil-bonded silica sand; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P.
Test Bar No. 94—Mold coat, standard graphite wash.
Test Bar No. 97—Mold coat, 50 pct graphite wash, 50 pct sea coal by weight.
Test Bar No. 98—Mold coat, 75 pct graphite wash, 25 pct sea coal by weight.

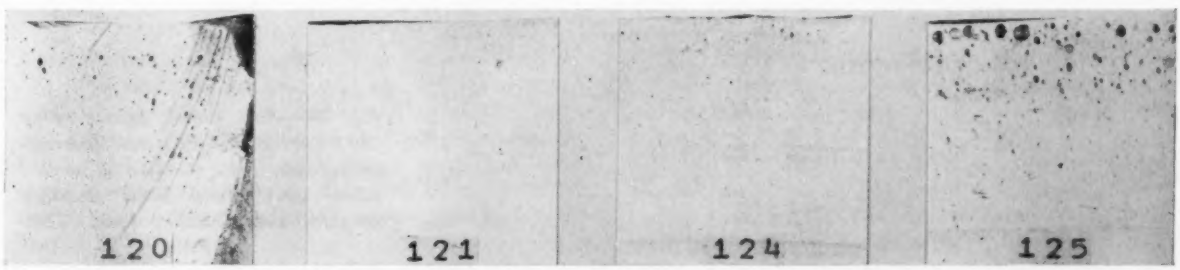


Fig. 21—Test Bars No. 120 and 121—Navy "M" (86.5-6.0-1.5-4.5) alloy; melt, coke-fired furnace. Test Bars No. 124 and 125—alloy 85-5-5-5; melt, indirect-arc furnace. All four test bars; mold, standard oil-bonded silica sand; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P.
Test Bar No. 120—Mold coat, standard graphite wash.
Test Bar No. 121—Mold coat, zircon fillers in proprietary vehicle (thermoplastic resin in a volatile solvent).
Test Bar No. 124—Mold coat, zircon fillers in proprietary vehicle (thermoplastic resin in a volatile solvent).
Test Bar No. 125—Mold coat, standard graphite wash.

cated that gassing appeared to be slightly aggravated by an increase in phosphorus.

However, the acid metal composition (copper 89.5, tin 8.5, lead 2.0) when poured into the standard oil-bonded mold showed a considerable amount of gas without phosphorus, and was free of macro-gas porosity after an addition of 0.07 pct phosphorus was made (Fig. 24). The 2 in. round stick portion of the test casting poured before the phosphorus addition showed mushrooming. A good shrink was noted in the casting treated with 7 oz of phosphor-copper shot per 100 lb of metal.

Ladle additions of lithium eliminated the macro-gas porosity in the 85-5-5-5 alloy and the nickel-bronze alloy (copper 87.5, tin 5.5, lead 4.75, zinc 0.50, nickel 1.75). These were the only alloys to which lithium was added.

The lithium was added in the form of lithium-zinc master alloy (containing approximately 2 pct lithium-98 pct zinc) and lithium cartridges (metallic lithium

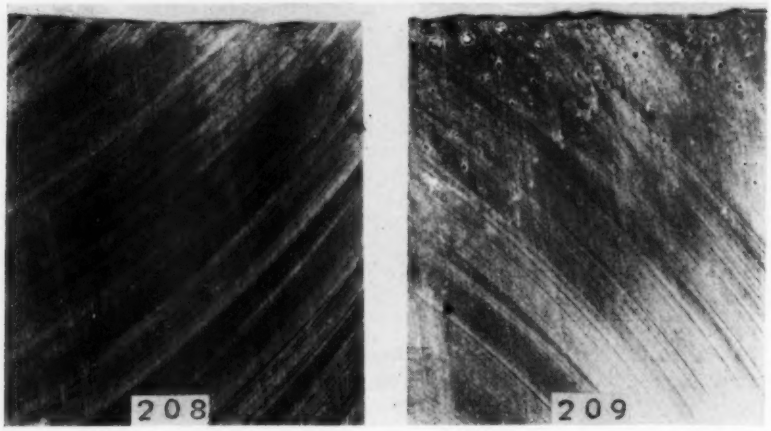


Fig. 22—Bearing bronze alloy (84 Cu, 8 Sn, 8 Pb, 0.05-0.19 P); melt, oil-fired furnace; mold, standard oil-bonded silica sand; pouring temp., 2050 F; deoxidized, 0.08 pct P.
Test Bar No. 208—Mold coat, zircon fillers in proprietary vehicle (thermoplastic resin in a volatile solvent).
Test Bar No. 209—Mold coat, standard graphite wash.

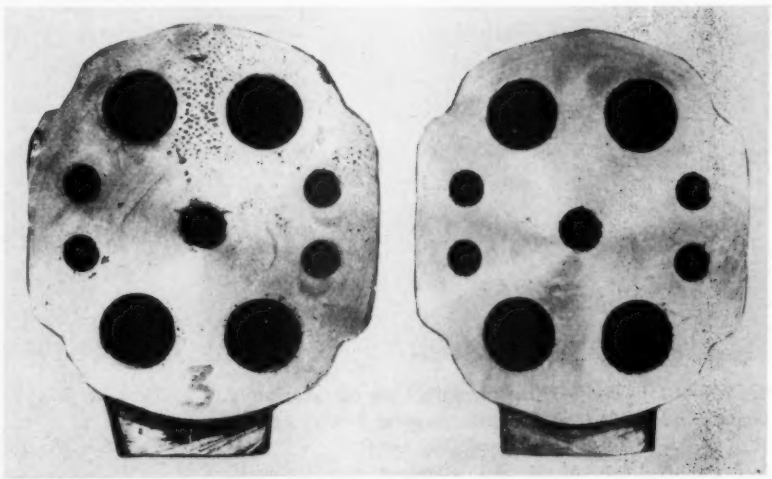


Fig. 23—Regulator body castings; bearing bronze alloy (84 Cu, 8 Sn, 8 Pb); core mix, oil-bonded zircon sand; pouring temp., 2025 F; deoxidized, 0.08 P.
Left—Mold, No. 125 Albany sand; core coat, standard graphite wash.
Right—Mold, synthetic green sand (2-3 pct pitch by weight); core coat, zircon fillers in proprietary vehicle (thermoplastic resin in volatile solvent).

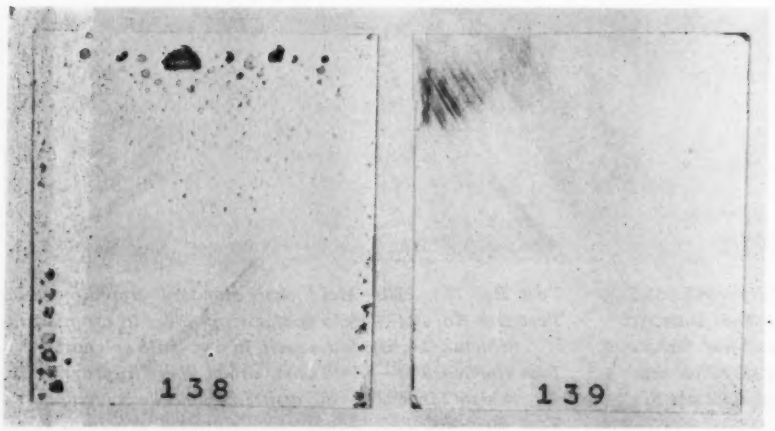


Fig. 24—Acid metal bronze alloy (89.5 Cu, 8.5 Sn, 2 Pb); melt, indirect-arc furnace; mold, standard oil-bonded silica sand; mold coat, standard graphite wash; pouring temp., 2100 F.
Test Bar No. 138—Deoxidizer, none.
Test Bar No. 139—Deoxidizer, 0.07 pct P.

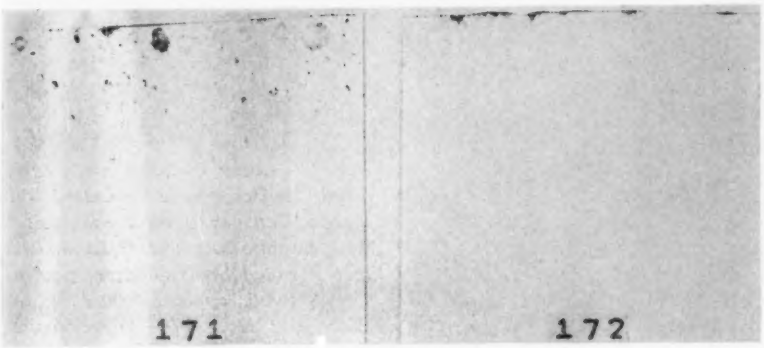


Fig. 25—Alloy 85-5-5-5; melt, indirect-arc furnace; mold, standard oil-bonded silica sand; mold coat, standard graphite wash; pouring temp., approx. 2100 F; deoxidized, 0.02 pct P added before lithium.
Test Bar No. 171—Degassification, none.
Test Bar No. 172—Degassification, 1.5 lb Li-Zn per 100 lb metal plunged into metal and poured (Li added in form of master alloy—98 Zn, 2 Li).

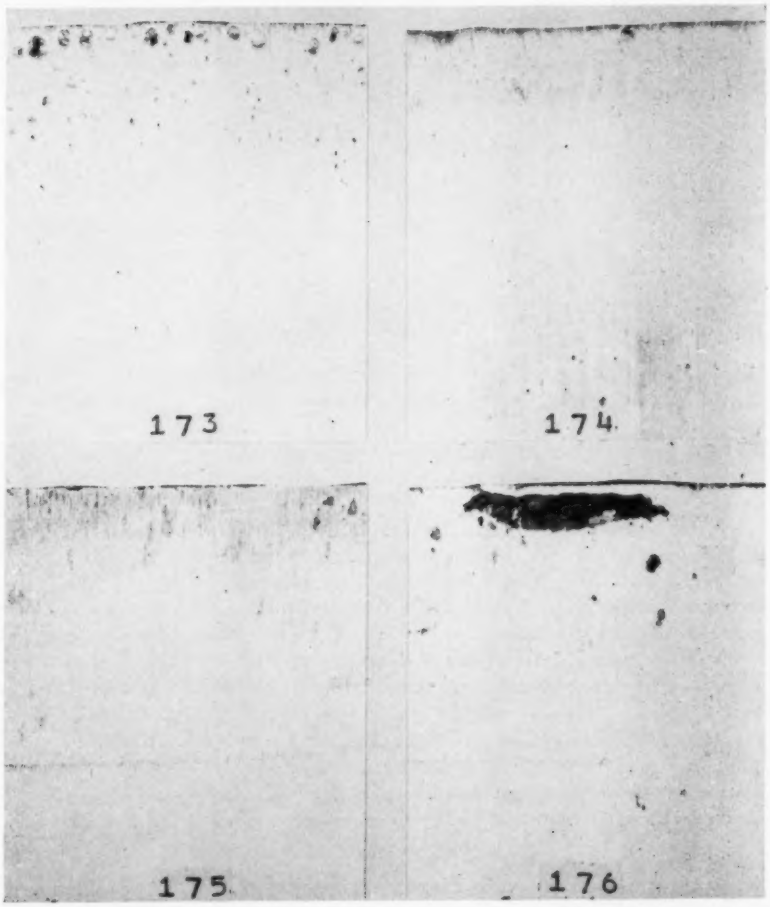


Fig. 26—Alloy 85-5-5-5; melt, indirect-arc; mold, standard oil-bonded silica sand; mold coat, standard graphite wash; deoxidized, 0.02 pct P added before lithium.
Test Bar No. 173—Pouring temp., 2125 F; degassification, none.
Test Bar No. 174—Degassification, 1.5 lb Li-Zn per 100 lb metal plunged into metal and poured.
Test Bar No. 175—Pouring temp., 2020 F; degassification, 3 min holding time in ladle after Li added.
Test Bar No. 176—Pouring temp., 1960 F; degassification, 6 min holding time in ladle after Li added.

in hermetically sealed copper tubing). The lithium cartridges are available in 9.0 grams, 4.5 grams, 2.25 grams of metallic lithium corresponding to 0.02, 0.01 and 0.005 pct lithium additions per 100 lb of metal. Lithium is classed as a deoxidizer and degassifier since it reacts with hydrogen and other gases as well as with oxides, sulphides, etc., while phosphorus is classed only as a deoxidizer.

The tests made with the lithium-zinc master alloy on the 85-5-5-5 alloy are shown in Fig. 25.

Casting No. 172 treated with approximately 0.03 pct lithium showed no evidence of gas. However, the cut surface showed numerous discolored brown spots (not visible on photo) which were believed to be due to a reaction between the residual lithium or lithium compound in the casting with the coolant used in cutting the sample. An alkaline hydroxide was probably formed which caused localized corrosive attack.

Figure 26 shows the results of an experiment to determine the effect of holding time after the lithium addition on the gas porosity. Casting No. 173 was poured after deoxidizing with 0.02 pct phosphorus (pouring temperature 2125 F). The lithium-zinc alloy (1½ lb per 100 lb of metal—0.03 pct lithium) was plunged into the remaining metal and the test casting poured at 2080 F. There was no evidence of gas (casting No. 174). After holding the metal for 3 min, casting No. 175 was poured (pouring temperature 2020 F, which showed a slight amount of gas. After hold-

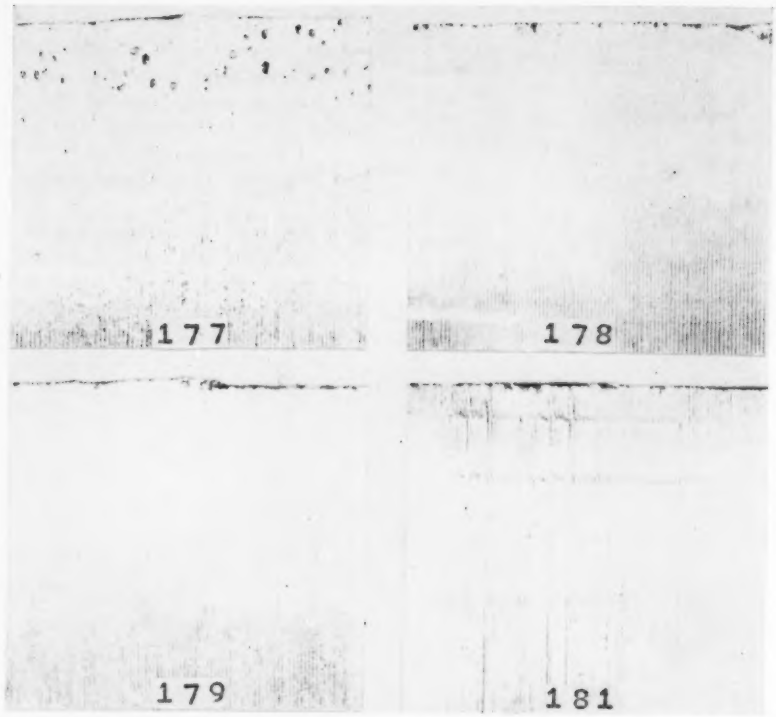


Fig. 27—Alloy 85-5-5-5; melt, coke-fired furnace; mold, standard oil-bonded silica sand; mold coat, standard graphite wash; deoxidized, 0.02 pct P added before lithium; pouring temp., approx. 2100 F.
Test Bar No. 177—Degassification, none.
Test Bar No. 178—Degassification, 0.01 pct Li (4.5 gr. Li cartridge per 100 lb metal).
Test No. 179—Degassification, 0.02 pct Li.
Test Bar No. 181—Degassification, 0.03 pct Li.

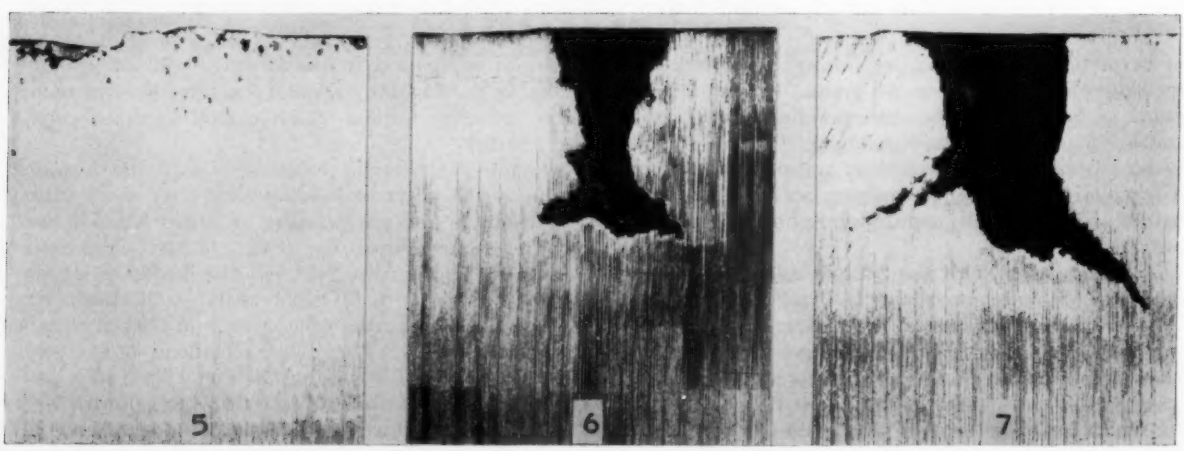
ing the metal for 6 min, casting No. 176 was poured at approximately 1960 F. The metal barely flowed to the top of the 2 in. round stick portion of the casting, and some of the defects noted may be due to sluggishness of the metal. A tarnished surface was again observed on the lithium-treated castings.

Figure 27 shows other results obtained with various percentages of lithium added to the ladle after deoxidizing with 0.02 phosphorus. Lithium cartridges were used, wired to a piece of angle iron and plunged to the bottom of the ladle.

Effective degassification was obtained even with a 0.005 pct lithium addition, and less discoloration was noted with the smaller quantity. Shrinkage has been noted in the 2-in. round stick portion of the casting in all test castings where the macro-gas porosity has been eliminated.

To obtain a relative picture as to the effect of lithium on shrinkage a more sensitive test casting was used. This casting, also made in core, is 4-in. in dia. x 10 in. high, and is gated directly into the top with a ¾ in. dia. gate (gate height approximately 6 in.). The results are shown in Fig. 28. No attempt was made to analyze for residual lithium. Lithium has been used as a means of degassification on production castings, but on intricate castings showing abrupt changes in

Fig. 28—Effect of lithium on shrinkage; test bar 4-in. diam. by 10-in. height, top gated (¾-in. diam. gate). Alloy 85-5-5-5; melt, indirect-arc furnace; mold, oil-bonded zircon sand; mold coat, standard graphite wash; pouring temp., 2100 F; deoxidized, 0.02 pct P added before lithium.
Test Bar No. 5—Degassification, none.
Test Bar No. 6—Degassification, 0.003 pct Li.
Test Bar No. 7—Degassification, 0.007 pct Li.



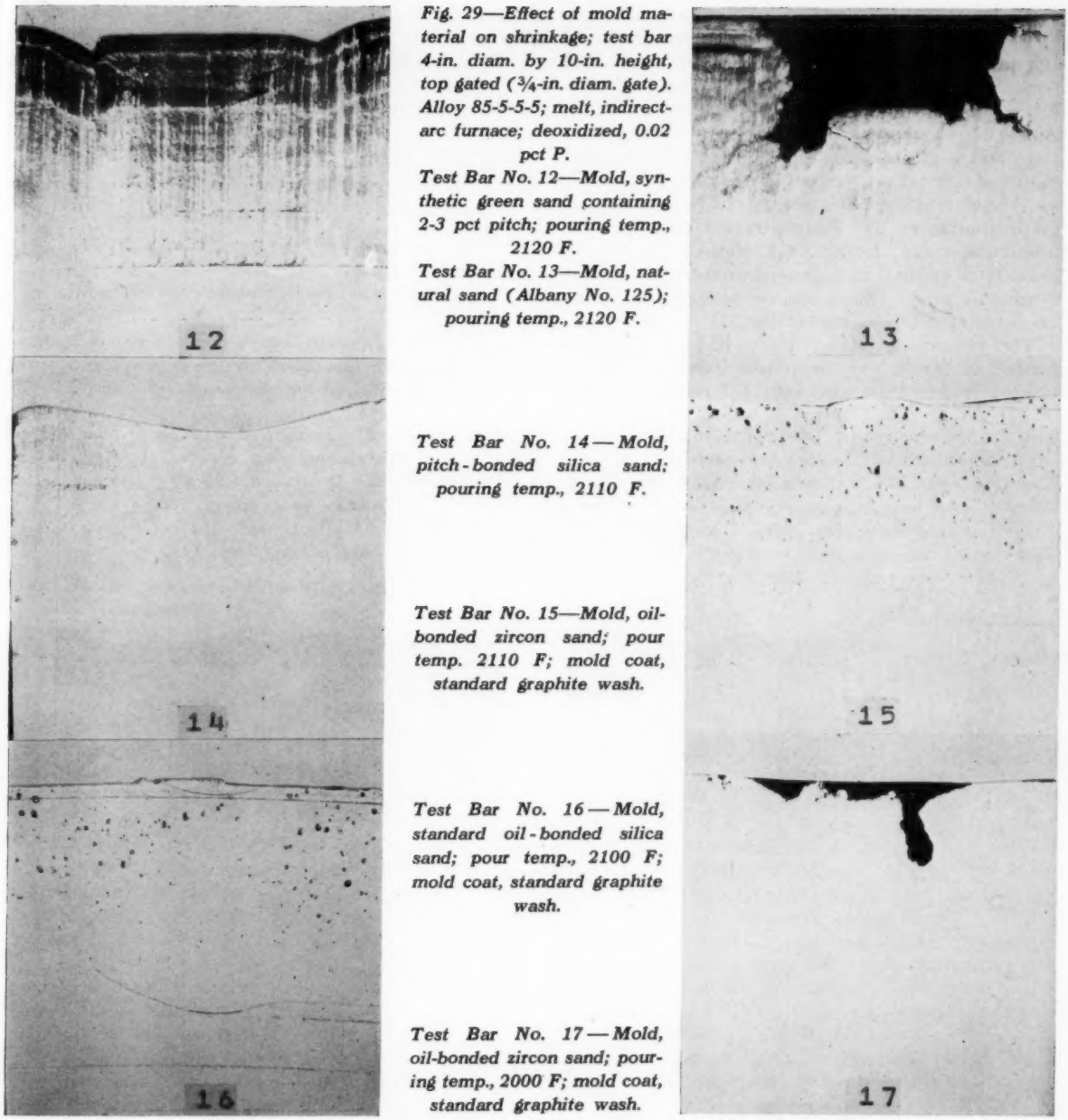


Fig. 29—Effect of mold material on shrinkage; test bar 4-in. diam. by 10-in. height, top gated (¾-in. diam. gate). Alloy 85-5-5-5; melt, indirect-arc furnace; deoxidized, 0.02 pct P.

Test Bar No. 12—Mold, synthetic green sand containing 2-3 pct pitch; pouring temp., 2120 F.

Test Bar No. 13—Mold, natural sand (Albany No. 125); pouring temp., 2120 F.

Test Bar No. 14—Mold, pitch-bonded silica sand; pouring temp., 2110 F.

Test Bar No. 15—Mold, oil-bonded zircon sand; pour temp. 2110 F; mold coat, standard graphite wash.

Test Bar No. 16—Mold, standard oil-bonded silica sand; pour temp., 2100 F; mold coat, standard graphite wash.

Test Bar No. 17—Mold, oil-bonded zircon sand; pouring temp., 2000 F; mold coat, standard graphite wash.

section shrinkage tears similar to that shown on casting No. 7 were observed. It is quite obvious that lithium-treated metal is characterized by high shrinkage, and it is necessary to take this into account in the gating and risering of castings so that proper feeding is accomplished.

A number of these 4 in. round x 10 in. shrinkage test specimens have been made on several bronze alloys poured into various mold materials. Several photographs are shown to illustrate the results obtained on ounce metal mainly to emphasize how easily copper-base alloys can be influenced by minor changes in mold materials and additions. Figure 29 shows the relative shrinkage and gas occurrence of the 85-5-5-5 alloy cast in various sand mixtures. No macro-gas porosity was observed in castings poured in the synthetic, natural sand and pitch-bonded molds, which agrees with our standard gas porosity test cited previously. The oil-bonded zircon and No. 60 silica sand mold poured at approximately 2100 F shows the macro-gas porosity. The zircon mold poured at 2000 F does not show macro-gas porosity, but does show some shrinkage. Considerable segregation was noted in the green sand samples. Comparative analyses of the segregated button on casting No. 13 and the main body of the castings showed the following:

	Cu	Sn	Pb	Zn	P	Ni	Fe	Sb
Button	71.08	12.89	13.53	1.48	0.01	0.72	0.23	0.08
Body of Casting	86.84	4.31	3.86	4.35	0.01	0.45	0.17	0.07

The ingot metal for these castings was melted in the indirect-arc furnace; deoxidized with 0.02 pct P.

Figure 30 shows the effect of minor ladle additions of manganese, aluminum and magnesium on shrinkage and gassing tendency of the 85-5-5-5 alloy in oil-bonded core sand molds. Ingot metal was melted in the indirect-arc furnace. Casting No. 20, deoxidized with 0.02 pct phosphorus, represents standard conditions and is gassed. The additions of 0.2 pct aluminum and 0.10 pct magnesium have greatly affected the amount of shrinkage, and give results from this standpoint similar to the lithium-treated castings. The aluminum-treated casting No. 22 shows a considerable amount of piping, and gross shrinkage porosity beneath this pipe. The manganese-treated casting No. 21 shows evidence of segregation.

The additions of manganese to 85-5-5-5 alloy were further explored, and the results shown in Fig. 31. Casting No. 24 represents standard metal and mold conditions and shows the macro-gas porosity. Some segregation was noted on all manganese-treated castings. Standard test bars were poured representing the base metal and the 0.20 and 0.50 pct manganese additions with the following results:

Bar No.	Manganese Additions*	Tensile Str., psi	Elongation % in 2 in.
	85-5-5-5 ASTM B62-49 Min.	30,000	20
571	None	41,000	28
682	0.20%	38,000	23
793	0.50%	35,000	15

* Actual analysis for manganese was not made.

The test bar poured with the 0.50 pct manganese addition was below the minimum requirements of the ASTM specification for elongation.

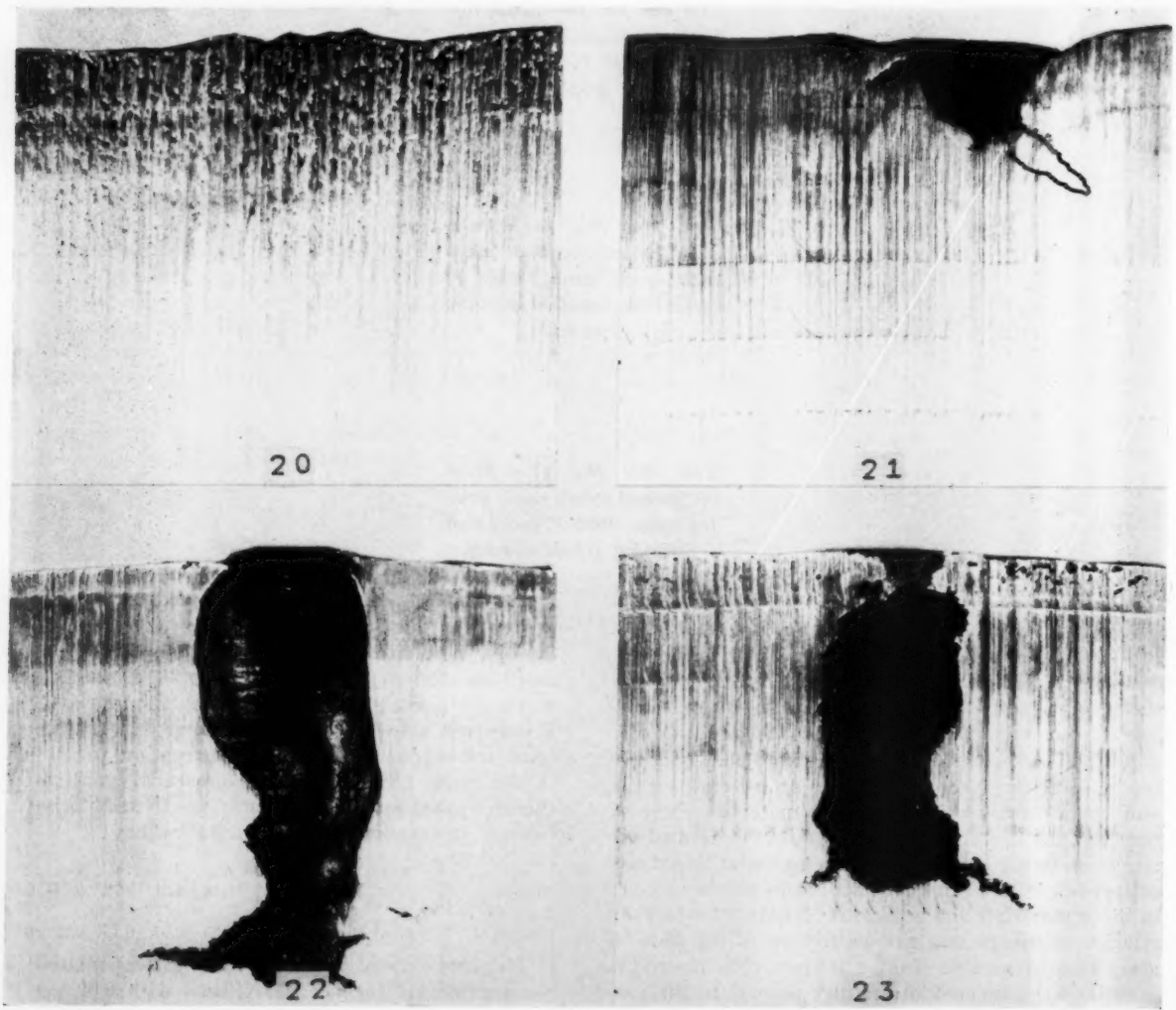
Fig. 30—Effect of minor ladle additions of Mn, Al, and Mg on shrinkage and gassing tendencies; test bar 4-in. diam. by 10-in. height, top gated (¾-in. diam. gate). Alloy 85-5-5-5; melt, indirect-arc furnace; mold, oil-bonded zircon sand; mold coat, standard graphite wash.

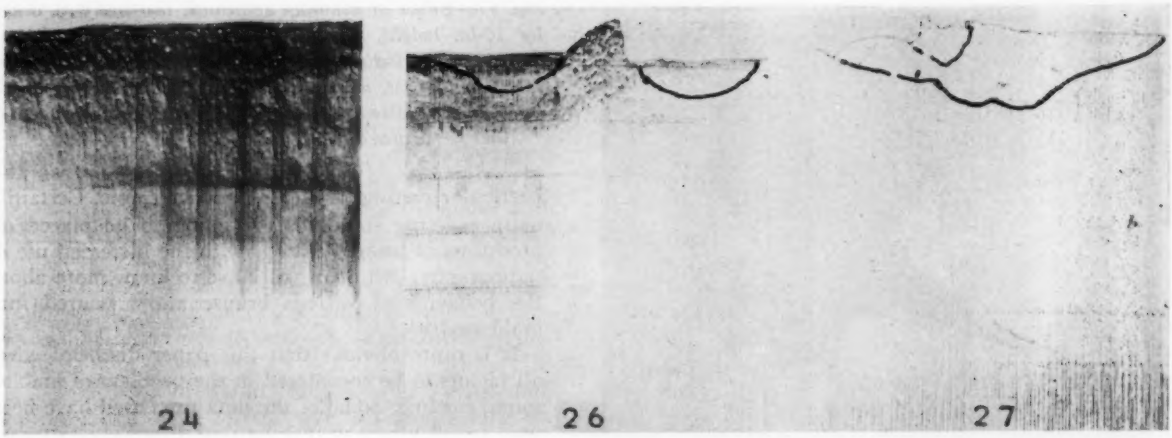
Test Bar No. 20—Deoxidized, 0.02 pct P; pouring temp., approx. 2100 F.

Test Bar No. 21—Deoxidized, 0.02 pct P plus 0.20 pct Mn; pouring temp., approx. 2100 F.

Test Bar No. 22—Deoxidized, 0.02 pct P plus 0.20 pct Al; pouring temp., approx. 2100 F.

Test Bar No. 23—Deoxidized, 0.02 pct P plus 0.10 pct Mg; pouring temp., 1990 F.





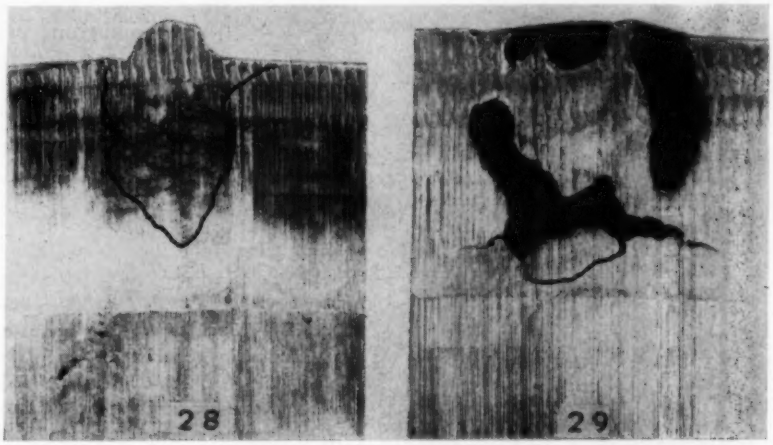


Fig. 31—Effect of Mn addition; test bar 4-in. diam. by 10-in. height, top gated (¾-in. diam. gate). Alloy 85-5-5-5; melt, indirect-arc furnace; mold, oil-bonded zircon sand; mold coat, standard graphite wash; pouring temp., approx. 2100 F.

Test Bar No. 24—Deoxidized, 0.02 pct P.

Test Bar No. 26 — Deoxidized, 0.02 pct P plus 0.10 pct Mn.

Test Bar No. 27 — Deoxidized, 0.02 pct P plus 0.20 pct Mn.

Test Bar No. 28—Deoxidized, 0.02 pct P plus 0.35 pct Mn.

Test Bar No. 29 — Deoxidized, 0.02 pct P plus 0.50 pct Mn.

An addition of 0.25 pct titanium (added in the form of 25 pct titanium-copper alloy) to the 85-5-5-5 alloy under standard conditions gave high shrinkage similar to that observed in the lithium, aluminum and magnesium-treated metal (Fig. 32).

These particular tests were included mainly to emphasize that residual quantities of certain elements can exert a tremendous influence on the casting characteristics of bronze alloys.

Foundries melting many different alloys have a scrap segregation problem. An error in mixing even small quantities of scrap of one metal with another metal may be very harmful, and considerable amounts of time and money may be spent in an effort to overcome some of the difficulties that could arise.

Lithium (0.01 pct lithium addition) was also effective in eliminating gas in the low zinc-nickel bronze alloy (Fig. 33). The standard shrinkage test (4 in. round x 10 in. high) on this particular alloy was not poured, but a relative difference in shrinkage can be observed on the top of the 2 in. round stick portion of the gas porosity test casting.

Degassing the 85-5-5-5 alloy with dry nitrogen gas was not successful (Fig. 34). The treatment with an inert gas was not expected to work since gassing of the alloy takes place in the mold cavity. The test was included to give further proof of this fact.

It is generally considered that gases in bronze are deleterious. However, the presence of small quantities of gas may be beneficial for inadequately fed castings because shrinkage porosity will be distributed more uniformly throughout the casting, resulting in a higher percentage of pressure-tight castings.

A recent experience can be cited along a similar line pertaining to a shell molded pump impeller (approximately 5 in. dia.—5 bladed, closed type) poured from Navy "M." Due to the complexity of the part, the casting was inadequately fed and shrinkage cracks resulted at base of the back shroud and vanes. Shrinkage cracks at these critical locations were eliminated by gassing the metal. This was done by pouring on top of a prescribed amount of charcoal which was placed at the bottom of the ladle with the phosphor copper.

Test bars were poured from metal representing standard melting practice and metal from the same melt gassed with charcoal. The type test bar used (machined to 0.505 in. dia.) is shown in Fig. 35, and the results of tensile tests are shown in Fig. 36. Certainly no one would accept the impeller with the shrink cracks, but how many would know that the crack-free impeller was gassed. It should not be misconstrued from the example cited that gassing of metal is advocated.

It is possible that a good many bronze castings are gassed either from the melting atmosphere or mold atmosphere. To what degree, or whether the gassing is beneficial or harmful for the use intended for a



Fig. 32—Effect of titanium additions; test bar 4-in. diam. by 10-in. height, top gated (¾-in. diam. gate). Alloy 85-5-5-5; melt, indirect-arc furnace; mold, oil-bonded zircon sand; mold coat, standard graphite wash; pouring temp., 2100 F; deoxidized, 0.20 pct P plus 0.25 pct Ti added in form of Ti-Cu alloy (25 pct Ti).

particular casting, is difficult to determine. Certainly more exacting standards are going to be placed on producers of bronze castings with the increased use of radiography. All of us will have to know more about the behavior of various bronze alloys poured into mold cavities.

It is quite obvious that this paper does not cover all factors to be considered in the problem of making sound castings. Some of the data presented have been used to advantage in making sounder production castings. Every alloy, and every type of mold presents numerous problems to the casting producer. It is hoped that this paper will stimulate further interest and thinking regarding the compatability of various metals and mold materials.

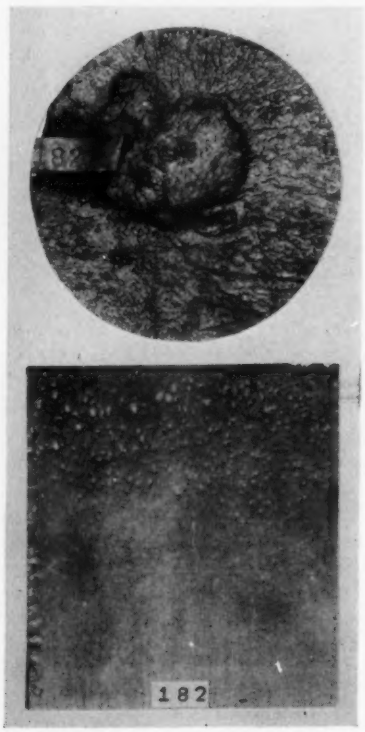




Fig. 33—Effect of lithium addition; low zinc-nickel bearing bronze (87.5 Cu, 5.5 Sn, 4.75 Pb, 0.5 Zn, 1.75 Ni); melt, indirect-arc furnace; mold, standard oil-bonded silica sand; mold coat, standard graphite wash; pouring temp., approx. 2100 F; deoxidized, 0.03 pct P.

Test Bar No. 182—Degassification, none.

Test Bar No. 183—Degassification, 0.01 pct Li.

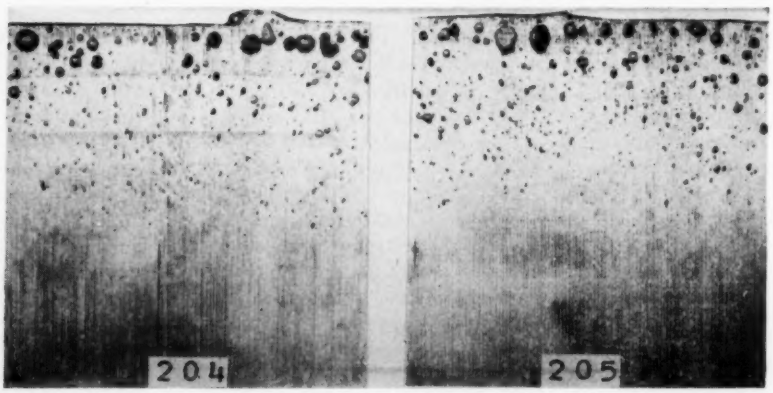


Fig. 34—Degassification with dry nitrogen gas; alloy 85-5-5-5; melt, oil-fired furnace (oxidizing flame); mold, standard oil-bonded silica sand; mold coat, standard graphite wash; deoxidized, 0.02 pct P.

Test Bar No. 204—Pouring temp., 2100 F; degassification, none.

Test Bar No. 205—Pouring temp., 2120 F; degassification, metal flushed with dry nitrogen gas for 3 min at 0.67 cu. ft per min. Phosphorus added after flushing.

Conclusions

1. Macroporosity of the spherical type was observed on all test castings in which ounce metal was poured into oil bonded silica sand molds. This gas occurred regardless of the type of melting unit or melting practice employed or type core oil used for a binder.

2. The porosity is believed to be the result of a reaction between the gas evolved from the mold and metal or direct absorption of the gas which is liberated on solidification.

3. a) The ounce metal poured into a pitch-bonded mold was free from macro-gas porosity, probably due to the difference in mold atmosphere produced by the pitch.

b) Castings produced from cement bonded and natural green sand molds were practically free of macroporosity.

c) Molds bonded with natural wood resin and urea resins appeared to be better from the gassing standpoint than the oil-bonded molds.

d) Phenolic resin molds gassed the castings as severely as the oil-bonded sand. Additions of pitch, 3-5 pct by weight reduced or eliminated the macro-gas porosity.

e) The following miscellaneous binders showed rather severe gassing:

1) Liquid and dry hemicellulose extract.
2) Organic binder comprised of pure hydrocarbon and organic vegetable binders (dry).
3) Mineral and organic type (dry binder).
4) Dextrinized clay proprietary binder.

Fig. 35—Standard type test bar for bronze alloys; mold, standard oil-bonded silica sand; mold coat, standard graphite wash.



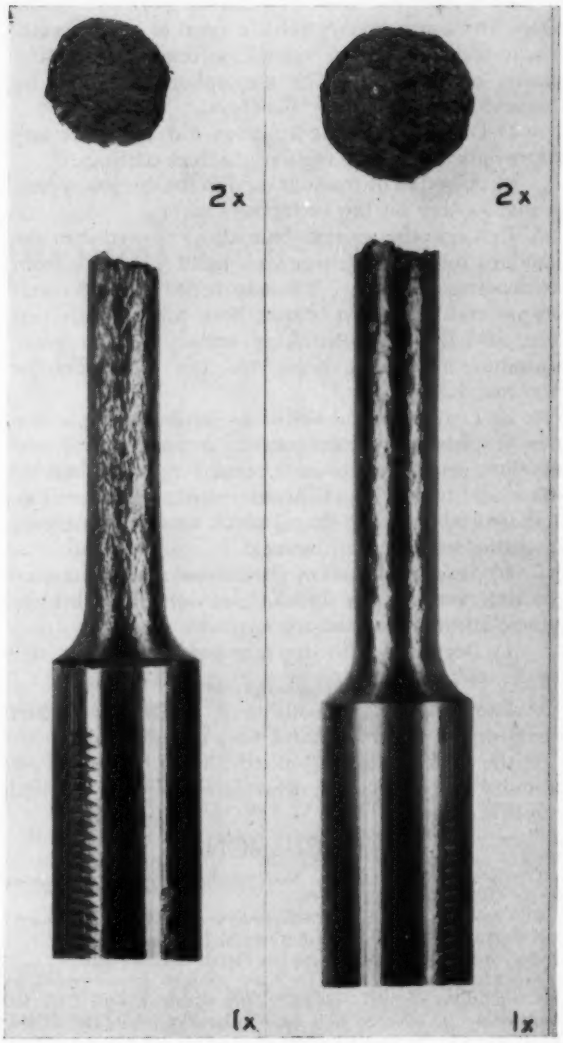


Fig. 36—Results of tensile tests on test bars shown in Fig. 35. Alloy Navy "M" (USN Spec. 46B8); melt, coke fired furnace.

Test Bar No. 1—Deoxidized, 0.02 pct P.

Test Bar No. 2—Deoxidized, 0.02 pct P; metal gassed by pouring on top of charcoal added with phosphorus in ladle.

	Tensile Strength,* psi	Elong., % in. 2 in.	Density
USN Spec. 46B8 min.	34,000	22.0	—
Test Bar No. 1	47,250	53.0	8.74
Test Bar No. 2	29,250	15.0	8.23

* Average of two test bars.

4. a) Graphite water-base coatings containing at least 15 pct pitch compound by weight successfully inhibited the formation of the macro-gas porosity when applied over an oil-bonded sand mold probably because of its effect on the mold atmosphere.

b) Sea coal additions to the graphite wash were less effective than pitch.

c) A coal tar pitch base paint inhibited the gas porosity.

d) A rubber cement base type of spray coating did not inhibit the gas.

e) A special coating containing zircon filler, water, and a proprietary vehicle (said to be a thermoplastic resin carried in volatile solvent) reduces the gassing considerably. The atmosphere produced by the vehicle appears to be beneficial.

f) Other inert type coatings did not offer any improvement over the regular graphite coatings.

g) All types of coatings used to inhibit gas appear to give poorer casting surfaces.

5. Certain other copper-base alloys poured into the standard oil bonded silica sand mold were free from the macro-gas porosity. These included the acid metal bronze and 11 pct tin bronze, both substantially zinc free, and the high shrinkage brasses—silicon brass, aluminum-manganese brass, and low aluminum-low iron manganese brass.

6. a) Lithium metal additions inhibited the formation of spherical type gas porosity in ounce metal, and the low zinc-nickel bronze poured into oil bonded silica sand molds. The lithium-treated metal was characterized by more shrinkage, which must be considered in gating and risering castings.

b) Small additions of aluminum, magnesium and titanium increase the shrinkage of ounce metal. Manganese additions caused segregation.

c) Degassing with dry nitrogen was ineffective.

Acknowledgment

Acknowledgment is made to B. F. Shepherd, chief metallurgist, Ingersoll-Rand Co., under whose direction the work was accomplished, and to H. D. Horton, foundry superintendent, who initiated and inspired the investigation.

DISCUSSION

Chairman: W. B. SCOTT, National Bearing Div., American Brake Shoe Co., Meadville, Pa.

Co-Chairman: R. A. COLTON, Federated Metals Div., American Smelting & Refining Co., Barber, N. J.

Recorder: J. F. KLEMENT, Ampco Metal, Inc., Milwaukee.

CHAIRMAN SCOTT: Often we have ascribed gassed metal to the conditions of melt. However, the author affords food for controversy in offering well substantiated evidence as to the singularly adverse role played by the oil-sand core. The mechanics of the reaction would seem to be over-simplified in offering the pitch-binder atmosphere as an explanation of the improved performance of that material. It would seem to this commentator that the significance of the rougher surfaces resulting from the pitch-bonded cores is important and might well be investigated further. To my knowledge no one has ever suggested that smooth surface castings would have a less sound internal structure. Personal observations on shell molded and metal molded castings have suggested this as a possibility, when working with 85-5-5-5 metal and related alloys.

It was encouraging to have the author report that the susceptibility to this spherical macro-gas porosity varied with the alloy. It would appear that the mechanics of the mold-metal interface reaction is more complex than is generally appreciated."

J. J. UPPGREN:[1] Do you feel that a less turbulent gating system would tend to eliminate some of this porosity?

MR. FISCHER: A less turbulent gating system might have a minor influence on the amount of gas porosity but would not be the prime factor in the elimination. Two methods of gating other than that shown in the paper were tested. (1) Using an extreme choke at the bottom which helped only slightly. (2) Using a step gate or second gate into the top of the bob where the gas is observed with a flow off into the 2-in. round. This method did not offer any improvement.

CO-CHAIRMAN COLTON: 1. According to the paper, the gas porosity found in the castings is directly attributable to the oil in the dry sand mold. Apparently the oil breaks down under temperature and forms gaseous materials which are then absorbed by the metal. Pitch is directly related to oil in that both are hydrocarbons. Generally speaking, the principal difference is that pitch is made up of heavier compounds. Why in one case does oil cause gas porosity and in the second case pitch prevent it, if we are dealing with two similar substances. It would seem that pitch would also produce gaseous substances similar to those produced when molten metal comes in contact with oil. Is there an explanation for this difference in behavior?

2. I wonder if the author has any explanation as to why various alloys behave differently in producing gas porosity with pitch and without it in the sand. In other words, has any work been done to indicate which of the various components of the alloys is responsible for picking up gas. It would seem worthwhile to make up alloys leaving out one of the ingredients in each case and see whether or not this behavior can be attributed to the presence of one of the component metals.

3. In several places the author has mentioned that the castings made with pitch-bonded sand had decidedly poorer surface appearance than those made without the pitch in the sand. The same castings, however, were free of gas porosity of the type described in the paper. It is my opinion that this poor surface on the castings can be attributed to the presence of reducing gases in the mold which permits penetration of the sand, on a local scale, by the metal. In other words, if metal oxides form readily in the mold, there is considerable resistance to penetration because of lower fluidity. On the other hand, with the presence of reducing gases it is entirely possible the oxide formation is retarded and fluidity thus increased enough so that the metal penetrates between the sand grains, giving a rough surface to the castings.

This falls in line with observed behavior that fluidity of metals in general is related to the presence of metal oxides in the liquid. A similar circumstance would be the casting of high lead bronzes that have been thoroughly deoxidized with phosphorus. If this is the case, severe penetration occurs and the casting surface is usually very rough. If on the other hand no deoxidizer is used, the castings are usually fairly smooth when they come from the sand. As a corollary of this, when the castings are smooth the metal is usually gassed, and when the surface is rough the metal is usualy sound. This, of course, is related to the presence of phosphorus which in one case has removed most of the oxygen and in the other case has not.

MR. FISCHER: 1. The gas porosity observed was not only attributed to core oil but to other binders as well—pitch excepted. Analyses were not made of the gases given off from the various binders used but such analyses may throw some light on the gas or gases which might be harmful to the 85-5-5-5 metal. It has been shown by Volenik* that when cores bonded with linseed or commercial core oils are burned in air or nitrogen approximately 22 to 30 per cent CO_2, 26-30 per cent CO, 6-21 per cent H_2, 12-27 per cent CH_4 and 7-15 per cent C_2H_4 is given off. It was definitely observed that the gases coming off the pitch-bonded molds appeared denser and had a dark gray color. The gases given off from the oil-bonded molds appeared much less dense and were a light gray color. Carbon residue or soot deposits were prevalent in certain locations of the castings poured from pitch-bonded molds, while there were no carbon or soot deposits observed in molds made from oil-bonded sand. It is assumed from these general observations that the analyses of the gases coming from each mold would be quite dissimilar although both materials are considered to be hydrocarbons.

2. Some work was done in reducing the percentage of zinc in the 85-5-5-5 alloy. Alloys were made with 2 per cent and 1 per cent zinc. It was thought that a slight improvement could be noted with the 1 per cent zinc. The reason for selecting zinc as the component to reduce was based on the fact that the acid metal composition and 11 per cent tin bronze (see Table 1) which are practically zinc free did not show the spherical type macro gas porosity. Zinc alone does not appear to be the main deleterious component because the bearing bronze containing only residual zinc, 8 per cent tin and 8 per cent lead was also gassed similar to the 85-5-5-5 composition. The acid metal bronze also containing approximately 8 per cent tin but only 2 per cent lead, is gas free. It would appear that the effect of different alloys is quite complex and would require a time consuming, costly, but interesting research program to get concrete data on the effect of the various components.

3. The roughness observed on the castings should have been more thoroughly described. It was not due to metal penetration into the sand as mentioned in Mr. Colton's discussion. The roughness was attributed to a collection of soot at localized areas which caused pock marks or depressions in the casting. There was no adherent sand in these locations.

[1] Foundry Manager, Northern Ordnance Inc., Minneapolis.

* "Measurement and Investigation of Gases Escaping From Oil Cores," by Vojtech Volenik, TRANSACTIONS, AFA, vol. 54, p. 86 (1946).

AGAR GELS IN PLASTER-BONDED INVESTMENT

By

H. Rosenthal* and S. Lipson*

ABSTRACT

In order to utilize a moist foamed-typed investment previously developed for aluminum alloys, a suitable refractory precoat was needed. This work shows that such a precoat is one containing 0.2 per cent agar in a water-mixed solution with the standard 70:30 silica-gypsum investment. The effect of the agar was to thicken the slurry so that a dipped pattern is completely covered without appreciable loss by drainage.

Because of the interesting properties of agar, the experimental work was extended to cover its addition to standard nonferrous investment. It was found that 0.1-0.2 per cent addition of agar to the water component was sufficient to inhibit a common type of casting defect. This defect is associated with separation of free water from the investment mix during the setting process.

Plaster-bonded Precoats

The possibility of utilizing the retained moisture in an investment to increase its chilling power has been investigated.[1] This work indicated that an investment with sufficient permeability would permit the steam generated upon contact of the molten metal with moisture in the investment to escape through the pores. The investment, however, was so porous that the molten metal penetrated the surface layer and created a serious surface defect. In order to avoid this condition, it was necessary to furnish a precoat which would provide a barrier between the casting surface and the porous backing investment. The precoat used for this work consisted of a slurry of the standard nonferrous investment. This precoat worked quite well, except for certain difficulties in application. The investment had a tendency to run off the pattern, leaving areas which were bare. It was necessary to allow the slurry to set up slightly before the pattern was dipped. This reduced the useful life of each batch of precoat, and even after thickening the coating tended to collect in a fairly heavy layer at the lower surfaces of the pattern.

At the time this work was done it was recognized that certain improvements in the precoating technique were necessary. As a logical starting point in the process of improving the precoat, the precoats used for the two-coat ferrous investment were studied. These, however, are unsuitable because the expansion characteristics do not match the gypsum-bonded backing investment, and because they require a high temperature firing cycle to develop the requisite strength and permeability. It was therefore apparent that a nonferrous precoat should contain a gypsum binder to match both the expansion characteristics of the backing and to develop a sufficiently high green strength.

The standard investment should make an excellent precoat if the problem of the mobility of the slip could be solved. In the ferrous field the mobility of the precoat is controlled by adding acid to the mix. This reacts with the silicate and forms a silica gel, thereby thickening the mix to the desired consistency.

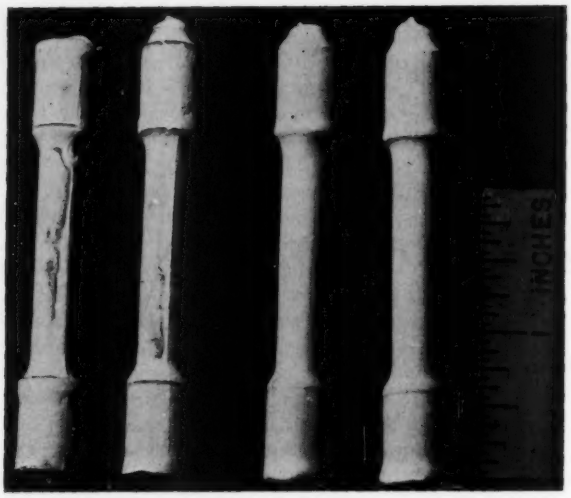
It appeared that if a substance were added to the nonferrous slip which would form a gel at room temperature, it would accomplish the same purpose as the silica gel in the ferrous precoats.

The substance agar seemed to fill these requirements and accordingly was tested. Preliminary work indicated that agar additions produced the anticipated effect.

Agar in Precoats

A stock solution was prepared by heating sufficient agar with water to make a 1 per cent solution. It was necessary that the solution be gently boiled for a few minutes in order to completely dissolve the solid matter. It was then permitted to cool to approximately 50 C (120 F) and maintained at this temperature for use. Excessive evaporation was avoided by keeping the container covered. This solution was added to the water just prior to mixing with the investment. The concentration of agar used in the mixing water is insufficient to form a perceptible gel in the time between adding the stock solution and proceeding with the mixing. However, once the water has been thoroughly incorporated with investment a pronounced thickening of the mix is effected. This is probably due to the setting process of the gypsum which removes water from the mix and thereby increases the effective concentration of agar. Figure 1 shows specimens which have been precoated by single dips in investment mixes with and without agar additions.

* Metallurgist, Pitman-Dunn Laboratories, Frankford Arsenal, Philadelphia, Pa.



A B

Fig. 1—Precoated patterns dipped in slurry containing (A) 60 cc H_2O/100 grams investment and (B) 60 cc H_2O with 0.2 per cent agar/100 grams investment.



Fig. 2—Casting obtained with mold investment with (A) 60 cc H_2O/100 grams investment ratio, (B) 60 cc H_2O with 0.2 per cent agar/100 grams investment ratio.

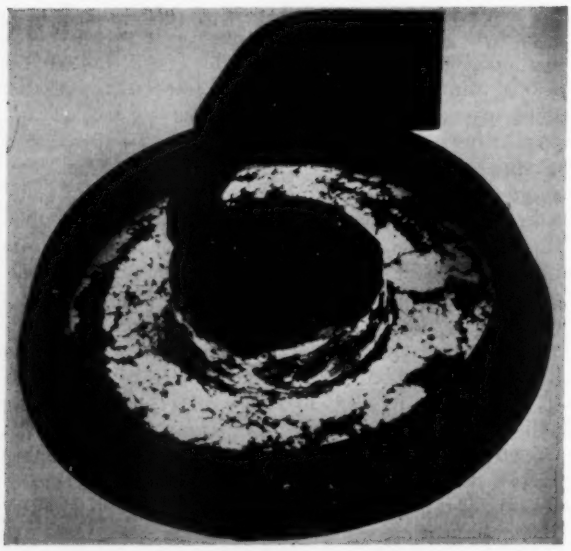


Fig. 3—Test pattern used to study defects caused by water separation from the investment slurry. Almost full size.

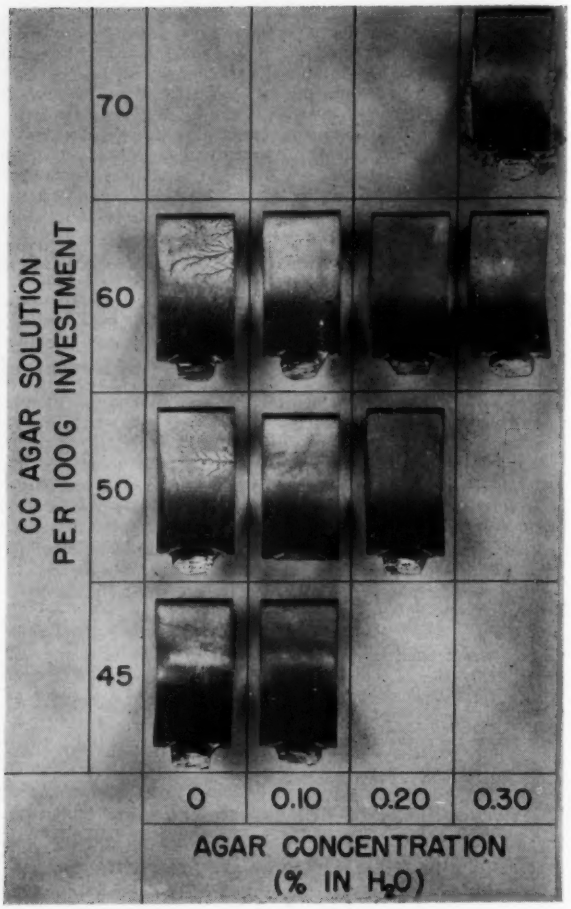


Fig. 4—Under side of test castings showing effect of agar concentration on the occurrence of defects caused by water separation. Approximately ½ original size.

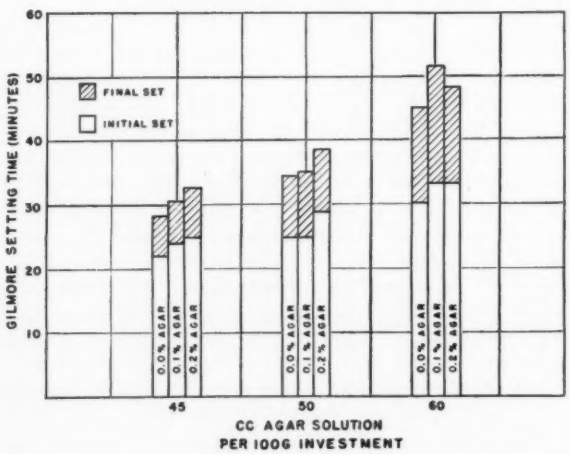


Fig. 5—Effect of agar concentration on setting time of gypsum bonded silica investment.

The effectiveness of the agar in arresting the draining process is shown by the better coverage of the specimen dipped in the agar mix and by the lesser amount of drainage that occurred with the agar mix.

Defects in Single Coat Investments

One of the most persistent surface defects encountered with nonferrous investments is apparently due to separation of water from the investment during the setting process. This water collects on the under sides of the specimen due to the settling of the solid material, and prevents complete contact of the investment with the pattern surface. The corrective measure for this type of defect is to reduce the amount of water used in the investment. This is sometimes awkward because with reduced water the investment becomes quite thick and difficult to handle and to vacuum.

With the experience gained in using agar for the precoats, it was anticipated that this material would be useful in improving the surface condition obtainable with the nonferrous investments. It appeared that the creation of a gel within the freshly mixed investment would inhibit the migration of the free water and prevent its accumulation on the under sides of the specimens. Figure 2 shows two castings made with a high water/investment ratio. The casting on the left shows the characteristic defect while the casting on the right made in a mold containing 0.2 per cent agar shows complete freedom from these defects. The high water/investment ratio was used to magnify the effect of excessive water in the investment mix.

Agar in Single Coat Investments

The effect of agar concentration was considered. The preliminary test had shown that 0.2 per cent agar in the water was effective. However, the presence of agar in the water also reduces its surface tension, and as a result the tendency of the mix to spill over the container during the vacuuming operation was intensified. The vacuuming operation causes the bubbles entrained in the investment to expand, rise to the surface, and break. Since the reduced surface tension caused by the agar tends to stabilize the bubbles, they increase to a larger diameter before bursting. This means that a deeper container is necessary to effectively perform the first vacuuming operation without spilling of the mix. The second vacuuming, when the patterns are already invested, does not cause as great an increase in the volume of the mix since most of the air churned in during mixing has already been removed. These considerations indicate that the smallest concentration of agar that will effectively inhibit water separation from the investment is most desirable.

A test specimen was designed and oriented in the mold in such a manner that the defects caused by water separation could be intensified and studied. This special pattern is shown in Fig. 3.

The pattern was oriented in the flask so that two separate under side surfaces were created. One surface made an angle of 45 degrees with the axis of the flask and the other was oriented just 90 degrees from this axis. This produced the type of undersurfaces which were most prone to develop the defects discussed above.

These patterns were invested using a range of water/investment ratios and a range of agar concentrations. The results of this test are shown in Fig. 4. The photograph shows the under side of these specimens. The upper surface in all cases was free from the defects under discussion.

The specimen obtained using 45 cc H_2O/100 grams investment corresponds to the mix currently being used for nonferrous castings. The characteristic defects are just discernible on this specimen. With more water in the mix, intensification of the defect occurs. The addition of agar to these mixes effectively suppressed the water separation and resulted in relatively smooth undersurfaces. Examination of these specimens indicated that with increased water/investment ratios better results were obtained with increased agar concentration. When the water ratio was increased to 70 cc H_2O/100 grams investment, excessive cracking of the mold resulted. The most satisfactory mix from the point of view of ease of handling and resulting surface condition was the one containing 0.1 per cent agar in the ratio of 50 cc H_2O/100 grams of investment.

The effect of the agar addition on the setting time of the mix was also studied. Gilmore setting times were obtained for the investment mixes in accordance with ASTM Specification C191-49. The data obtained are shown in Fig. 5. This indicated that the addition of agar was responsible for only slightly retarding the setting of the investment.

Conclusions

It is concluded that agar additions to investment mixes in the concentrations of 0.1—0.2 per cent agar by weight in the mixing water are effective in: (1) arresting the mobility of the investment when used as a dipping precoat, and (2) suppressing the collection of free water on the under side of the patterns when used as a single coat investment.

Acknowledgment

The authors are indebted to the Department of the Army for permission to publish this report. The opinions expressed in the paper are those of the authors and not necessarily endorsed by the Department of the Army.

References

1. H. Rosenthal and S. Lipson, "Precision Casting Aluminum in Moist Investment Molds," A.F.S. TRANSACTIONS, vol. 58, pp. 460-464 (1950).

DISCUSSION

Chairman: W. A. MADER, Oberdorfer Foundries, Inc., Syracuse, N. Y.

Co-Chairman: D. L. LAVELLE, American Smelting & Refining Co., Barber, N. J.

Recorder: J. L. SCHMIEDER, JR., Oberdorfer Foundries, Inc., Syracuse, N. Y.

T. E. MOORE *(Written Discussion):*[1] I want first to compliment the authors for the interesting paper they have presented. I was present at the meeting when their paper "Precision Casting Aluminum in Moist Investment" was read,* and the general technic appealed to me as quite a satisfactory one. However, there was the difficulty of the short time interval in which the plaster bonded investment coat could be applied.

[1] Manager, Industrial Division, The Ransom & Randolph Co., Toledo, Ohio.

* See AFS TRANSACTIONS, vol. 58, pp. 460-464 (1950).

In our opinion it was too difficult to hold on the pattern until it had set, to make it a practical production technic for general casting.

The authors did not find the ferrous investment coat satisfactory because of the difference in thermal expansion between the coat and plaster bonded investment. The ferrous coat is more easily handled than the plaster bonded coat—a uniform consistency of mix can be maintained all day, since the setting reaction does not start until the pattern is coated. Why not use the ferrous back up investment with this ferrous coat for the non-ferrous castings? It is not necessary to heat the ferrous investments and ferrous coats above 1200 F. I know of one plant that is casting over 1000 ferrous investment flasks a day, and their standard burn-out temperature is 1200 F.

In my opinion, the ferrous coat and back up investment procedures warrant more study because, although the cost is about the same, these investments have much greater permeability and can be cooled to low temperatures with less danger of fractures.

We have found the authors' agar gel method a very good one for arresting the separation of water. Agar has an advantage over other organic gelling agents in that it is more effective at low concentration—thus it is more easily eliminated during the burn-out cycle. It is quite efficacious as an agent to arrest the mobility in plaster bonded pre-coats. But agar also has some disadvantages. The agar available on the market varies greatly in its water content, ease of solution, and gel-forming capacity. While a solution of the authors' agar was easily made by gently boiling for a few minutes, several other brands that are in common use are very difficult to put in solution; and since they require several hours of boiling it is common practice to use about 5 lb of steam pressure instead of the slow boiling method.

One grade of agar we used to make, a 0.1 per cent solution gelled in 80 F water before a mix of investment could be made; while a 0.2 per cent solution of another brand did not gel in 70 F water.

The different grades of agar greatly affect the investment consistency: for example, a 0.1 per cent solution of one produced an investment slump of 2½ in. diam when mixed 50 cc solution to 100 grams investment; while the slump of another grade was 4½ in. in diameter.

Because of the non-uniformity of agar I think anyone using it to prevent water separation in investment will find it necessary to vary the concentration until one is found that will meet their requirements.

The authors have stated that the additions of agar to the water make the slurry more difficult to vacuum because of the reduced surface tension, which causes the bubbles to expand to a greater degree before bursting. While this is true, we have found that the increased volume during the vacuum operation is so small that we think no one would find it objectionable. It is much better than tragacanth and many other organic gelling agents. The agar, even in high dilutions, lowers the surface tension of the investment slurry enough to greatly improve its coverage of the pattern.

The authors have found that when the water/investment ratio was increased to 70/100 the agar gel prevented the water separation, but that with this high water content excessive fracture of the mold resulted.

Our limited experience with agar gels in investments has shown that approximately 50 cc of 0.1 per cent solution of a certain brand to 100 grams investment produces the same consistency mix as 45 cc of water. The use of this 50 cc mix of 0.1 per cent agar entirely prevented water separation on test patterns similar to those used by the authors. This was shown by the absence of water streaks on the castings. They frequently appeared when the 45/100 water/investment ratio was used—however, when we increased the water/investment ratio to 50/100 as in the 0.1 per cent agar slurry, we decreased the strength of the investment over 20 per cent. While there was no noticeable finning of metal on the castings of the simple test patterns, when we cast large oval shaped parts placed vertically in the flask, or round turbines set up in the horizontal position, we invariably experienced excessive fracture of the mold, with resultant finning. These fractures were not apparent when the 45 cc water to 100 grams investment ratio was used. May I ask the authors if they have noticed this on any of their castings?

There is no question in my mind that the findings of the authors will greatly improve the results that can be obtained with many non-ferrous investments. The agar can be readily handled in the research laboratory, but will the production man be willing (1) to make the stock agar solution; (2) to keep it at the proper elevated temperature; and (3) to measure the amount required for each mix? Would he not prefer to use an investment that contained the gelling agent, thus saving him the extra operations which entail additional expense?

ROBERT NEIMAN *(Written Discussion):*[2] First the authors should be thanked and congratulated for another of their papers dealing with Plaster Casting and Precision Investment Casting. Perhaps this is not entirely unexpected since one of the authors is Chairman and Subcommittee Chairman respectively of the two A.F.S. Committees.

The concise form and modest statements in this paper hardly portray the real import of this most timely contribution.

Investments for precision casting are an outgrowth of dental investments. The latter were modified somewhat for jewelry casting and in most cases performed quite satisfactorily, in fact so well that it was used and made possible precision non-ferrous casting. As investment casters learned to cast larger and more complicated shapes, the investment became less adequate.

Several years ago the cracking of molds with resulting "fins" and often increased dimensions was the chief difficulty with non-ferrous investments. Improved burn-out schedule, controls, and casting methods have reduced cracking. An impartial survey recently would most likely disclose "water marks" as the leading investment difficulty.

As so well pointed out and illustrated by the authors, these "water marks" occur most often on the bottom side (as invested) and to a lesser degree on angulated or vertical surfaces, rarely if ever on the top surface.

This common fault occurs and disappears in spasms and has been blamed on careless operators, non-uniform investments, temperature, humidity, careless use of oil or grease on the patterns or assembly base, and even on varying additions to given municipal water supplies.

At the time the authors made this study, we doubt if any available commercial investment would produce consistently "water mark"-free castings. The use of "agar" was not known, certainly not generally known in the industry. "Dopes" as complicated and less efficacious than "Indian" tonics were tried without too much success. Agar is therefore a welcome "first aid" remedy and even works well as a preventative "typhoid shot."

The use of agar has a few slight disadvantages; the authors pointed out the minor difficulty in vacuuming and a slight thickening of the mix. From a practical standpoint it involves making a boiling solution and adding it to the mixing water in controlled amounts. Unfortunately in many plants the proportioning of water is slipshod and extra additions would be frowned upon.

Investment makers evidently had overlooked the authors' agar procedure or did not publicize it, because of their belief that they needed something in the investment itself to remove water marks in order to insure its positive and uniform use. It is gratifying to report that several recent investments will produce not only water mark-free castings but produce castings of enhanced smoothness all over. Significantly those tested have no adverse effect on vacuuming nor appreciable change in other properties. We wonder if the authors have had a chance to test these yet.

In conclusion it may be stated that whether the user buys an investment containing a water mark remover or uses any other type, he is now in a position to prevent its occurrence by a relatively easy means requiring the exercise of only a minimum amount of control. The authors are to be sincerely congratulated on making this important contribution to the precision investment caster.

H. ROSENTHAL AND S. LIPSON *(Authors' Closure):* The authors are indebted to Mr. Neiman for his contribution in this discussion. The authors concur with Mr. Neiman's observation that this treatment may be considered as a preventative

[2] Director of Research, Chemicast Division, Whip-Mix Corporation, Louisville, Ky.

even when the appearance of "water marks" is only an occasional plague. The authors are unfamiliar with any recent investment which eliminates water marks. Information on these materials would be greatly appreciated.

The authors are in general agreement with all the points raised in Mr. Moore's discussion. The experiments described form a valuable supplement to the work covered in our paper.

The use of a plaster bonded precoat is proposed to complement the investment used in molds where some of the chemically held water is utilized for increasing the chilling power of the mold material. This mold, therefore, is not really subjected to a firing cycle, *per se*, but simply a low temperature drying cycle to drive off the free water in the investment. Mr. Moore's point that the ferrous two coat investments warrant consideration for nonferrous casting is well taken. The greater permeability is undoubtedly a valuable asset and may be extremely useful for those nonferrous application where permeability and high resistance to cracking is demanded.

It is unfortunate that this material, agar, is so variable a product that specific concentrations necessary to inhibit "water mark" defects cannot be applied to all brands. It has been the authors' experience, however, that a fine grade of the type used for preparing bacteriological culture media can be relied upon to give consistent results and to go into solution without difficulty. This grade is considerably more expensive than the grades tested by Mr. Moore, however, when it is considered that 1 lb of agar will treat 2000 lb of investment, the increased cost is not believed to be prohibitive. For some applications the use of agar may actually permit extending the investment such as when a 50cc/100gm ratio is used instead of the 45cc/100gm ratio required without agar.

The strength of the investment unquestionably suffers when the water to investment ratio is increased. However, as Mr. Moore comments, for certain parts this results in no noticeable effect on the casting. Where the nature of the casting demands the maximum strength of the investment it will not be possible to increase this ratio. However, these denser mixes should require smaller concentration of agar to suppress the occurrence of the water mark defect.

The authors have experienced no increased tendency toward mold cracking that may be attributed to the presence of agar. The experience of others who have used the agar additions on a production basis would be welcomed.

Finally, the transition of the use of agar from the laboratory to the production plant could undoubtedly be facilitated if a grade of agar were used which could be relied upon to give consistent results. Since only volumetric measuring is required in making the agar addition from the stock solution, it is not believed that this operation would significantly change the cost picture of the investment operation.

A STUDY OF SEGREGATION IN BRONZE USING A RADIOACTIVE TECHNIQUE

By

W. C. Winegard*

ABSTRACT

The segregation of tin in small castings of 10 per cent bronze was investigated by the use of a radioactive tracer technique. Results are reported for various cooling conditions. It is established that inverse segregation does occur in very small castings.

■ The normal segregation that occurs when a large mass of metal is solidified is termed *coring*. The first material to solidify contains a smaller proportion of the solute than the remainder. This means that the outside of a casting will be purer than the center. In some non-ferrous alloys, however, there is a tendency to reverse this type of segregation. The outside of the casting, which is the first part to freeze, may contain more solute than the last part to freeze. When segregation is of this type, it is called *inverse segregation*.

It has long been known that tin bronzes exhibit marked inverse segregation during solidification. A recent review by Pell Walpole and Hanson[1] indicates that this phenomenon has not hitherto been studied by the radioactive tracer technique. The purpose of this paper is to set forth the technique of autoradiography and to show the results obtained with this technique in the laboratory. From the results, it is clearly seen that laboratory tests give a true indication of what actually occur in large castings.

Making an Autoradiograph

The autoradiographs in this work were obtained by the use of a relatively simple procedure which has been used by several investigators.[2,3,4] It is necessary, of course, to have one constituent of the alloy radioactive. When the alloy solidifies, the casting is sectioned longitudinally, polished and placed on a photographic plate for several hours. The radioactive material present in the alloy gives off radiation which will affect the emulsion on the plate in the same manner as light does. Thus, an image of the specimen will be reproduced on the photographic plate. If a particular section of the casting contains no radioactive material, then the emulsion under that part of the specimen will remain unaffected.

If resolution is not too important, the film used can be a coarse grained x-ray film which reacts rather rapidly to the radiation and an autoradiograph may be obtained in a few hours. However, should resolution be an important factor, a finer grained film should be used. Lantern plates may be used for this type of work, and were used in the present investigation. As a general rule, it will take twice as long to produce the same density image on a lantern slide plate as on an x-ray film.

If resolution is of prime importance, then nuclear track plates may be used. Nuclear track emulsions are very fine-grained and thus have a high resolving power. It is possible to actually trace the path of a

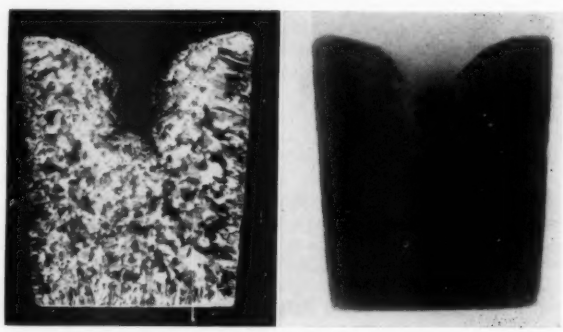


Fig. 1—Macrophotograph (left) and autoradiograph (right) of bronze casting poured in steel mold. X 2/3.

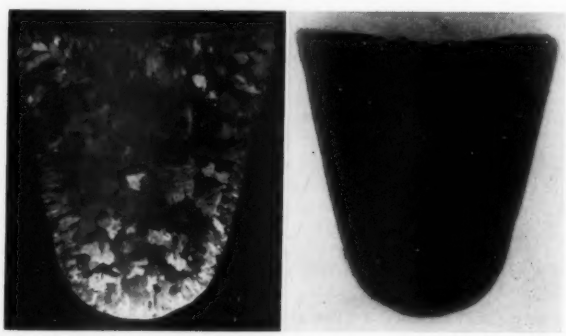


Fig. 2—Segregation is more distinct and columnar zone is deeper in casting poured in ceramic mold. X2/3.

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radioactive particle through the film. The particles ionize the emulsion while travelling through it and on development the track of the particle is visible. Nuclear emulsions can be made so that they will only be sensitive to certain energy particles and thus the background fogging of the emulsion will be reduced. Autoradiographs obtained by the use of nuclear track plates usually require an exposure time of about four times that for an x-ray film.

When the casting has been in contact with the film for the required time, the specimen is lifted off and the photographic plate processed in the normal manner. While the casting is in contact with the film, all light must be prevented from reaching the plate. This may be accomplished by loading both plate and specimen into a light tight plastic bag for the exposure.

Use Radioactive Tin

Grade D phosphor bronze (Sn, 10.63 per cent; P, 0.1; Pb, 0.04; Fe, 0.07; Zn, 0.2) was used in the investigations. To this material 0.1 gram of tin containing tin 113, a radioactive isotope, was added. The whole was melted in an electric muffle furnace and allowed to remain at 1080C for several minutes to ensure homogeneity in the liquid. The radioactive tin diffuses through the liquid and behaves exactly the same as the remainder of the tin except for its radiation properties. Three small castings were poured from 1080C. The first melt was poured into a steel mold, the second into a ceramic mold, and the third into a hot ceramic mold with the bottom insulated so that freezing proceeded from the top downwards. Figure 1 shows the macrophotograph and autoradiograph of the casting poured into the steel mold. The darker portions on the autoradiograph show where the tin content of the casting is highest.

Figure 2 is the macrophotograph and autoradiograph of the casting poured into the ceramic mold. The columnar zone is deeper and the segregation, as shown on the autoradiograph, is more distinct. This would seem to indicate that the segregation observed was connected with the appearance of a good columnar zone. This is in accord with other experimental evidence on inverse segregation.

In normal castings, columnar growth occurs in from the sides and up from the bottom of the ingot. It was decided to investigate inverse segregation under conditions where solidification would proceed from the top downwards. This method of solidification was produced by casting into a hot ceramic mold with the bottom of the mold insulated to prevent heat loss. The resulting casting and autoradiograph is shown in Fig. 3. The bottom of the ingot is porous.

The most striking case of inverse segregation observed during this investigation occurred in a small specimen of 10 per cent bronze 2 in. long and ½ in. in cross section. The alloy was heated in a graphite boat using an apparatus similar to that described by Chalmers[5] for the growth of single crystals. When the bronze was molten, the graphite boat was removed from the furnace and solidification occurred from the outside inwards in all directions. The specimen was sectioned, polished, photographed and an autoradiograph was taken. The specimen and the autoradiograph are shown in Fig. 4. The segregation is very marked, the tin-rich material being at the outside region which was the first to solidify.

It is felt that the technique described above may be usefully employed in solving many practical casting problems. It has been shown by several investigators that inverse segregation is more pronounced when melting occurs in the presence of some gases. The study is by no means complete and it seems reasonable to suppose that more quantitative information will be made available by the use of this technique.

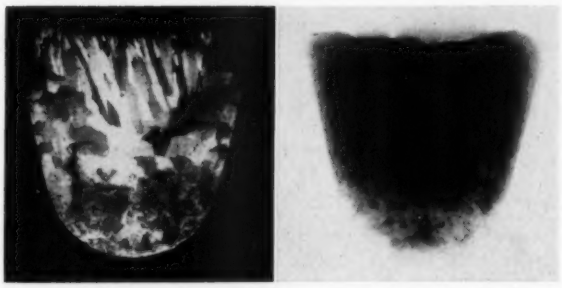


Fig. 3—Casting solidified from top down in hot ceramic mold insulated at bottom to prevent heat loss. X 2/3.

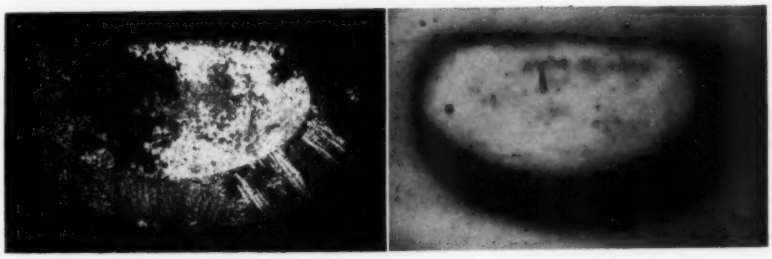


Fig. 4—Bronze specimen cooled in hot graphite boat to make solidification proceed from outside inwards in all directions. Outside region which solidified first is high in tin. About X 5.

Acknowledgments

The author would like to thank Dr. B. Chalmers, Dr. W. R. Thomas and R. S. Davis, for helpful discussion. The help of F. de Maio and R. Urlocker in the preparation and examination of the castings is gratefully acknowledged. The work was supported by the Defence Research Board, Canada, and the National Research Council, Canada.

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DISCUSSION

Chairman: W. B. SCOTT, National Bearing Div., American Brake Shoe Co., Meadville, Pa.

Co-Chairman: R. A. COLTON, Federated Metals Div., American Smelting & Refining Co., Barber, N. J.

Recorder: J. F. KLEMENT, Ampco Metal, Inc., Milwaukee.

CHAIRMAN SCOTT: Would the radioactive technique be useful as a control method for a small foundry?

MR. WINEGARD: The radioactive technique would be of some use as a control method, but the real application in foundry work is likely to be as a tool in trouble shooting.

S. V. ARNOLD:[1] I should like to enquire whether the author attempted to quantitatively measure segregation in cast bronze by photo-densitometer scanning of autoradiographic negatives exposed in contact with sections of the test castings.

MR. WINEGARD: The autoradiographs in this work were not examined by a photo-densitometer. However, the author has measured segregation in other systems at least semi-quantitatively by a microphotometer technique.

E. F. TIBBETTS:[2] The conclusion of this work is that in order to overcome inverse segregation, pouring temperatures must be held as low as possible. According to Ames high pouring temperatures produce the best columnar type structure for maximum pressure tightness. What procedure should we follow?

MR. WINEGARD: The conclusion of this work is that to avoid inverse segregation it is desirable to avoid columnar type structures. This may be done by pouring at proper temperatures or by the use of a nucleating agent to promote nucleation. The conditions which one must use are of course determined by the service in which the casting is to be used.

G. L. LEE:[3] Since most bronze foundrymen consider lead or tin sweat types of segregation due to gas in the metal, can you tell us what percentage of the 11 types of segregation that you have identified, are due to gas?

MR. WINEGARD: It is difficult to say what types of segregation are caused by gas in the metal. It is known that the presence of gas in the melt will make inverse segregation more pronounced and it is felt that the amount of segregation, regardless of the type, will be affected by the presence of excess gas.

A. J. MESSMER:[4] Does the segregation you describe have any effect on the machinability of the castings? What must be done to overcome it?

MR. WINEGARD: Inverse segregation in the extreme form will affect the machinability of the casting. To prevent inverse segregation one must eliminate a columnar zone and remove excess gas from the melt. A good review of this subject has recently been completed by Pell Walpole and Hansen "Cast Tin Bronzes".

J. W. HORAN:[5] Does the radioactive element in this technique permeate the tin content of the alloy uniformly and if so, could this method be utilized for the quantitative determination of tin in the alloy?

MR. WINEGARD: It is felt that the radioactive tin does mix uniformly. The method could be used for quantitative determination of tin in this alloy but a good deal of work would be required to set up the necessary standards.

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[1] Watertown Arsenal, Philadelphia.
[2] Wollaston Brass & Aluminum Foundry Inc., North Quincy, Mass.
[3] The International Nickel Co., Inc., Bayonne, N. J.
[4] Messmer Brass Co., St. Louis.
[5] American Crucible Products Co., Lorain, Ohio.

DEVELOPING STANDARD DATA TABLES FOR CORE FINISHING

By

L. L. Martin*

ABSTRACT

This paper presents an approach to the problem of developing Standard Data Tables for finishing medium and heavy cores in a weight range from 15 to 1500 lbs. Although this type of operation is considered in many plants as indirect labor and operating under an indirect labor incentive plan, it has been found to be highly advantageous and successful to develop direct standards to cover the operations.

An analysis of the work area shows that time studies taken must be on an elemental basis, and all studies described in the text were so taken.

It is to be assumed that any plant considering the development of Standard Data for core finishing has already in effect Standard Data for coremaking. Certain definite information is generally tabulated for each and every core for those standards, and such information may be utilized in many ways in developing the finishing Standard Data.

Since core finishing methods vary more greatly in different plants than, for example, machine molding, a detailed account of the work area and methods of finishing is carried on throughout the paper. This will indicate the scope and variety of the problems and the approach to and solution of these problems.

The Standard Data Tables described are in actual use today, with the men earning a good premium and having confidence in the standards and the Standards Department.

The method of taking the time studies, leveling, determining the speed ratings and relaxation allowances are not treated in detail, as these subjects should be treated as individual topics rather than as component parts of this paper.

Standard Data

There are many reasons for using the method herein described for setting standards. Among the more important reasons are:

1. **To Establish Consistent and Fair Standards**—Standards set by Standard Data are fair and consistent in relation to like jobs because the Standard Data Tables are developed from time studies covering all phases of the operation performed by various operators.

2. **Workman's Confidence**—The man in the shop has confidence in the standards and the Standards Department when he knows that the standard has been set to certain specifications, and that these same specifications and the time allowed for them are recorded and the information can be made available to him, if necessary.

3. **Time Saving**— Standards can be set in a relatively short time. Where 100 per cent standard coverage is required, it is practically impossible to do so with direct time studies.

4. **Determining Costs**— In the face of highly developed competition, being able to pre-determine costs is a prime requisite in obtaining new business.

Work Area

The work area for the entire group of finishers, finishers helpers, and crane operator is shown in Fig. 1. The night drying ovens are located on each side of the core car transfer pit. The "Car Unloading Station" is located near the end of the core car transfer pit, with an electrically powered cable winch located immediately at the end of the pit. The finishing area is approximately 40 by 28 ft, and in the area are 13 pairs of steel horses, each 6 ft long and 25 in. high, positioned in two rows. Between the two rows of horses are two work tables, each 10 ft long and 2 ft wide with a shelf below which is used for storing miscellaneous tools and equipment. Above the tables are racks for holding gas drying torches and pilot flames for lighting the torches. Three compressor tanks, complete with spray guns, are also located centrally between the two rows of horses. The cores

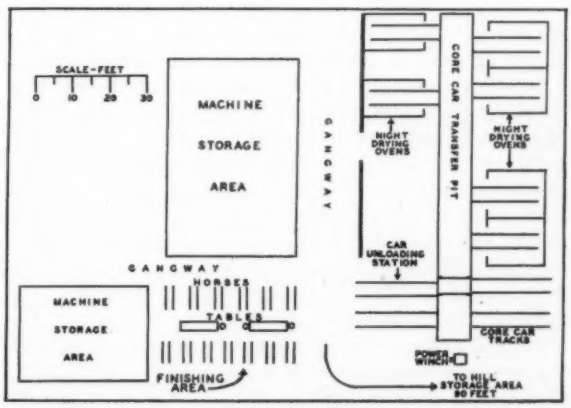


Fig. 1—Layout of Work Area.

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are finished by hand in this area, and after being finished are stored in the "Machine Storage Areas" or the "Hill Storage Area" (Fig. 1). An overhead crane services and is a part of this group.

At the present time this group consists of five core finishers, two core finishers helpers, and the overhead crane operator. One foreman is in direct charge of this group.

Work Analysis

A full car of dried cores is removed from the ovens and moved to the end of the core car transfer pit (Fig. 1) by use of the electrically powered cable winch. It is then pulled into position in the car unloading station by the overhead crane. Cores which can be handled manually are carried to the finishing area and those requiring crane handling are placed on the finishing horses by the overhead crane. As the car unloading process progresses, the core plates are unloaded and stacked beside the car, to be transported by the finishing crane at a later time to the coremaking area. These consist of angle reinforced steel plates, small and medium size cast iron plates with side lugs for handling, and large cast iron plates with bottom lugs for handling. Steel rails which form the lateral part of the "builds" on the core car are placed on the bottom rack of the car for re-use. "Core bricks," hollow cast iron blocks which form the vertical part of the builds on the core car, are placed in large trays to be transported at a later time to the coremaking department by other labor. When the core car is completely unloaded, it is moved directly across the transfer pit for reloading with green cores, or moved to the opposite end of the core car transfer pit where it will not interfere with further operations. The car removal and unloading goes on throughout the day as work progresses.

All finishing operations are performed in the finishing area. These operations consist of the preparation of the core so that it is suitable to be placed in the mold by the workers of the molding department. The finishing should be of sufficient quality to assure a smooth casting finish, free from unnecessary fins or other defects which may be caused by a poorly finished core. The cores are cleaned, sprayed with the proper type wash, and pasted or assembled where necessary. These and other operations will be described in detail in the element descriptions.

When the finishing operations are completed, the cores are ready to be placed in the storage areas. The Machine Storage Areas contain cores for green sand molds which are hand rammed or made on small molding machines. The "Hill Storage Area," as it is termed in this plant, contains cores for large dry sand molds which are hand rammed or made on a large molding machine. Storage operations are performed throughout the day to assure a steady flow of work through the work area.

At the end of the day's finishing operations the group mixes materials, cleans equipment, and sweeps the floor in preparation for the next day's operations.

Since the finishers, finishers helpers, and crane operator work as a group rather than on an individual basis, it can be readily realized that many of the finishers' and helpers' duties will, of necessity, overlap each other. This provides an advantage over a hard and fast demarcation of duties within the department in cutting the problem of inherent delay to a minimum. The work is so arranged that the crane operator acts as a part time helper when crane service is not required for a minimum period of approximately ten minutes.

Information from Other Records

Any plant considering the development and application of Standard Data Tables to core finishing will have in most cases a plan already in effect for coremaking. In the case of this plant, the Core Factor Card for coremaking provided certain information on nearly every core which would be processed through the finishing department. This information proved to be of the utmost value in the development of the Standard Data Tables. It provided:

1. The volume of the core by weight.
2. The projected area of the core, or the extreme dimensions of the care to indicate the plate size required for it.
3. The extreme height of the core as it rests on the core plate.
4. The number of sprue or pipe vents and special rod vents in the core.
5. The number of hooks in the core.
6. In the case of paste cores, the length of the groove or scratch vent on the paste surface of the core.
7. The presence of bedding sand on the surface of the core.
8. The type plate used for the core, whether steel or cast iron.
9. The classification of the core, either regular or irregular, used in the making operation.

If no such predetermined information is available, it is important that a list of such measurements as may be deemed necessary be prepared beforehand, and then recorded for each and every core on which time studies are taken.

Taking the Time Studies

A survey of the work procedure indicated that in practically no case did any individual worker perform all the finishing operations required on the core, but that the group operated as a team, with each worker doing a specific operation until other work presented itself. Under these conditions, the only satisfactory way to obtain the information required was by taking elemental time studies. A concentrated effort was made to obtain sufficient information on only a limited number of elements at one time, thereby making the studies more acceptable than if the entire scope of operations were studied concurrently. With the predetermined information available as mentioned above, the only information needed in the majority of the time studies was the pattern number and box number of the core. Other information required for certain elements studied was obtained by checking the core box equipment from which the cores were made.

Any leveling method which is satisfactory for tak-

	FLOOR FINISHING — ELEMENTS	SHEET NO. 2 — APRIL 8, 1951												
		1	2	3	4	5	6	7	8	9	10	%	100%	Allowance
1	SPRAY BLACKENING WASH													
2														
3	R. MOORE - 100%													
4	1248-1 8X1 48LB IRREG	.57	.52	.56									.55	
5	3038-22 8X16 220LB REG	.72	.79	.83									.78	
6	A3-17 8X2 27LB REG	.22	.19	.18	.17	.19							.19	
7	A3-17 8X1 44LB REG	.29	.31	.27	.32								.30	
8														
9														
10	J. BAKOS - 90%													
11	1256-1 8X2 16LB IRREG	.37	.40	.38	.36	.42							.35	
12														
13														
14	C. LEFEVER - 100%													
15	1502 8X2 28LB IRREG	.40	.42	.51	.37	.34	.35						.40	
16	1357-16 8X3 175LB IRREG	1.20	1.12	1.22									1.18	
17	2121-7 8X1 578LB IRREG	1.65	1.73	1.79									1.72	
18	2165-3 8X1 675LB IRREG	1.85	1.74										1.79	
19														
20														
21	H. SIGSBY - 100%													
22	2502-1 8X3 353LB REG	.84	.80										.82	
23	1431-1 8X1 136LB REG	.51	.53	.49	.54								.52	
24														
25	R. MOORE - 100%													
26	1200-1 8X9 88LB IRREG	.79	.68	.73	.74								.72	
27	121-225 8X6 36LB REG	.30	.25	.24	.27	.24							.26	
28	2165-1 8X30 32LB REG	.20	.18	.19	.23								.20	

Fig. 2—Time Study Sheet.

ing time studies is also satisfactory for developing Standard Data Tables. In this case, as only single elements were being considered in the time studies, the snap-back method of taking the studies was used. Also, since the studies were on single elements only, each element was speed rated and the average time for the same element on like cores was used. This method is illustrated in Fig. 2. This method of taking time studies, speed rating, and leveling was satisfactory for both the constant and variable elements. The constant elements are those which require the same amount of time, regardless of the size of the core being finished. The variable elements are those which depend upon the size and type of the core being finished, or upon the variations in area or length of any particular feature of the core used as a basis for measurement.

Due to the fact that the finishers and helpers duties overlapped each other to a certain extent, the conclusion was reached that any individual standard for finishing a core must reflect all the operations necessary to the core from removal from the oven to placing it in the proper storage area. It was also decided that the most desirable standard would correspond to the individual piece, whether it pasted or assembled with another core or was finished individually.

Comparison Sheet

One of the most important aspects of developing Standard Data Tables is gathering the information from the time studies and recording it in a systematic order for further use. This was accomplished by using a comparison sheet as shown in Fig. 3. This form, preferably prepared on a sheet of paper 11 x 17 in. or larger, is divided into two sections. The upper portion of the sheet contains the pattern number and box number of the cores on which time studies are taken, along with any necessary predetermined data as discussed in the section "Information from Other Records." The remaining, or lower portion of the sheet, contains a list of the elements of work encountered during the finishing operations. If the list of elements is too long to be placed on one sheet, as many comparison sheets as necessary may be used. The values are transferred from the time studies to the comparison sheet, with the values being corrected to 100 per cent speed rating, and the time study sheet number is noted in the space provided.

A comparison sheet is especially desirable from the standpoint of variable elements, and it can well be described as the intermediate step between the time studies and the charts derived from them.

During the time that the studies are made, the values should be transferred to the comparison sheet as soon as the information is available. In this way it can easily be determined when enough values have been recorded for each particular element, and if the range has been sufficiently covered. This also eliminates the waste of taking unnecessary time studies. When several values have been recorded, it is well to start plotting a chart of the values. This is a further indication of the coverage yet needed as well as any points which may require rechecking.

Relaxation Allowances

The values as plotted on the charts do not include relaxation allowances, therefore they must be added before the tables are developed. The relaxation allowances, which are personal and fatigue allowances combined, vary on the different elements of work. However, they are kept in proper relationship as to physical effort, duration, monotony, frequency, working conditions, etc., with each other and with other elements of work performed within the plant. Although the relaxation allowances may vary with those in other plants, it must be remembered that the bench mark of 100 per cent efficiency or 60 unit hours of work may also vary among different plants, thereby possibly requiring a higher or lower relaxation allowance. Figure 4 shows the relaxation allowances added to the various elements of work.

Core Classification

To cover the range of points in certain elements, it was found that the cores, for the purposes of cleaning, spraying core wash, and brushing, should be divided into two classes, regular and irregular.

PATTERN NUMBER	1502		1357-16		43-17		2165-3		121-225		1200-1A6	
BOX NUMBER	2		3		1		1		6		16	
CLASS (Per Finishing)	Irreg		Irreg		Reg		Irreg		Reg		Irreg	
WEIGHT, POUNDS	28		175		44		675		36		230	
PROJECTED AREA, SQ. INCHES	240		518		282		2752		107		960	
HEIGHT, INCHES	5 1/4		9		6 1/4		6 1/4		5 5/8		8 1/4	
TYPE PLATE	Steel		Steel		Steel		Cast Iron		Steel		Cast Iron	
LENGTH OF JOINT, PASTE CORE, INCHES	52		- - -		55		- - -		- - -		148	
CLASS OF JOINT, PASTE CORE	Reg		- - -		Reg		- - -		- - -		Irreg	
PASTE APPLIED, INCHES	55		- - -		58		- - -		- - -		240	
LENGTH OF VENT, PASTE CORE, INCHES	20		- - -		46		- - -		- - -		136	
MINUTES at 100% & STAR'S TIME STUDY NO.	100%	No.	100%	No.	100%	No.	100%	No.	100%	No.	100%	No.
CLEAN AND BLOW OFF	.33	3	.90	1	.29	3	1.53	3	.24	6	1.32	3
SPRAY SILICA WASH	- -		- -		.33	1	2.70	3	- -		1.61	3
SPRAY BLACKWASH	.40	2	1.10	2	.30	2	1.79	2	.26	2	1.26	4
BRUSH WITH WATER	.41	3	1.35	2	.21	2	1.92	3	.16	2	1.08	4
DRY WITH GAS TORCH	.73	3	1.35	3	.80	2	1.88	3	.46	3	- -	
CLEAN VENTS	.34	17	- -		.26	16	- -		- -		.60	20
RUB AND CALIPER, HAND	.45	17	- -		.77	17	- -		- -		- -	
APPLY PASTE	.23	17	- -		.32	17	- -		- -		.48	21
FIT TOGETHER, HAND	.65	16	- -		.76	17	- -		- -		- -	
FILL JOINT	.42	18	- -		.50	17	- -		- -		1.52	21
FILL JOINT	.85	18	- -		.93	17	- -		- -		1.58	21
RUB AND FIT, CRANE	- -		- -		- -		- -		- -		10.57	21
UNLOAD CORE, HAND	.45	3	- -		.51	3	- -		.46	8	- -	
UNLOAD CORE, CRANE	- -		1.48	3	- -		1.39	6	- -		1.65	4
TO MACHINE STORAGE, HAND	.51	40	- -		.58	36	- -		.38	35	- -	
TO MACHINE STORAGE, CRANE	- -		3.07	37	- -		- -		- -		- -	

Fig. 3—Comparison Sheet

10%	15%
SPRAY SILICA WASH	PASTE-IN CORES TYPE I
SPRAY BLACKENING WASH	PASTE-IN CORES TYPE II A&B
DRY WITH GAS TORCH	GET, AND POSITION BLOCKING
CLEAN VENTS	REPOSITION BY HAND
APPLY PASTE	HANDLE CORES BY CRANE
COVER GATE AND RISER OPENINGS	UNLOAD AND STACK CAST IRON PLATES BY CRANE
	TAKE DOWN VERTICAL BUILD CORE BRICKS
10% - 20%	
CARRY CORES BY HAND	15% - 25%
	UNLOAD AND STACK STEEL
10% - 25%	PLATES MANUALLY
RUB AND FIT BY HAND	
ASSEMBLE AND FIT BY HAND	20%
	PASTE-IN CORES TYPE II C & D
15%	REPOSITION BY CRANE
	RUB CORES IN JIGS
CLEAN AND BLOW OFF	TAKE DOWN LATERAL BUILD
BRUSH CORES WITH WATER	STEEL RAILS
FILE FILLETS	MOVE CORE TRANSFER CAR
BRUSH CHILLS	
REMOVE BEDDING SAND	
CLEAN HOOK HOLES	25%
CLEAN PIPE VENTS	
TIE WIRES	RUB AND FIT BY CRANE
FILE JOINTS	
FILL JOINTS	

Fig. 4—Relaxation Allowances.

Regular—A core which is relatively simple, with no deep pockets, overhanging edges, or high projections, and which presents no special difficulty in cleaning, spraying, or brushing.

Irregular—Cores which present difficulties in cleaning, spraying, and brushing due to deep pockets in the core, thin ribs, overhanging edges, high projections, or narrow deep openings to form ribs in a casting. An irregular core requires much more physical movement on the part of the operator to complete the operations.

Certain individual elements also require different classifications, which will be indicated in the element descriptions.

Finishing Elements

The elements of work which are performed in the finishing area and which concern the actual finishing of the core are of primary concern. Each element was plotted on a chart and the resulting curve was used to arrive at the proper values. As approximately 25 charts were plotted to obtain all the values necessary, only certain typical charts will be illustrated to show the range of points and values on the chart.

Clean and Blow Off—The element "Clean and Blow Off" is calculated on the weight and classification of the core and is plotted in Fig. 5. The operation consists of cleaning the core in preparation for the finish coat. A file is used to smooth rough spots and edges on the core, and the loose sand and any other foreign material which may be on the core is blown off with an air hose. A constant value of 0.07 min was allowed to compensate the operator for getting and replacing the equipment. The curves on the chart represent the values at 100 per cent, and to this must be added the relaxation allowance of 15 per cent plus an additional allowance of 10 per cent to compensate for minor patching or repair which may be necessary. The values may be read directly from the chart in making the tables. However, it proves quite satisfactory to prepare the table values by breaking the curve, when it is actually a curved line, into short segments and calculating the values in each segment separately. In this way, the tables may be broken into increments as small as may be desired. The calculation is done in the following manner, using the "regular" curve in Fig. 5 as an example.

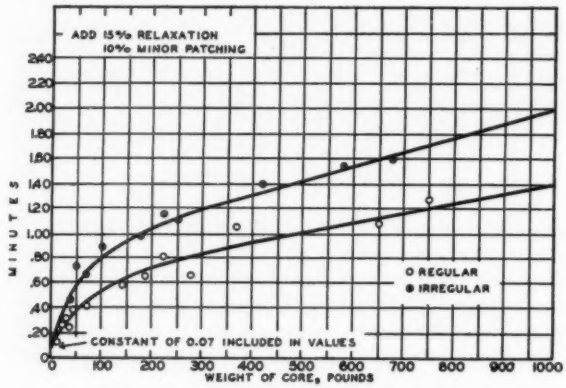


Fig. 5—Curve for Clean and Blow off Core.

250 lb—0.786 min plus 25% = 0.9825 min of allowed time

300 lb—0.846 min plus 25% = 1.0575 min of allowed time

If	300 lb	= 1.0575 min
	250 lb	= 0.9825 min
Then	50 lb	= 0.0750 min
and	1 lb	= 0.0015 min

Starting the calculation with 0.9825 min for 250 lb and adding 0.0015 min for each additional pound, all the values falling on the segment of the curve from 250 lb to 300 lb can be calculated with the increment of advance being as great or small as may be required. When the curve as plotted on the chart is a straight line, or becomes a straight line, the values derived from that type of curve may be made with only one calculation as shown above.

Spray Silica—This element is also calculated on the weight and classification of the core. The operation consists of spraying the surface of the core with silica wash, a ceramic type wash which is thinned with water to form a mixture suitable for spraying. This element carries a constant of 0.07 min to allow the operator time to get and replace the spray gun. In addition to the 10 per cent relaxation allowance, another 5 per cent is allowed to compensate for equipment delay, such as plugged spray guns, etc.

Spray Blackening—This element consists of spraying the surface of the core with blackening wash, a plumbago base wash which is mixed with water to form a consistency suitable for spraying. Like the above element, it is plotted against the weight and

classification of the core and carries the same constant and relaxation allowance plus the 5 per cent added allowance for equipment delay.

Brush Cores with Water— In order to assure a smoothly finished core without runs, heavy spots, or lightly covered spots on the core, it is necessary for each core to be brushed with a camels hair brush over the final finish coat. The brush is dipped in water frequently to carry out excess core wash which may be picked up in the process. This element is also plotted by using the weight and classification of the core as the variable factors. A constant of 0.07 min is allowed for getting and replacing the equipment and a relaxation allowance of 15 per cent is added to the basic chart values.

Dry Cores with Gas Torch—After the final finish coat is applied and the core is brushed, it is necessary to dry the finish coat. This is accomplished with the use of a pressure gas torch to produce the required degree of heat. As can be seen from the chart in Fig. 6, the curve advances very rapidly, then begins to level off at about 150 lb, and thereafter levels rapidly. The reason for this is that the lighter cores cool quite rapidly after being removed from the oven and therefore require proportionately more torch drying. The larger cores retain enough heat to dry nearly all the finish coat from the internal heat and only portions of them require drying by torch. This element is plotted by the weight of the core and carries a 10 per cent relaxation allowance.

Paste-In Cores— In order to obtain the desired contour and shape of the finished core, it is often necessary to paste small cores into the large, or main core.

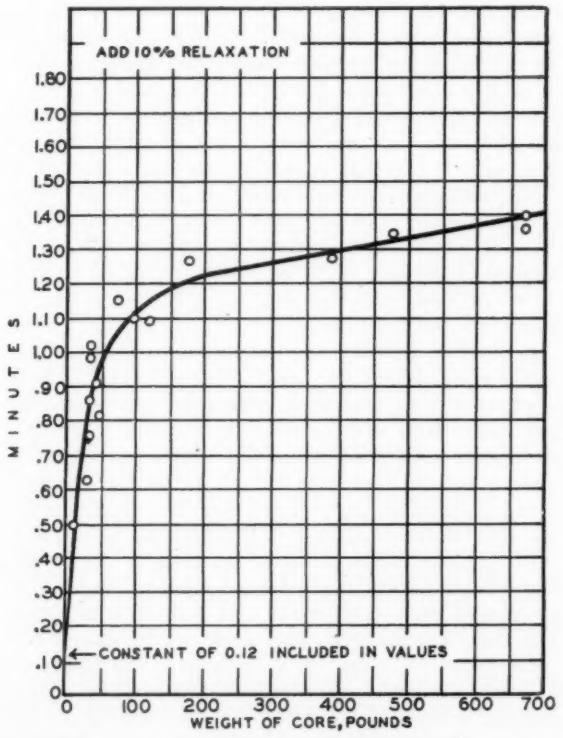


Fig. 6—Curve for Dry Cores with Gas Torch.

These small cores are termed "paste-in cores" and are received from the bench finishing department with the finish coat applied. Paste-in cores have been divided into two types, I and II.

Type I—Require getting the cores from the designated storage space within the finishing area, filing the paste-in core to fit the main core, applying paste, and placing it in the main core.

Type II—Require, in addition to the above, finishing the joint which is formed by the paste-in core and main core.

Time studies were taken on paste-in cores to cover the entire operation for each type, and it was found after plotting the values on a chart that these cores could be classified according to the weight of the paste-in core. These classes are "A," to 1 lb; "B," 1 to 5 lb; "C," 5 to 10 lb; and "D," over 10 lb. A 15 per cent relaxation allowance was added to the chart values for all Type I and Type II A and B. Type II C and D carry a 20 per cent relaxation allowance to compensate for the extra difficulty in handling a paste-in core of this type and size. The values for these cores are shown on the table in Fig. 12.

File Fillets—When the strike off surface, or plate side of the core forms a metal section of the casting, a fillet is usually indicated by markings on the pattern equipment. This consists of rounding off the sharp corners of the core with a file. This element is determined by the length of the fillet to be filed. A constant of 0.05 min is allowed the operator for getting and replacing the file, and the element carries a relaxation allowance of 15 per cent.

Bedding Sand—Certain sections of various cores require, in the green state, the support of sharp, or unbonded, sand to support them until the core has been baked. Before applying the finish coat, this bedding sand must be thoroughly removed from the surface. A small rubbing stone, file, or a coarse grade of sandpaper may be used to accomplish this operation. When the points were placed on the chart, it was found that two classifications were needed to cover the range of points. These classes were indicated as regular and irregular, depending upon the contour of the core where the bedding sand was placed.

Regular—A flat or nearly flat surface which is easily cleaned with a file or rubbing stone.

Irregular— A contoured surface requiring the careful use of the equipment to thoroughly clean the sand from the core. A cavity formed by a block type loose piece and having four or more surfaces in the core is also considered irregular.

A constant of 0.07 min was allowed for the operator to get and replace the equipment. This element carries a relaxation allowance of 15 per cent.

Hooks—This value represents the allowed time for cleaning out hook holes in the core to assure proper space for core setting hooks in the molding department. The value per hook is a constant and is allowed 15 per cent relaxation.

Pipe Vents—Pipe, or sprue vents in the core must be open to assure ample space for escaping gasses when metal is poured into the mold. The value represents inspecting and cleaning out these vents, if

necessary. The value per vent is a constant and is allowed 15 per cent relaxation.

Tie Wires, Tie-In Cores—Certain paste-in cores require in addition to the paste the support of tie wires to hold them in the main core. The value represents the allowed time for cutting and tying the wire. The value per wire is a constant and carries a relaxation allowance of 15 per cent.

Brush Chills—Chills placed in the core during the making operation require a specially prepared coating to prevent them from adhering to the casting surface. This preparation is brushed on, and the value is dependent upon the square inches of area to be brushed. As the number of coats required may vary, the table values represent the brushing of one coat only. A constant of 0.05 min is allowed the operator to get and replace the material and equipment, and the relaxation allowance for this element is 15 pct.

Reposition—The repositioning element occurs when it is necessary to the finishing operation. This is done either by hand for cores weighing to 200 lb, or by crane for cores weighing over 200 lb. A relaxation allowance of 15 per cent is included in the value for cores repositioned by hand. The crane repositioning time is a constant value including the crane operators and two hook-up men's time. An overall relaxation allowance of 20 per cent is given the crane repositioning element, which compensates for the extra physical effort required of the hook-up men over ordinary crane lifts.

Blocking—This value determines the blocking or wedging of the core, when required, after repositioning. It is treated as a constant and includes time for getting the blocking or wedging material from the designated storage space within the department and positioning the material. A relaxation allowance of 15 per cent is applied to this element.

Additional Elements in Pasting Operations

The foregoing elements as described in the previous paragraphs cover the actual finishing operations which may occur on any core which does not paste or assemble with another. Additional operations which are performed in the finishing area concern paste or assembly cores. In the cases of cores which paste together, the parts are referred to as the cope and drag halves, the cope being the upper half and the drag being the lower half of the pasted core. Three or more cores which paste together are referred to as assembly cores.

Clean Vents—To assure ample space for escaping gasses, the groove or scratch vents which are placed in the strike off surface of the core during the making operation must be cleaned of loose sand or any material which may obstruct them. The value is determined by the length of the vent to be cleaned. A constant of 0.05 min is allowed for getting and replacing the equipment, and the relaxation allowance of 10 per cent is added to the values from the chart.

Rub and Fit Cores Together (Hand)—In order to assure a good surface for pasting, the cope and drag halves of the core must be rubbed together. Calipering the core is also necessary to make sure that the pasted core will have the correct dimensions. Cores weighing to 200 lb are rubbed by hand and the values which are used in the tables are a combination of the values taken from two charts. The first value concerns setting the cope half on the drag, rubbing, and calipering. As the weight of the core was found to be the governing variable characteristic, the points were so plotted on the chart, and a relaxation allowance was added which progressed from 10 per cent at 10 lb to 25 per cent at 200 lb.

The second value consists of setting the cope half off the drag, blowing off the excess loose sand, and then after applying paste to the drag half of the core, resetting the cope half and fitting together, making sure that the joints of the core match together. This value is also dependent on the weight of the core, and the relaxation allowance added was from 10 per cent at 10 lb to 25 per cent at 200 lb. The two values contain a constant of 0.15 min to compensate the operators for getting and replacing the equipment necessary to the operations. Since this operation pertains to both the cope and drag halves of the core, it was felt that the time allowed should be divided equally between the halves. The values as used in the tables are one half of the total values plus relaxation allowances as taken from the charts. As the charts are plotted against the weight of the core handled, which is the cope half, that weight is the governing factor for this value. The value as taken from the table is applied to both the cope and drag halves of the core; for example, a drag half weighing 70 lb pastes with a cope half weighing 50 lb. The weight of the cope half being the governing factor, the value for 50 lb is applied to both cope and drag half cores.

Rub and Fit (Crane)—Cores which rub and fit together and weigh over 200 lb require the service of the overhead crane to accomplish the operation. The value is a constant and includes the crane operators time and the time of four men to hook up, level, and rub the core. In this operation the weight of the cope half is partially supported by the crane. The value includes hook up, level, rub, lift cope, blow off excess sand, reset cope and check for fit, repeat if necessary, unhook, and crane away. An overall relaxation allowance of 25 per cent is applied to this value, with one half the total value being applied to both cope and drag halves.

Jigs—Core rubbing jigs are a part of the core finishing equipment furnished for certain cores. These cores, both cope and drag halves, are placed in the jigs and rubbed with a steel rubbing bar. The cores are then removed from the jigs and fitted together. Cores weighing to 200 lb are placed in the jigs and removed by hand, and heavier cores are placed in the jigs and removed by crane. Relaxation allowances of 10 per cent at 10 lb to 25 per cent at 200 lb are allowed for handling those cores, and an overall relaxation allowance of 15 per cent is allowed for crane handling, which reflects the crane operators and two hook up men's time. A 20 per cent relaxation allowance is added to the rubbing time. The values include a constant of 0.95 min to compensate the operators for getting and replacing the equipment, including the jigs necessary to this operation.

Assemble and Fit—When three or more cores paste together, they are termed "assembly cores," and each piece must be rubbed and fitted separately. The operation, apart from this, is essentially the same as for cores which rub and fit together by hand. The value is determined by the weight of the core and a relaxation allowance from 10 per cent at 10 lb to 25 per cent at 200 lb is added for this factor.

Apply Paste—Paste is applied to only the drag half of paste cores. The paste, a preparation with a glucose base, is applied with a paste bulb in a continuous strip approximately ¼-⅜ in. in width. The value is determined by the number of lineal inches of paste applied. A constant of 0.15 min is allowed for getting and replacing the paste bulb and starting the flow of paste from the bulb. A relaxation allowance of 10 per cent is added to the values from the chart as shown in Fig. 7. An additional allowance of 10 per cent is also made to compensate the operator for refilling the paste bulb as required throughout the day.

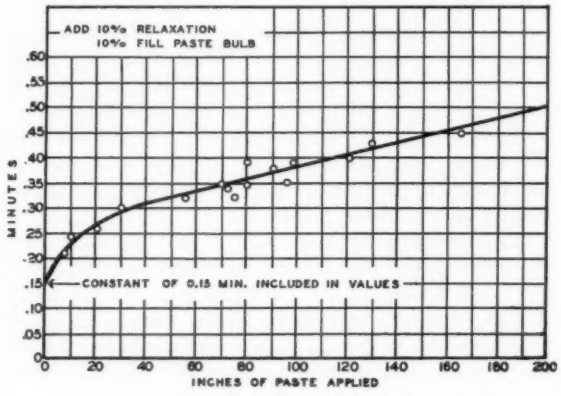


Fig. 7—Curve for Apply Paste.

Finish Joints—After the cores are pasted together, the joints formed must be filled to prevent penetration of metal into them. The preparation used is a heavy mixture of plumbago and water. The joints of paste cores are classed as regular and irregular to cover the range of points on the charts for filing and filling only.

Regular—Joints which are straight or nearly straight and not broken by sharp curves or angles.

Irregular—Joints which are broken by narrow openings, angles, sharp curves, or projections which present difficulty in filing and filling.

This value is a combination of five elements.

Element No. 1—File, is the first operation, and consists of filing the joint to remove any excess sand and assure a smooth joint. A constant value of 0.05 min is allowed the operator to get and replace the equipment. A relaxation allowance of 15 per cent is added to this element.

Element No. 2—Fill, is the filling of the joint with the plumbago mix to prevent metal penetration. A constant value of 0.10 min is allowed for the operator to get and replace this material, and a 15 per cent relaxation allowance is added to the chart values. An added allowance of 5 per cent is given to compensate for kneading and conditioning the filler in preparation to applying it to the joint.

Element No. 3—Spray, is the spraying of the joint with blackening wash. Relaxation and equipment delay allowances are the same as for the element "Spray Blackening."

Element No. 4—Brush, is the brushing of the joint with water. The relaxation allowance is the same as for the element "Brush Cores With Water."

Element No. 5—Dry, is the drying of the joint with a pressure gas torch. The relaxation allowance is the same as for the element "Dry Cores With Gas Torch."

The values are determined by the length and class of the joint finished. Elements No. 3, 4, and 5 contain no constants, as they are continued from other elements already carrying the constants. The joints of cores which rub and fit together but do not paste are filed only. On certain other cores the joints are filed and filled only, while on the remainder the joints are completely finished. The tables indicate each of these operations. The table values are one half of the chart values plus relaxation and other allowances, with the cope and drag halves each receiving the value as shown on the table.

Miscellaneous Elements—Other miscellaneous elements concerning paste or assembly cores occur only occasionally and can be summed up briefly. These include "Cover Gate and Riser Openings," which concerns assembled core molds. The openings are covered with paper, which is pasted in place to prevent foreign materials from entering the assembly during storage. This element is given a relaxation allowance of 10 per cent and the value is determined by the number of openings covered.

"Tie Wire, Bearing Ends" determines getting, cutting, and tying wire around the bearing ends of certain paste cores to prevent them from separating or warping apart after cooling. A relaxation allowance of 15 per cent is added for this element, the value being determined by the number of wires cut and tied. Both cope and drag halves of the core receive equal values as shown on the table.

"Tie Wires, Hook Ends" is essentially the same operation except that the wire is tied through adjoining hooks in the paste cores, the hooks being placed in the core during the making operation.

"Tie Wires, Assembly Cores" determines the getting, cutting, and tying of wire around core assemblies to prevent them from separating after cooling. The value is determined by the number of wires cut and tied. A 15 per cent relaxation allowance is added to this element, and the value as shown on the tables must be pro-rated to the cores around which the wire is tied.

Handling Factors

The handling factors concern getting the cores from the night drying ovens to the finishing area, disposing of core plates, builds, etc., and removing the completely finished cores from the finishing area to storage areas. The equipment used in this plant and the methods employed will vary from those employed in other plants; therefore, these items as dis-

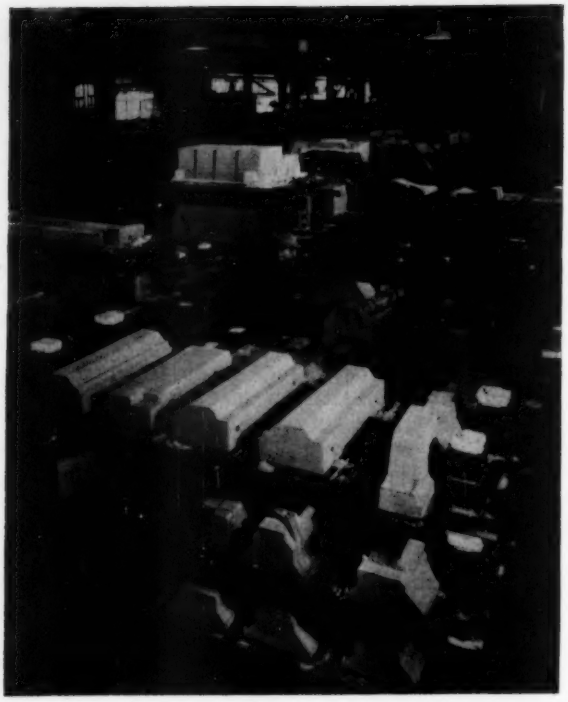


Fig. 8—Core cars being loaded with green cores. The method of making builds with core bricks and steel rails is clearly shown, along with various types of core plates. The cores shown are typical of those being finished daily by the Core Finishing Dept. These cars are 13½ ft long and 7 ft wide, and builds may be made to a height of 5 ft, 4 in.

cussed in this paper show the approach and solution to the problem in this plant only. As previously stated, the complete finishing standard must reflect these handling operations as well as the finishing operations already described. The factors arrived at to cover the handling are described with an explanation of the various values from which they were consolidated. A general description of these values is considered by the writer to be sufficient in covering these values.

Car and Plate Value—This value represents the removal of the loaded core car from the night drying ovens, bringing the car into position to unload, and the disposal of the core plates, builds (rails and bricks), and the unloaded core car. Figure 8 shows the method of loading the core car.

A combination of the following values, all of which are dependent upon the projected area, height, or both, of any given core completes the value. Two tables, one for cores baked on steel plates and one for cores baked on cast iron plates are built from these values.

1. The total man minutes, including the relaxation allowances, were determined to cover the operation of removing the loaded core car from the oven, positioning to unload, and removing the car after it was unloaded. The value was pro-rated according to the number of cores of any given projected area and height which could be loaded on a core car with 162 by 84 by 64 in. of useful building volume, taking into consideration the heights of the plates, rails, and bricks included in the loading build. This value then covers all car handling required.

2. The value per plate for unloading and stacking steel and cast iron plates of the various sizes encountered was determined. These values covered both manual and crane unloading. The values for transporting these stacked steel or cast iron plates was prorated according to the average number of each size and type which are transported at one time. By converting the dimensions of the plates to area, the allowed time, including relaxation allowances, for any given steel or cast iron plate was calculated. It was found that the average core required a plate with an area approximately 50 per cent greater than the projected area of the core, and this basis was used to determine the area of the plate required. (This type plate used is a part of the predetermined information mentioned earlier). This value then covers all plate handling, and included is an extra allowance for handling core driers, whenever necessary.

3. The value for removing the rails from the builds and placing them on the bottom rack of the car was pro-rated according to the size of the plate required.

4. The value for removing the core bricks from the builds and placing them in trays was prorated according to the size of the plate required and the height of the core.

Cores to Finishing Area—Cores are unloaded from the car either manually or by crane and placed on the steel horses in the finishing area. Cores weighing to 75 lb are carried by hand, with a relaxation allowance of 10 per cent at 15 lb to 20 per cent at 75 lb added to the values from the chart. The crane unloading is a constant value for cores weighing over 75 lbs. The craneman's time and hook up man's time are included in this value, with a relaxation allowance of 15 per cent added.

From Finishing Area to Storage—Cores are stored in either the "Machine Storage Areas" or the "Hill Storage Area" according to the location of the molding center where the cores are used.

Cores stored in the Machine Storage Areas weighing to 75 lb are carried by hand, with a relaxation allowance of 10 per cent at 15 lb to 20 per cent at 75 lb added to the values from the chart. Cores weighing over 75 lb are stored by crane, the value being a constant and including the crane operator's and two hook up men's time. A relaxation allowance of 15 per cent is added to this value.

Cores stored in the Hill Storage Area are loaded on pallets which are then moved by hand into the adjoining bay. The crane in this bay transports the loaded pallets to the storage area, where the cores remain on the pallets until used by the molding department. The total time required, plus allowances, was calculated for the handling of one pallet. This value was then pro-rated according to the average number of cores stored per pallet by weight. The cores are loaded on the pallets either by hand or by crane, and these values were calculated in the same

manner as the handling times previously described. A combination of these two values as applied to the weight of the core determines the total value for this factor.

In both the Machine Storage Areas and the Hill Storage Area there are cores which are stored after pasting or assembling. In these cases, the total pasted or assembled weight is used to determine the method of storage, and that value is pro-rated to the halves of the pasted core or parts of the assembled core.

Rebaking—Certain cores, after being partially finished, must be reloaded on the core car for rebaking to assure a thoroughly dried core. The value is a constant as all such cores require crane handling. The value includes the crane operators and two hook-up men's times and carries a relaxation allowance of 15 per cent. Cores which are rebaked go through the "Car and Plate" and "To Horses, Floor" cycles twice, and therefore receive double values for these factors.

Roll Over for Crane Handling to Storage—Certain cores, to be stored properly with the crane, must be rolled over either by hand for cores weighing to 200 lb or by crane for cores weighing over 200 lb. These values are determined in the same manner as for the element "Reposition."

Blocking in Storage—To prevent damage to the finished cores which are stacked in the storage areas, blocking or wedging is required in some cases. The value for this element was determined in the same manner as that for "Blocking after Repositioning."

Set-Up Time—A study was taken of the time required to prepare for the next day's operations, and it was found that it was necessary to allow 3.00 manhours at the end of each day for this work, which is termed "Daily Set-up Time." It includes sweeping the car unloading station and the finishing area, mixing core paste, joint filler, silica and blackening washes, and cleaning the spray equipment.

100 Per cent Coverage—A requirement of the plan is complete standard coverage on all cores finished. This is necessary because the men work as a group rather than individually, thereby making it impossible to separate any off standard time for the complete finishing operation of any particular core. The only non-incentive time possible for any individual of the group is an unavoidable delay beyond the worker's control, or for extensive repair work on damaged cores. In any case, the non-incentive time requires the approval of the foreman of the group.

Recording the Finished Cores—The cores produced by the Floor Coremaking group each day are processed through the finishing department on the following day. The foreman of the finishing group is furnished a report showing the pattern number, box number, and number of pieces made from each particular box which will be processed through the department. This report is completed by the foreman to show the pieces finished and is then forwarded to the Standards and Payroll Department for calculation of performance and premium.

Table Arrangement—The tables as shown in Figs. 9 to 17 are arranged conveniently with the elements of work which are dependent upon certain variables grouped together. The tables in Figs. 9, 10, 11, 12, and 13 cover all finishing operations which may be

Sterling Foundry Company — Sheet 1 of 9
Department: Core — Standard Data
Operation: Floor Finishing — August 1951

CLEAN AND BLOW OFF		SPRAY SILICA (per coat)		WEIGHT OF CORE	SPRAY BLACKENING		BRUSH		DRY W/TORCH
Reg.	Irreg.	Reg.	Irreg.		Reg.	Irreg.	Reg.	Irreg.	
.21	.26	.21	.34	10	.18	.33	.15	.28	.61
.25	.34	.25	.42	15	.21	.41	.19	.35	.72
.28	.42	.29	.49	20	.24	.48	.22	.43	.82
.32	.50	.33	.56	25	.28	.56	.26	.51	.91
.35	.55	.37	.61	30	.30	.60	.28	.57	.95
.38	.60	.40	.65	35	.33	.63	.31	.63	.98
.41	.65	.43	.71	40	.35	.67	.34	.70	1.02
.44	.70	.46	.76	45	.38	.71	.37	.76	1.05
.47	.75	.49	.82	50	.40	.75	.40	.83	1.08
.49	.78	.52	.86	55	.42	.77	.42	.88	1.10
.51	.81	.54	.91	60	.44	.90	.44	.93	1.12
.53	.84	.56	.96	65	.46	.83	.47	.92	1.14
.55	.88	.59	1.01	70	.49	.85	.49	1.04	1.16
.57	.91	.61	1.05	75	.51	.88	.51	1.09	1.18
.59	.93	.63	1.09	80	.52	.90	.53	1.13	1.19
.61	.95	.66	1.13	85	.54	.92	.55	1.19	1.21
.62	.97	.68	1.17	90	.55	.94	.57	1.21	1.22
.64	1.00	.70	1.21	95	.57	.96	.59	1.24	1.23
.66	1.02	.73	1.25	100	.59	.98	.61	1.28	1.24
.68	1.05	.76	1.32	110	.62	1.02	.65	1.33	1.26
.71	1.09	.80	1.39	120	.65	1.05	.69	1.39	1.27
.74	1.12	.84	1.45	130	.69	1.09	.72	1.43	1.29
.76	1.15	.88	1.51	140	.70	1.12	.76	1.47	1.30
.79	1.19	.92	1.56	150	.73	1.16	.80	1.51	1.31
.81	1.21	.95	1.61	160	.76	1.19	.83	1.54	1.32
.83	1.24	.98	1.65	170	.78	1.22	.86	1.57	1.33
.85	1.26	1.02	1.69	180	.80	1.24	.90	1.60	1.33
.87	1.29	1.05	1.73	190	.83	1.27	.93	1.63	1.34
.89	1.31	1.08	1.77	200	.85	1.30	.96	1.67	1.35
.91	1.33	1.11	1.80	210	.87	1.33	.99	1.69	
.93	1.35	1.14	1.83	220	.89	1.35	1.02	1.71	1.36
.95	1.37	1.17	1.85	230	.91	1.38	1.05	1.74	
.97	1.39	1.20	1.88	240	.93	1.41	1.08	1.76	
.98	1.41	1.23	1.90	250	.95	1.43	1.10	1.78	1.37
1.00	1.43	1.25	1.93	260	.97	1.45	1.13	1.80	
1.01	1.45	1.28	1.95	270	.99	1.48	1.15	1.82	1.38
1.03	1.47	1.30	1.97	280	1.01	1.55	1.18	1.84	
1.04	1.48	1.33	1.99	290	1.03	1.52	1.20	1.87	
1.06	1.50	1.36	2.01	300	1.04	1.54	1.23	1.89	1.39
1.07	1.51	1.38	2.03	310	1.06	1.56	1.25	1.90	
1.08	1.53	1.41	2.05	320	1.07	1.58	1.27	1.92	1.40
1.09	1.54	1.43	2.07	330	1.09	1.60	1.30	1.94	
1.11	1.56	1.46	2.09	340	1.10	1.62	1.32	1.95	
1.12	1.57	1.48	2.11	350	1.12	1.64	1.34	1.97	1.41
1.13	1.59	1.51	2.13	360	1.13	1.66	1.36	1.99	
1.14	1.60	1.53	2.15	370	1.15	1.68	1.38	2.00	
1.15	1.61	1.55	2.17	380	1.16	1.69	1.41	2.02	1.42
1.16	1.63	1.57	2.19	390	1.17	1.71	1.43	2.03	
1.17	1.64	1.59	2.21	400	1.19	1.73	1.45	2.05	1.43

Fig. 9—Floor finishing operation data in core department.

Sterling Foundry Company — Sheet 2 of 9
Department: Core — Standard Data
Operation: Floor Finishing — August 1951

CLEAN AND BLOW OFF		SPRAY SILICA (per coat)		WEIGHT OF CORE	SPRAY BLACKENING		BRUSH		DRY W/TORCH
Reg.	Irreg.	Reg.	Irreg.		Reg.	Irreg.	Reg.	Irreg.	
1.18	1.66	1.61	2.23	410	1.20	1.74	1.47	2.06	
1.19	1.67	1.63	2.25	420	1.21	1.76	1.49	2.08	1.44
1.20	1.69	1.65	2.27	430	1.22	1.77	1.50	2.10	
1.21	1.70	1.67	2.29	440	1.23	1.79	1.52	2.11	
1.22	1.71	1.69	2.31	450	1.24	1.80	1.54	2.13	1.45
1.23	1.73	1.71	2.33	460	1.25	1.82	1.56	2.14	
1.24	1.74	1.72	2.35	470	1.26	1.83	1.58	2.16	
1.24	1.76	1.74	2.37	480	1.27	1.85	1.60	2.17	1.46
1.25	1.77	1.75	2.39	490	1.28	1.86	1.61	2.19	
1.26	1.79	1.77	2.41	500	1.29	1.88	1.63	2.21	
1.27	1.80	1.78	2.43	510	1.31	1.89	1.65	2.22	1.47
1.28	1.81	1.80	2.45	520	1.32	1.91	1.66	2.24	
1.29	1.83	1.81	2.47	530	1.33	1.93	1.67	2.25	1.48
1.30	1.84	1.83	2.49	540	1.34	1.94	1.69	2.27	
1.31	1.86	1.84	2.51	550	1.35	1.96	1.70	2.28	1.49
1.32	1.87	1.86	2.52	560	1.36	1.97	1.71	2.30	
1.33	1.89	1.87	2.54	570	1.37	1.99	1.73	2.32	1.50
1.34	1.90	1.89	2.56	580	1.38	2.00	1.74	2.33	
1.35	1.91	1.90	2.58	590	1.39	2.02	1.75	2.35	
1.36	1.93	1.92	2.60	600	1.40	2.03	1.77	2.36	1.51
1.37	1.94	1.93	2.62	610	1.41	2.05	1.78	2.38	
1.38	1.96	1.95	2.64	620	1.43	2.06	1.79	2.39	1.52
1.39	1.97	1.96	2.66	630	1.44	2.08	1.81	2.41	
1.40	1.99	1.98	2.68	640	1.45	2.10	1.82	2.43	
1.41	2.00	2.00	2.70	650	1.46	2.11	1.83	2.44	1.53
1.42	2.01	2.01	2.72	660	1.47	2.13	1.85	2.46	
1.43	2.03	2.03	2.74	670	1.48	2.14	1.86	2.47	1.54
1.44	2.04	2.04	2.76	680	1.49	2.16	1.87	2.49	
1.45	2.06	2.06	2.78	690	1.50	2.17	1.89	2.50	
1.46	2.07	2.07	2.80	700	1.51	2.19	1.90	2.52	1.55
1.47	2.09	2.09	2.82	710	1.52	2.20	1.91	2.54	
1.48	2.10	2.10	2.84	720	1.53	2.22	1.93	2.55	
1.49	2.11	2.12	2.86	730	1.55	2.23	1.94	2.57	1.56
1.50	2.13	2.13	2.88	740	1.56	2.25	1.95	2.58	
1.51	2.14	2.15	2.90	750	1.57	2.26	1.97	2.60	1.57
1.52	2.16	2.16	2.92	760	1.58	2.28	1.98	2.61	
1.53	2.17	2.18	2.94	770	1.59	2.30	1.99	2.63	1.58
1.54	2.19	2.19	2.96	780	1.60	2.31	2.01	2.65	
1.55	2.20	2.21	2.98	790	1.61	2.33	2.02	2.66	
1.56	2.21	2.22	3.00	800	1.62	2.34	2.03	2.68	1.59
1.57	2.23	2.24	3.02	810	1.63	2.36	2.05	2.69	
1.58	2.24	2.25	3.04	820	1.64	2.37	2.06	2.71	1.60
1.58	2.26	2.27	3.06	830	1.65	2.39	2.07	2.72	
1.59	2.27	2.29	3.08	840	1.67	2.40	2.09	2.74	
1.60	2.29	2.30	3.10	850	1.68	2.42	2.10	2.76	1.62
1.61	2.30	2.32	3.12	860	1.69	2.43	2.11	2.77	
1.62	2.31	2.33	3.14	870	1.70	2.45	2.13	2.79	1.63
1.63	2.33	2.35	3.16	880	1.71	2.47	2.14	2.80	
1.64	2.34	2.36	3.18	890	1.72	2.48	2.15	2.82	

Fig. 10—Addition floor finishing operation data.

Sterling Foundry Company
Department: Core
Operation : Floor Finishing

Sheet 3 of 9
Standard Data
August 1951

CLEAN AND BLOW OFF		SPRAY SILICA (per cent)		WEIGHT OF CORE	SPRAY BLACKENING		BRUSH		DRY W/TORCH
Reg	Irreg	Reg	Irreg		Reg	Irreg	Reg	Irreg	
1.65	2.36	2.38	3.19	900	1.73	2.50	2.17	2.83	1.64
1.66	2.37	2.39	3.21	910	1.74	2.51	2.18	2.85	
1.67	2.39	2.41	3.23	920	1.75	2.53	2.19	2.86	1.65
1.68	2.40	2.42	3.25	930	1.76	2.54	2.21	2.88	
1.69	2.41	2.44	3.27	940	1.77	2.56	2.22	2.90	
1.70	2.42	2.45	3.28	950	1.79	2.57	2.23	2.91	1.66
1.71	2.44	2.47	3.31	960	1.80	2.59	2.25	2.93	
1.72	2.46	2.48	3.33	970	1.81	2.60	2.26	2.94	1.67
1.73	2.47	2.50	3.35	980	1.82	2.62	2.27	2.96	
1.74	2.49	2.51	3.37	990	1.83	2.63	2.29	2.97	
1.75	2.50	2.53	3.39	1000	1.84	2.65	2.30	2.99	1.68
1.77	2.54	2.57	3.44	1025	1.87	2.69	2.33	3.03	1.69
1.80	2.57	2.61	3.49	1050	1.89	2.73	2.37	3.07	1.70
1.82	2.61	2.64	3.54	1075	1.92	2.77	2.40	3.11	1.71
1.85	2.64	2.68	3.59	1100	1.95	2.80	2.43	3.15	1.72
1.87	2.68	2.72	3.64	1125	1.98	2.84	2.47	3.19	1.73
1.90	2.71	2.76	3.69	1150	2.00	2.88	2.50	3.23	1.74
1.92	2.75	2.80	3.74	1175	2.03	2.92	2.53	3.26	1.75
1.94	2.79	2.84	3.79	1200	2.06	2.96	2.57	3.30	1.76
1.97	2.82	2.87	3.84	1225	2.09	3.00	2.60	3.34	1.77
1.99	2.86	2.91	3.88	1250	2.11	3.04	2.63	3.38	1.78
2.02	2.89	2.95	3.93	1275	2.14	3.07	2.67	3.42	1.79
2.04	2.93	2.99	3.98	1300	2.17	3.11	2.70	3.46	1.80
2.07	2.96	3.03	4.03	1325	2.19	3.15	2.73	3.50	1.81
2.09	3.00	3.06	4.08	1350	2.22	3.19	2.77	3.54	1.82
2.11	3.04	3.10	4.13	1375	2.25	3.23	2.80	3.58	1.83
2.14	3.07	3.14	4.18	1400	2.28	3.27	2.83	3.62	1.84
2.16	3.11	3.18	4.23	1425	2.30	3.31	2.87	3.66	1.85
2.19	3.14	3.22	4.28	1450	2.33	3.34	2.90	3.70	1.86
2.21	3.18	3.26	4.33	1475	2.36	3.38	2.93	3.74	1.87
2.24	3.21	3.29	4.38	1500	2.39	3.42	2.97	3.78	1.89

EXTENSION OF TABLE

Spray Silica, Regular	Extend at	.001527	per lb.
Spray Silica, Irregular	" "	.001971	" "
Spray Blackening, Regular	" "	.001090	" "
Spray Blackening, Irregular	" "	.001542	" "
Brush, Regular	" "	.001334	" "
Brush, Irregular	" "	.001570	" "
Clean and Blow Off, Regular	" "	.00097115	" "
Clean and Blow Off, Irregular	" "	.0014286	" "
Dry With Torch	" "	.00041525	" "

Fig. 11—Floor finishing data.

Sterling Foundry Company
Department: Core
Operation : Floor Finishing

Sheet 4 of 9
Standard Data
August 1951

FILE FILLETS

Inches Length	All. Time
10	.16
15	.21
20	.26
25	.31
30	.36
35	.41
40	.46
45	.51
50	.56
55	.61
60	.66
65	.71
70	.76
75	.81
80	.86
85	.91
90	.96
95	1.01
100	1.06
110	1.16
120	1.26
130	1.36
140	1.46
150	1.56
160	1.66
170	1.76
180	1.86
190	1.96
200	2.06

BRUSH CHILLS

Area Sq. In.	All. Time (per coat)
5	.12
10	.17
15	.22
20	.26
25	.30
30	.34
35	.37
40	.39
45	.42
50	.45
55	.48
60	.50
65	.53
70	.56
75	.59
80	.61
85	.64
90	.67
95	.70
100	.73
110	.78
120	.84
130	.90
140	.95
150	1.01
160	1.07
170	1.12
180	1.18
190	1.24
200	1.30

CLEAN OUT HOOK HOLE
each - .19

CLEAN OUT PIPE VENTS
each - .19

TIE WIRES, TIE-IN CORES
each - .77

REPOSITION FOR FINISHING
By Hand to 200# .33
By Crane 200#-up 4.19

BLOCK AFTER REPOSITIONING
each - .45

PASTE-IN CORES

Type	Class A	B	C	D
I	.29	.53	.96	1.44
II	.58	1.84	4.32	6.96

EXTENSION OF TABLES

File Fillet	.01 per lin. in.
Brush Chills, per coat	.0057 per sq. in.

Fig. 12—Floor finishing data.

Sterling Foundry Company
Department: Core
Operation : Floor Finishing

Sheet 5 of 9
Standard Data
August 1951

CLEAN OFF BEDDING SAND

Area Sq. In.	Reg	Irreg
5	.18	.35
10	.27	.53
15	.33	.65
20	.40	.78
25	.46	.90
30	.52	1.02
35	.57	1.12
40	.61	1.21
45	.66	1.30
50	.70	1.38
55	.73	1.46
60	.76	1.54
65	.80	1.61
70	.83	1.68
75	.85	1.76
80	.87	1.83
85	.90	1.90
90	.92	1.97
95	.94	2.04
100	.96	2.11
110	1.00	2.24
120	1.05	2.38
130	1.09	2.52
140	1.13	2.66
150	1.17	2.80
160	1.21	2.94
170	1.25	3.07
180	1.30	3.21
190	1.34	3.35
200	1.38	3.49
210	1.42	3.63
220	1.46	3.77
230	1.51	3.91
240	1.55	4.04
250	1.59	4.18
260	1.63	4.32
270	1.67	4.46
280	1.71	4.60
290	1.76	4.74
300	1.80	4.87
310	1.84	5.01
320	1.88	5.15
330	1.92	5.29
340	1.97	5.43
350	2.01	5.57
360	2.05	5.71
370	2.09	5.84
380	2.13	5.98
390	2.17	6.12
400	2.22	6.26
410	2.26	6.40
420	2.30	6.54
430	2.34	6.67
440	2.38	6.81
450	2.43	6.95
460	2.47	7.09
470	2.51	7.23
480	2.55	7.37
490	2.59	7.51
500	2.63	7.64
510	2.68	7.78
520	2.72	7.92
530	2.76	8.06
540	2.80	8.20
550	2.84	8.34
560	2.89	8.47
570	2.93	8.61
580	2.97	8.75
590	3.01	8.89
600	3.05	9.03
610	3.09	9.17
620	3.14	9.31
630	3.18	9.44
640	3.22	9.58
650	3.26	9.72
660	3.30	9.86
670	3.35	10.00
680	3.39	10.14
690	3.43	10.27
700	3.47	10.41
710	3.51	10.55
720	3.55	10.69
730	3.60	10.83
740	3.64	10.97
750	3.68	11.11
760	3.72	11.25

EXTENSION OF TABLE

Reg.	.0041818 Sq. In.
Irreg.	.013846 Sq. In.

Fig. 13—Floor finishing data.

Sterling Foundry Company
Department: Core
Operation : Floor Finishing

Sheet 6 of 9
Standard Data
August 1951

CLEAN VENTS	FINISH JOINTS		INCHES LENGTH	APPLY PASTE	FILE & FILL JOINTS ONLY		FILE JOINTS ONLY	
	Reg	Irreg			Reg	Irreg	Reg	Irreg
.10	.33	.40	10	.27	.23	.31	.08	.10
.13	.45	.56	15	.30	.31	.42	.10	.14
.16	.57	.71	20	.32	.38	.53	.13	.18
.19	.69	.87	25	.34	.45	.64	.15	.22
.22	.81	1.03	30	.35	.53	.75	.18	.25
.25	.93	1.18	35	.36	.60	.85	.20	.29
.28	1.05	1.34	40	.37	.67	.96	.23	.33
.31	1.17	1.49	45	.38	.75	1.07	.25	.37
.34	1.29	1.65	50	.39	.82	1.18	.28	.40
.37	1.41	1.81	55	.39	.89	1.29	.30	.44
.40	1.53	1.96	60	.40	.97	1.40	.33	.48
.43	1.64	2.12	65	.41	1.04	1.51	.35	.51
.46	1.76	2.27	70	.41	1.11	1.62	.38	.55
.49	1.88	2.43	75	.42	1.18	1.73	.40	.59
.53	2.00	2.59	80	.43	1.26	1.84	.43	.63
.56	2.12	2.74	85	.44	1.33	1.95	.45	.66
.59	2.24	2.90	90	.44	1.40	2.06	.48	.70
.62	2.36	3.05	95	.45	1.48	2.17	.50	.74
.65	2.48	3.21	100	.46	1.55	2.28	.53	.78
.71	2.72	3.52	110	.47	1.70	2.50	.58	.85
.77	2.96	3.83	120	.49	1.84	2.71	.63	.93
.83	3.20	4.15	130	.50	1.99	2.93	.68	1.00
.89	3.44	4.46	140	.51	2.13	3.15	.73	1.08
.95	3.68	4.77	150	.53	2.28	3.37	.78	1.15
1.01	3.92	5.08	160	.54	2.43	3.59	.83	1.23
1.07	4.16	5.39	170	.56	2.57	3.81	.88	1.30
1.13	4.40	5.71	180	.57	2.72	4.03	.93	1.38
1.19	4.64	6.02	190	.59	2.86	4.25	.98	1.45
1.25	4.88	6.33	200	.60	3.01	4.46	1.03	1.52
			210	.61				
			220	.63				
			230	.64				
			240	.66				
			250	.67				
			260	.69				
			270	.70				
			280	.71				
			290	.73				
			300	.74				

Note: When two cores paste together from the same box, use one half the value for 'Apply Paste'.

EXTENSION OF TABLE

Clean Vents	.00604 per inch
Finish Joints, Reg.	.024 " "
Finish Joints, Irreg.	.0312 " "
Apply Paste	.00143 " "
File and Fill Only Reg.	.01461 " "
File and Fill Only Irr.	.02188 " "
File Only Reg.	.005 " "
File Only Irreg.	.0075 " "

Fig. 14—Floor finishing data.

necessary for cores which do not paste. Additional values concerning cores which paste or assemble are shown in Figs. 14 and 15, while the handling values are shown in Figs. 16 and 17. A neat, convenient

Sterling Foundry Company
Department: Core
Operation : Floor Finishing
Sheet 7 of 9
Standard Data
August 1951

RUB AND FIT. HAND	RUB AND FIT. JIGS	WEIGHT OF CORE	ASSEMBLE AND FIT
.35	1.38	10	.70
.47	1.50	15	.94
.59	1.60	20	1.18
.69	1.69	25	1.38
.78	1.76	30	1.56
.85	1.83	35	1.70
.92	1.88	40	1.84
.99	1.94	45	1.98
1.06	2.00	50	2.12
1.13	2.06	55	2.26
1.20	2.12	60	2.40
1.27	2.18	65	2.54
1.34	2.24	70	2.68
1.42	2.30	75	2.84
1.49	2.35	80	2.98
1.56	2.41	85	3.12
1.63	2.47	90	3.26
1.70	2.53	95	3.40
1.77	2.58	100	3.54
1.91	2.70	110	3.82
2.05	2.82	120	4.10
2.19	2.93	130	4.38
2.34	3.05	140	4.68
2.48	3.17	150	4.96
2.62	3.29	160	5.24
2.76	3.40	170	5.52
2.91	3.52	180	5.81
3.05	3.64	190	6.10
3.19	3.75	200	6.38

WEIGHT OF CORE	RUB AND FIT. JIGS
220	5.92
240	5.98
260	6.04
280	6.10
300	6.16
320	6.22
340	6.28
360	6.34
380	6.40
400	6.46
420	6.52
440	6.58
460	6.64
480	6.70
500	6.76
550	6.91
600	7.06
650	7.21
700	7.36
750	7.51
800	7.66
850	7.81
900	7.96
950	8.11
1000	8.26

Use Cope Half Wt. for Value

RUB AND FIT. CRANE
Over 200# per core 6.85
(apply to both cope and drag)

TIE WIRES
Bearing ends, per core .25
Hook ends, per core .39
(apply to both cope and drag)

TIE WIRE AROUND ASSEMBLY CORES
each wire .77

COVER GATE AND RISER OPENINGS (ASSEMBLY CORES)
each opening .50

Fig. 15—Floor finishing data.

Sterling Foundry Company
Department: Core
Operation : Floor Finishing
Sheet 8 of 9
Standard Data
August 1951

CAR AND PLATE VALUE - STEEL PLATES

Projected Area of Core	HEIGHT OF CORE 2"	4"	6"	8"	10"	12"	14"	18"	22"	26"
To 150 Sq. In.	.56	.57	.58	.68	.71	.78				
150 - 200	.59	.61	.63	.73	.77	.84				
200 - 250	.61	.64	.65	.76	.81	.88	.95			
250 - 300	.68	.70	.75	.85	.91	1.09	1.09			
300 - 350	.77	.78	.84	.97	1.04	1.25	1.25	1.57		
350 - 400	.80	.81	.87	1.00	1.07	1.28	1.28	1.60		
400 - 450	.86	.88	.96	1.10	1.19	1.44	1.44	1.84	1.93	
450 - 500	1.03	1.06	1.15	1.30	1.41	1.69	1.69	2.15	2.25	
Over 500	1.19	1.23	1.33	1.53	1.65	2.00	2.00	2.55	2.69	2.83

CAR AND PLATE VALUE - CAST IRON PLATE

Projected Area of Core	HEIGHT OF CORE 2"	4"	6"	10"	14"	18"	22"	26"
To 550 Sq. In.	2.24	2.29	2.35	2.66	2.99	3.54	3.63	3.73
550 - 900	2.46	2.53	2.61	3.04	3.49	4.24	4.39	4.52
900 - 1200	3.25	3.48	3.66	4.12	4.79	5.92	6.13	6.34
1200 - 2300	4.93	5.24	5.49	6.09	6.99	8.50	8.78	9.06
Over 2300	5.39	5.85	6.22	7.02	8.22	10.34	10.62	10.90

CORES TO HORSES

Weight of Core	All. Time
15	.42
20	.45
25	.49
30	.52
35	.55
40	.58
45	.62
50	.65
55	.68
60	.72
65	.75
70	.78
75	.82
80-over Crane Handle	1.87

RELOAD FOR REBAKING - 4.25 sm.

Note: Cores which are reloaded for rebaking go through 2 cycles of the "Car and Plate" and "To Horses" elements, and will therefore receive double values for those elements.

Fig. 16—Floor finishing data.

and easy-to-use set of tables is essential to successful standard setting, and this portion of any Standard Data development should be given adequate work and attention to make the tables understandable to

Sterling Foundry Company
Department: Core
Operation : Floor Finishing
Sheet 9 of 9
Standard Data
August 1951

CORE STORAGE

TO MACHINE FLOOR STORAGE

Weight of Core	All. Time
15	.50
20	.53
25	.57
30	.60
35	.63
40	.67
45	.70
50	.73
55	.77
60	.80
65	.83
70	.87
75	.90
Crane Handle 80 - over	4.25

TO HILL FLOOR STORAGE

Weight of Core	All. Time
15	1.01
20	1.07
25	1.13
30	1.19
35	1.25
40	1.31
45	1.37
50	1.43
55	1.49
60	1.55
65	1.61
70	1.67
75	1.73
Crane Handle 80 - 200	4.86
200 - 400	5.77
400 - 800	9.41
800 - over	15.48

ROLL OVER FOR CRANE HANDLING TO STORAGE
By Hand - 80 - 200 .69 sm.
By Crane - over 200 4.19 sm.

BLOCKING OR WEDGING IN STORAGE - .45 sm.

Note: In determining the correct storage values for cores which paste or assemble, the total pasted or assembled weight will be used, with the values pro-rated to the halves of the pasted core or parts of the assembled core.

Fig. 17—Floor finishing data.

any person familiar with the type of work concerned. The values on the tables are calculated to the nearest hundredth of a minute to simplify the setting of standards.

Pre-Check and Application

As soon the the tables were sufficiently developed to permit the setting of standards, a three week pre-check was made. This pre-check showed that the Standard Data Tables covered the range of work being processed through the department, and that the standards were consistent (one job with another). Observation of the group and discussion with the foreman of the group during the pre-check period indicated that the performances calculated were exceptionally close to the expected performance at that time. After the pre-check was completed, the standards were put into actual use. The group had previously worked under an indirect labor incentive plan, and therefore realized the advantages to be gained under an incentive plan. The men asked many questions which were explained thoroughly to them, and after being familiarized with the Standard Data standards, they accepted them as being the most accurate measure of work produced, knowing that any changes in methods or finishing requirements on a core would be immediately reflected in the standard.

Factor Card

In order to make use of the Standard Data Tables, a "Core Finishing Factor Card" was designed to show all the operations necessary for core finishing. This card, as shown in Fig. 18, was designed to accommodate both bench and floor core finishing values and standards.

The upper portion of the card contains informa-

PATT. NO. 1740-1 BX NO. 30 CUST. W & S CO. STD. BCH. STD. FL. 19.3

CL. OF CORE IRREGULAR WT. 70 PROJ. AREA 341 HT. 4 1/4" COPE/DRAG HALF DRAG

PASTE/ASSEM. W BX/SKD 27 TOTAL PASTED/ASSEM WT 115 FACTORED BY L.M DATE 7-18-51

FINISHING	MEAS.	STD.	FINISHING	MEAS.	STD.	HANDLING	MEAS.	STD.
CLEAN & BLOW OFF		.88	CUT VENTS			CAR & PLATE	STEEL	.84
SILICA, COATS 1		1.01	CLEAN VENTS	40"	.28	TO HORSES, FLOOR		.78
BLACKENING		.85	RUB & FIT, HAND	✓	.99			
BRUSH		1.04	RUB & FIT, CRANE			TO CONVEYOR, BENCH		
DRY		1.18	RUB & FIT, JIGS					
PASTE-IN CORES			ASSEMBLE & FIT			TO MACH. STORAGE		
	1-28	1.84	CALIPER			TO MILL STORAGE	✓	2.43
FILE BURR			APPLY PASTE	100"	.46			
FILE FILLET	40"	.46	FINISH JOINTS IRR	80"	2.59	TO BENCH STORAGE		
GRINDING GANG IRR	140"	2.66				REMARKS		
			FILE & FILL ONLY					
HOODS						ROLL FOR CRANE HANDLE TO STORAGE		
PIPE VENTS			COVER GATE & RISER OPENINGS			BLOCK IN STORAGE	✓	.23
WIRE VENTS			TIE WIRES					
TIE WIRES, TIE-IN CORES			BLACKING ENDS			TOTAL HANDLING		4.26
CHILLS, COATS			HOOK ENDS					
			ASSEMBLY					
REPOSITION	✓	.33	TOTAL FINISHING		15.00	TOTAL FINISHING STANDARD		19.26
BLOCKING	✓	.45						

Fig. 18—Core finishing factor card showing application of values for a drag half paste core.

tion which identifies the core as well as certain characteristics of the core necessary to arrive at the proper values from the tables. The lower portion of the card is divided into three columns, the first column being concerned with factors which pertain to operations performed in the finishing area. The second column concerns additional finishing operations pertaining to paste or assembly cores only, and the total finishing allowance is recorded at the bottom of the second column. Handling factors only appear in the third column, with the total handling allowance recorded. The Total Finishing Standard, which is recorded in the lower right corner of the card, is calculated to the nearest tenth of a minute and indicated on the upper portion of the card in the proper space. This standard is entered on the Master Standard Record, and the factor card is filed for future reference. If any change occurs in the equipment or method of finishing the core, a new factor card is made out showing the new standard. The old factor card is placed in an obsolete file with notations explaining the change, and is retained as a record of the original standard.

Write-Up

When a Standard Data Set-up is ready for application, a complete and comprehensive write-up should be made describing the location and condition of all equipment and materials used for the operations and a complete analysis of the methods used. Explanations of all elements, the relaxation allowances used, and the combining of any elements to form factors should be included. Photographs or floor plan sketches of the work area and locations of various equipment are helpful in explaining details of the work analysis, etc. Whenever any change in methods or equipment is made, the portion of the tables affected should be checked and discussed thoroughly, and new studies should be made if they are found to be necessary. It is of the utmost importance that the Standards Department keep abreast of all changed conditions in the plant, as Standard Data Tables are workable only so long as the conditions under which the tables were developed remain in effect.

The development of Standard Data is often a time consuming operation, but the final advantages realized from a good set of Standard Data Tables repays the effort expended many times over. Any time study man familiar with the proper technique of taking time studies, and having the imagination to visualize the problems which may be encountered and the results to be obtained, can develop good Standard Data Tables which are complete and workable. The desire of any plant manager is to maintain quality production. Honest and equitable standards set by such tables can contribute greatly to this end and at the same time gain the good will of the workers in the plant.

RESINS AND SANDS FOR SHELL MOLDING

By

Manuel F. Drumm*

ABSTRACT

The proper choice of a sand and the resin binder for shell molding determine to a large extent whether the process will be successful and economic. Most resins perform differently in degree and their utility depends upon their physical properties and the mechanism of the thermosetting reaction. Some are far superior to others. A cheap sand with the desired fineness is not always an economic shell molding sand. The performance of a sand on the automatic machine is dependent greatly upon the particle size distribution and grain shape, but of extreme importance is the condition of the surface of the sand.

The choice of molding materials may be guided by proper screening methods and once production has started, control methods must be inaugurated to insure smooth and economic operation.

The shell molding process for the casting of metals has presented to the foundryman a new opportunity to express his resourcefulness. No longer is he dealing with a wet molding medium which is jolted or rammed to shape; rather, it is with a dry, free-flowing mixture which is molded by the force of gravity and the action of heat on a resinous binder. Many of the previous theories on flow and packing, which may be sound in themselves, lend to confusion, since, except for a few basic principles, the similarities of green sand molding and shell molding disappear.

Each of the two components of a shell molding mixture, sand and resin, contribute to the success of the process. For clarity of comprehension, each must be understood thoroughly. The foundryman has an inherent knowledge of sands, their limitations, their reaction with metals and refractoriness. It lies, therefore, with the resinologist to familiarize the foundrymen with the technology of these binders which are so important to success of shell molding technique.

There are many excellent publications on resinous materials and one may satisfy his interest to any extent. However, there is little written of a general nature with emphasis on one application, viz., foundry. This paper, therefore, is designed to bring before the consumer the general characteristics of the materials with which he will be concerned during his association with shell molding.

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The Resin

Resinous materials are classified broadly as thermoplastic and thermosetting. The former is generally defined as a material which may be subjected to heat in a reversible manner without undergoing a chemical transformation. For example, some metals may be melted, cooled and remelted, under controlled conditions, with no chemical change. These are thermoplastic by common definition. Thermosetting resins are those which will transform to an infusible, insoluble state when subjected to heat, and this process is not reversible. Thermoplastic resins include wood rosin, nylon, polystyrene, polyethylene and polyvinylchloride. Typical thermosetting materials are certain types of polyesters, urea-formaldehyde, melamine-formaldehyde, resorcinol-formaldehyde and phenol-formaldehyde resins.

As shell mold binders, any of the above might be used, providing the processing conditions were so adjusted. It is apparent, however, that only the thermosetting types could make the shell production cycle economical, at least with our present process know-how. Of these thermosetting materials, only the urea-formaldehyde and phenol-formaldehyde types are economically feasible. It is these, therefore, with which the discussion will deal.

Phenol-formaldehyde resins are prepared by reacting phenol and formaldehyde in the presence of a catalyst. The formaldehyde is provided generally as a 37 per cent water solution; the phenol is supplied quite pure. The reaction is allowed to proceed under carefully controlled conditions, and then the water is removed. Depending upon the ratio of reactants, the catalyst and conditions of reaction, a large variety of products are accessible, among which are liquid resins (binders for coated abrasives, grinding wheels, core binders), solid thermosetting resins (glass wool insulation, wood pulp molding) and thermoplastic resins which are caused to thermoset by mixing with a cross-linking agent (shell molding, grinding wheels, molding powders). Very generally, the use of a basic catalyst and a mole excess of formaldehyde will result under carefully controlled conditions, in the formation of a thermosetting material which may be in

either solid or liquid form. An acid catalyst and slight excess of phenol will offer a thermoplastic resin which is used almost exclusively in the solid form.

The thermosetting phenolic resins may be "hardened" merely by the application of heat. No additional catalyst is necessary. This reactivity, unfortunately, is not limited to extreme heat, as they will advance or react slowly at room temperature. Therefore, it is necessary to inventory under refrigeration if large amounts are to be accumulated for an extended period.

The thermoplastic phenolics, on the other hand, will not set, even when heated, unless mixed with a setting or cross-linking agent. This agent is commonly hexamethylenetetramine, a condensation product of ammonia and formaldehyde. It is crystalline and may be ground to a fine powder. The resin and cross-linking agent are pulverized together and thoroughly blended. In this state, they may be stored indefinitely, so long as moisture is omitted and they are not exposed to excessive heat.

Thus, it remains that so far as shell molding is concerned, with the present machines available and process requirements, the solid, thermoplastic, phenol-formaldehyde resin to which has been added a curing agent, is the most universally adaptable *phenolic* binder in use today.

It has been indicated that thermosetting urea-formaldehyde resins will find application in shell molding. They have not been used successfully with the ferrous metals, but some promise is held for use with aluminum, magnesium and their alloys. It appears that, at the pouring temperatures of most ferrous metals, the evolution of gases by these polymers, whether by their nature or rate of evolution, causes serious casting defects. The usual surface defects have been noted, in addition to severe internal porosity. This has been true in some cases with the bronzes, also.

Pouring temperatures of the light metals, however, are sufficiently low to minimize gassing. Phenolics do not break down as readily as urea resins and, at low pouring temperatures, a shake-out problem may arise. The inexpensive nature of urea-formaldehyde resins and ease of shake-out provide a promising binder for the shell casting of the light metals.

Urea-formaldehyde polymers are prepared in either the liquid or solid (usually spray-dried) condition. Both forms may be used as core binders, but the solid form is employed in shell molding. Mixing the dry materials with phenolic resins give a variety of binders whose properties will vary considerably and offer a large number of shell molding resins.

The use of urea resins has not been evaluated by many foundries and practical knowledge on their use for this application is very limited. Since the phenol-formaldehyde types are being used for all applications, and since it is with these the foundry will be most concerned, the detailed discussion will be limited to these materials.

The properties of a resinous material vary slightly from batch to batch to some degree, even though its production is controlled carefully. Performance of the product is related to its properties and strict record is kept on each batch. Normally, for control purposes, the following characteristics are measured: softening point, cure time, flow, particle size and curing agent concentration. There are many methods by which these measurements are made. However, a brief description of one method used for each determination will suffice to illustrate the principal.

Control Tests for Shell Molding Binders

Softening Point: Since these resins are amorphous, they have no true melting point. However, as heat is applied, they soften, and a measure is made of the temperature at which the material softens sufficiently to adhere to a plate under constant pressure. This may be accomplished by use of a polished copper block which is heated to provide a temperature gradient over the desired range. A small sample of constant volume (and weight) is allowed to fall upon the block, remain there for a given period, then brushed with a camel's hair brush with constant pressure, at a constant rate. The temperature at which the solid adheres sufficiently that it will not brush off and below which it may be removed by brushing is termed the softening point. Figure 1 illustrates this principal.

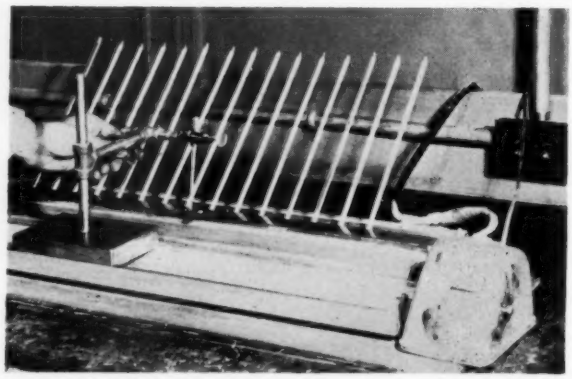


Fig. 1—Determination of softening point of a resinous material.

Cure Time: The rate at which a resin cures is probably the most controversial of all measurements. The determination of a cure or setting time does not reveal much unless one can determine the melt viscosity, length of time during which the resin remains fluid and the nature of the cured product, among others. These important properties are extremely difficult to measure, and little has been accomplished to do so. Thus, the determination of a cure time must serve for the present. Figure 2 shows a controlled hot plate whose surface has been polished and the temperature of which may be adjusted carefully. A sample of given weight is placed on the plate and a timer started. The sample is stroked with a spatula that remains in constant contact with the resin. When the material cures to the point where it will no longer flow and has a dry surface appearance, the timer is stopped and the time recorded as "film cure time."

Flow: The determination of flow, such that it is comparable to and may be related with that occurring in the many diverse applications of resinous products, has not been accomplished. However, a method which allows comparison between resins is available. A pill composed of the desired resin is placed on a graduated plate, such that its leading edge touches a zero line. The plate is set horizontally on a fixture which is in a carefully controlled oven at an arbitrary temperature. The oven is closed and the plate allowed to remain horizontal for 3 min. It is then tilted, automatically, to 65 degrees and allowed to remain so for 17 min. The flow is then determined as the total forward flow of the pill. The results of this test are shown in Fig. 3.

Concentration of Curing Agent: Shell mold binders are, for the most part, two-stage or thermoplastic polymers admixed with hexamethylenetetramine, $(CH_2)_6N_4$. Providing no other nitrogenous materials are available, the concentration of cross-linking agent may be measured by determining the amount of nitrogen present. This may be done by the common Kjeldahl method.

Particle Size: These binders are usually pulverized so that less than 2 per cent is retained on a standard U.S. 200 mesh sieve. Their average particle diameters are in the range of 10-20 microns. A particle size distribution for a shell molding resin as determined by particle count is given in Fig. 4.

Resin Properties and Characteristics

The specifications of some typical shell mold binders are given in Table 1. The range in these values is necessary to insure a controllable process; these properties are within the 95% confidence limits.

TABLE 1—SPECIFICATIONS OF SOME TYPICAL SHELL MOLDING RESINS

	Resin A	B	C
Softening Point (°C)	98-108	90-105	95-105
Film Cure Rate at			
150 C (300 F), sec		35-50	40-75
155 C (310 F), sec	60-110		30-55
Flow at 125 C (257 F) mm	10-30	12-20	25-50
Particle Size (+200 Mesh), %	1.5	1.5	1.5
Nitrogen Content, %	3.35-3.85	4.42-5.42	3.35-3.85

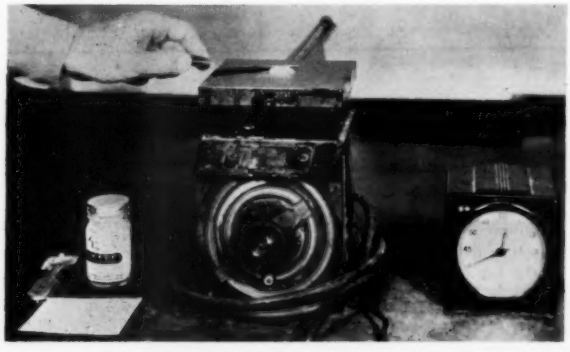


Fig. 2—Determination of cure time of a thermosetting resin.

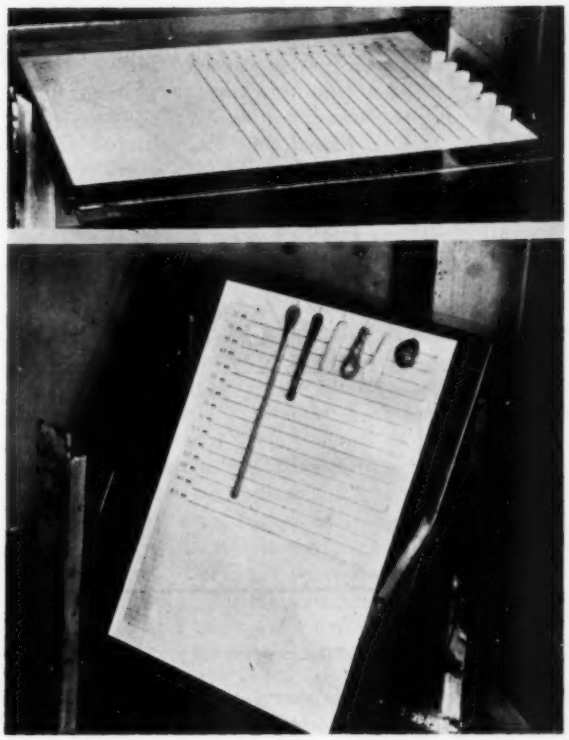


Fig. 3—Measurement of flow of a thermosetting resin.

The properties of shell mold binders have not been correlated successfully to their performance on a variety of patterns and machines. It is re-emphasized these figures do not tell the whole story. Only with a great deal of experience can one predict the general performance of a binder from its control properties.

So that the consumer of these molding materials may have a deeper appreciation of the complexity of the materials with which they are dealing. Figure 5 should be considered. This diagram represents the manner in which the rheological properties of some thermosetting resins vary with time at constant temperature.

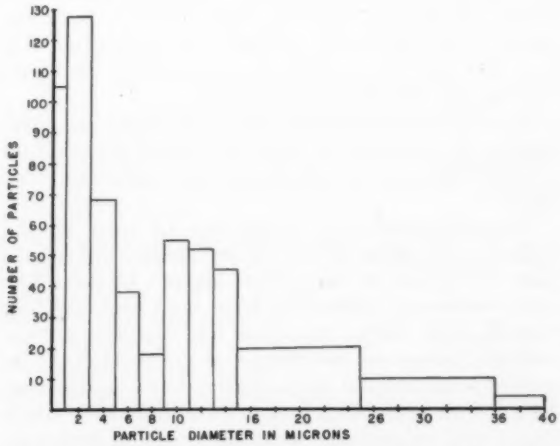


Fig. 4—Particle size distribution of a shell molding resin.

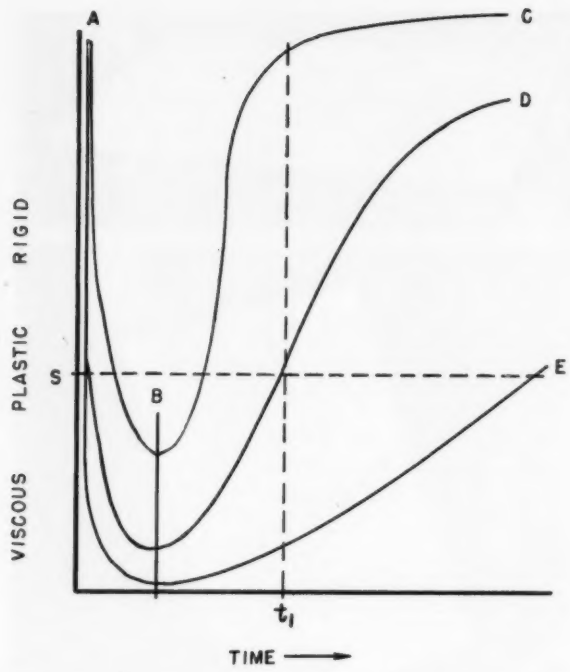


Fig. 5—Physical changes during the curing process of a thermosetting resin.

In Fig. 5, the line AB represents a melting process during which there is a decrease in viscosity and the binder coats the sand grains and flows toward the heated pattern. At B, flow is stopping and the resin is beginning to cure. Here, the sand grains are in their permanent position. BC shows the increase in rigidity as the resin continues to harden. Let us presume that at point S, the resins appear the same visually under the conditions of the film cure test. Also, it is feasible that the resins have comparable softening points and flow values. Depending upon the path which the hardening process follows, BC, BD or BE, the ejection of the shell at time t_1 will furnish a rigid shell, a pliable or a soft shell and a very rubbery shell, respectively. Also, it is probable that resin BE would not have formed a bond sufficiently strong at t_1 and the shell will fall from the pattern. At the other extreme, it is possible that resin BC set the sand grains too quickly, thus preventing an excellent shell surface.

Thus, it is performance, not properties that offers success. It could well be that one resin is preferred on certain jobs, yet be considered less satisfactory for others.

Phenol-formaldehyde resins may be used successfully for the casting of ferrous and non-ferrous metals. The limitations of the process are not known. The only noteworthy difficulties have been with the low carbon, mild steels, but these too will be cast successfully, when the metallurgy of the mold-metal interface is understood more clearly. Urea binders may be used for aluminum, magnesium and their alloys, and it may be preferred here due to ease of shake-out, as compared to the more heat-resistant phenolic. Urea-binders are not used successfully for brasses, bronzes or ferrous metals because of porosity of the castings due to gas. Even though phenolics are notably more heat resistant than ureas, except for aluminum and magnesium, casting temperatures are far above the decomposition temperatures of both. Rather, it is the nature of the product formed and/or the rate of evolution which cause the difficulty. The decomposition of a phenolic resin has been observed to pass through a tarry stage as contrasted to that of urea resins which pass directly to a completely disordered ash. This may account somewhat for the greater strength of phenolics over a longer period of exposure to high temperatures. There is need for research here.

The Molding Mixture

The performance of shell mold binders may best be shown by tests performed on shell molding mixtures. One sand was chosen and used as a basis for comparison of several resin binders. These binders were chosen indiscriminately from those produced by the leading resin manufacturers specifically for the shell molding process.

The sand used was an Ottawa sand with the sieve analysis shown in Table 2, as determined on several shipments. Before any testing was accomplished, the sand was graded to insure a relatively uniform sieve analysis.

TABLE 2—PARTICLE SIZE DISTRIBUTION OF THE STANDARD TEST SAND

U. S. Sieve No.	Analyses Number—Per Cent Retention 1	2	3
30	0.00	0.00	0.00
40	0.00	0.00	0.00
50	0.00	0.00	0.00
70	0.16	0.00	0.10
100	2.34	2.36	3.74
140	40.60	41.26	47.70
200	36.90	36.54	31.28
270	12.90	13.04	12.12
Pan	6.04	5.56	5.06
Total	98.94	98.76	100.00
A.F.S. Fineness No.	138	138.5	133.7

The resinous binders to be considered have the properties shown in Table 3 according to the previous test methods.

TABLE 3—PROPERTIES OF SOME SHELL MOLDING RESINS

	Resin D	E	F	G	H	I	J	K	L
Softening Point, °C	103	100	95	92	105	107	111	101	102
Film Cure Rate at 150°C, sec	40	27	55	40	49	53	42	74	56
Flow at 125°C, mm	17	11	17	14	17	25	15	21	28
Particle Size (+200 mesh), %	0.51	0.53	0.13	0.62	1.33	6.0	10.04	0.21	0.47
Nitrogen Content, %	3.58	4.72	3.66	5.38	4.3	3.62	4.22	2.5	5.00
Water Content, %	1.20	2.61		1.15	1.26	0.57	0.63	3.33	2.00

The decision which designates the resin to be used in a particular shop should be based upon sound reasoning. It depends upon the mechanical limita-

tions of the shell producing machine, the geometry of the pattern, required handling, method of handling and assembling, among others. It has been indicated that though one resin may perform excellently on one job, it may not be so satisfactory on another which utilizes different ovens, pattern design or mechanical adjustment. Also, it has been shown that if an automatic shell making machine has been adjusted to conditions optimum for one resin, it is possible that no other binder will operate *to its best advantage* under those conditions. In the early days of shell molding, the machines were "designed for the resin." Now, however, the many types of machines available and more diversified patterns require that the resin manufacturer maintain an active line of binders, one of which will prove satisfactory for almost any job.

In many cases, the foundry engineers available through the resin manufacturer, can size up a job reasonably well and make an intelligent recommendation of the types of binders to be used. This becomes increasingly difficult, as the process becomes more widespread, since the number of these trained people is not large. Also, there is no reason why, with a little background in resin technology, the foundryman cannot evaluate many resins in a short period, without attempting a production run. This is to be recommended for the foundry, rather than attempting to set up a control laboratory for the determination of the properties of the resins themselves. The latter would allow the foundry to check each batch of resin against the specifications of the manufacturer, but this would require extensive training of personnel in the test methods of each manufacturer, the adoption of statistical analysis and a long range program to establish a correlation between sets of data. The anticlimax is that when these properties have been determined, no satisfactory correlation between resin properties and performance on the shell molding line has been established, even by those who have the most diversified experience with shell molding, the resin manufacturer. It remains, therefore, that the foundryman should concentrate on the properties of the shell molding mixture and begin his work toward correlating these to actual performance on his particular application. This would also allow him to compare resins directly, rather than through a series of correlations of data.

Tests on the Molding Mixture: There are several tests which can be performed on the resin-sand shell molding mixture which do not require elaborate equipment, but which will show a comparison between binders. First, of course, a sand must be chosen which is representative of the type to be used in the shop. Our laboratory uses the sand whose properties are given in Table 2 as a standard for comparison. It was chosen from several sands of varying grain shapes and particle size distributions. The mixture to be tested may be prepared in any common mixer, the Hobart, twin-cone, twin-shell, but quite satisfactory is a muller. A standard mixing procedure should be established. There are several tests which can be run on this mix. This laboratory has developed three quantitative and two qualitative measurements. The former are flexural, tensile strengths and shell thickness or weight, while the qualitative tests are an evaluation of the tendency of the mixture to peel or fall off a pattern and its ability to fill completely areas of deep draw and sharp angles.

Flexural Test: The specimen mold, Fig. 6, may be fabricated from aluminum, or other suitable metal, such as cold rolled steel. The molding mixture may be from a production batch or compounded in the laboratory in any efficient mixer. Conditions for molding are chosen to conform approximately with those of the shell producing machine. The practice of this laboratory has been to use a mold temperature of 400 ± 10°F and a cure time of two minutes at 580

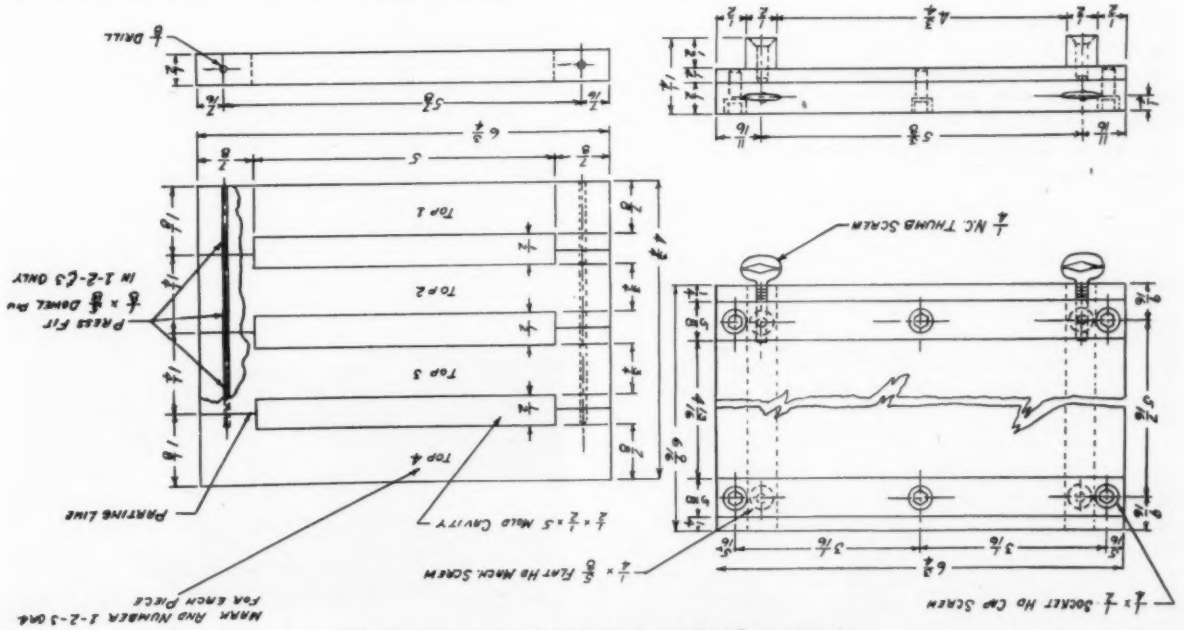


Fig. 6—Specimen mold for shell molding flexural test.

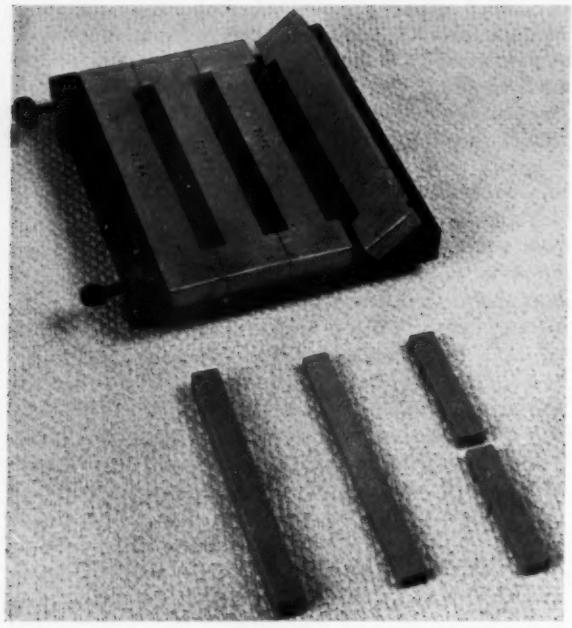


Fig. 7—Flexural bar mold and specimens. Lower left, as cast; Center, ground to dimensions, Right, ready for testing.

± 10°F. These conditions were adopted in the early days of testing, thus the low oven temperature. A curing temperature near 800-900 F would be more representative of commercial practice.

The test pieces are molded by filling the mold cavities to overflow, then striking off the excess quickly. After curing for 2 min, the dimensions of the piece will be about 5 in. × ½ in. × ½ in. One side of the specimen will be rough and non-uniform. Since the flexural strength is dependent greatly upon the uniformity of cross-sectional area, the rough side is abraded such that a constant thickness is produced. This is done by passing under an abrasive wheel. Each piece is then cut in half, producing a bar with the approximate dimensions 2.5 in. × 0.5 in. × 0.33 in. The test bars are conditioned for at least 12 hr under ASTM conditions before testing; this may not be critical, however. Figure 7 shows the mold and test specimens.

The bars are tested in any suitable device for the measurement of flexural strengths. This laboratory uses the Baldwin-Southwark Universal Tester with a cross-head speed of 0.025 in./min and a 2-in. span.

Figure 8 shows a sample being tested. It is important that the same face (rough or smooth) be in the same position for all tests. Those surfaces in contact with the mold walls are resin-rich compared to the rough face.

The test has good reproducibility with values of sufficient magnitude for accuracy. Statistical analyses of many of the data have been performed with designed experiments. The results are not complete at this writing; however, it is indicated that reproducible results are possible and that the test is capable of distinguishing between shell molding mixtures. The test may be adopted also to the measurement of the modulus of elasticity and toughness.

The flexural strengths offered by the resins in Table 3 are given in Table 4, and were determined by the test method described above. These early data are accurate within 10 per cent.

TABLE 4—FLEXURAL STRENGTHS OF SHELL MOLDING MIXTURES

Resin (Table 3)	Resin Concentration (%): Flexural Strength, psi 2%	4%	6%	9%
D	386	770	1120	1420
E	100	458	869	1297
F	348	848	1206	1508
G	172	472	810	1145
H	205	651	823	1367
I	201	578	952	1375
J	85	453	749	1213
K	158	432	658	839
L	150	552	849	1184
Ottawa Sand: Table 2				

Tensile Test: Specimens for this test are prepared in the same manner as above, using the mold shown in Fig. 9. This is the standard A.F.S. briquette, ¼ in. thick. "Striking-off" is accomplished in both directions from the center of the longitudinal axis. With proper care during manipulation, a relatively smooth surface may be obtained.

Any suitable tester may be used. The Instron Testing Machine is used by this laboratory. The mold and test piece are shown in Fig. 10.

This test is not so well established as the flexural test. The values obtained are small (200-400 psi) and often the clamps will rupture the sides of the test piece before a tensile break occurs, or the specimen ruptures off-center.

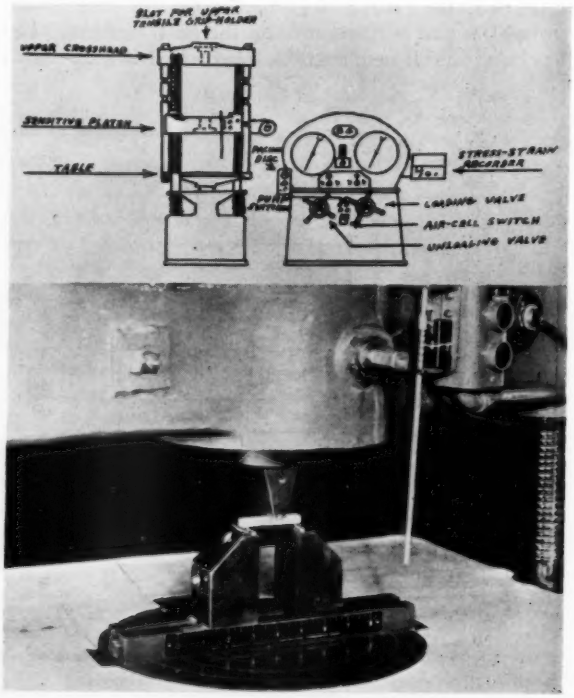


Fig. 8—Breaking the flexural bar.

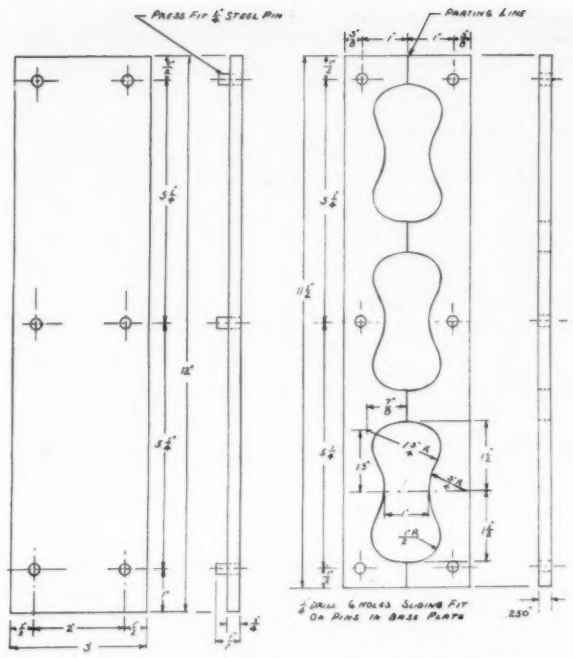


Fig. 9—Specimen mold for the shell molding tensile test.

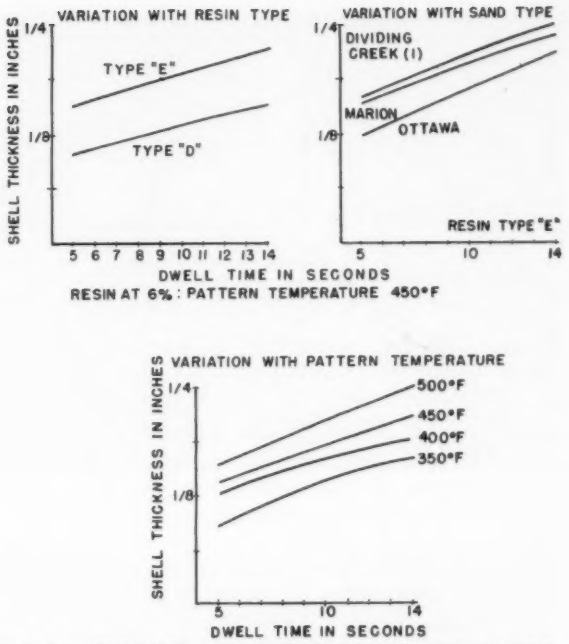


Fig. 11—Variation of shell thickness with type of resin and sand and with pattern temperature.

Even though some of the available foundry test equipment may not measure tensile strengths, but a combination of several stresses, a relative measurement may be entirely satisfactory for control purposes, and any accurate machine may be used regardless of the type of stress applied.

Shell Thickness and Weight: A cast iron plate, 6 in. × 4 in. × 0.5 in. and an open wooden box made of ¾-in. plywood whose top is grooved to provide a tight fit for the iron plate, are the equipment for this test. The plate is heated to 400 ± 10°F (or any temperature comparable to operating conditions), placed on the box and the assembly inverted for a given period. The plate and shell are then removed and placed in an oven to cure. The shell weight and its thickness are determined. The thickness of a shell mold is dependent upon plate temperature, coating time, resin content, resin properties, and the sand. Sands of high bulk densities provide thin shells. Figure 11 show the relationship of some of these properties.



Fig. 10—Tensile test mold and specimen.

Sub-Surface Hardness: The surface of the shell prepared by the preceding test is evaluated for its resistance to abrasion during handling by measuring its surface and sub-surface hardness. This is done by pressing down on the instrument so that it is depressed to its maximum, then rotating the knife-edge five times and reading the scale. The deflection, indicating the degree of penetration is called the "sub-surface hardness." This is recommended over the scratch hardness since differences in mold quality are more easily determined.

Packing on Vertical Surfaces: A small, round, cast iron pattern consisting of a 6-in. cylinder with a diameter of $\frac{3}{16}$-in. mounted on a circular base is used. The pattern is fitted to a cylindrical "dump box" of a convenient depth. The pattern may be heated to any desirable pattern temperature and the shell molding mixture may be dropped from any height by adjusting the depth of the dump box. The shell so formed is then sawed in half, longitudinally, and the surface examined over its entire length.

Hot Rigidity: The rigidity of a shell as it comes off the pattern governs whether a shell will be warped or whether ejection will be easy. Soft shells will many

TABLE 5—GRAIN DISTRIBUTION OF SOME SHELL MOLDING SANDS AND REFRACTORIES

Sand Designation U. S. Sieve No.	Per Cent Retention 20	30	40	50	70	100	140	200	270	Pan	Total	A.F.S. Fineness No.
Ottawa	0	0	0	0	0.08	2.81	43.19	34.91	12.69	5.55	99.23	137
New Jersey	0	0.18	0.68	1.20	2.72	15.48	29.80	28.00	11.80	8.42	98.28	123
Michigan (1)	0	0	0	0.72	13.88	50.80	23.76	9.44	1.92	0.44	100.96	84
Dividing Creek (1)	0	0.80	1.60	2.80	10.60	26.20	24.40	17.80	7.20	7.20	100.6	112
Dividing Creek (2)	0	0	0.06	0.30	3.18	34.76	36.74	16.92	4.98	2.34	99.28	104
Pennsylvania	0	0	0	1.46	7.68	14.24	20.14	24.80	16.14	15.10	99.56	146
Merion	0	0.40	0.80	1.80	5.60	24.2	31.8	21.4	8.4	4.8	99.2	115
Michigan (2)	0	0	0	0.74	15.82	53.28	21.28	6.74	1.02	0.34	99.22	80
McConnelsville	0.5	0.70	0.16	1.70	7.60	26.40	32.60	21.36	6.08	5.70	102.8	112
Zircon Sand	0	0	0	0	0	0.04	24.72	64.40	11.50	0.06	100.72	97
Mullite-Kyanite (50/50)	0	0	1.00	5.00	14.1	19.6	17.0	15.6	11.8	16.0	100.1	134
Calemo (Chamotte)	0	0	0	0.64	13.68	24.58	17.94	13.40	9.00	21.10	99.34	139

times break in the vicinity of the stripping pins or warp sufficiently to cause sticking to the pattern. The determination of this rigidity has not been established satisfactorily for publication. However, essentially, it involves molding a flexural bar under conditions prevailing in the shop, removing this bar immediately after cure and placing a standard weight on one end. The bar is allowed to bend for 10 sec and the amount of distortion measured.

The significance of these various tests and their relationship to the final product of shell molding, the casting is not known fully. The process is too young. However, it is necessary to establish some parameters which allow control of the production line. The detailed discussion of these tests gives the basis for furthur development of useful control methods.

Choosing the Resin

For the shop which is in automatic shell mold production, the evaluation of a prospective shell mold binder may be accomplished according to the following schedule, without interfering with production with a "blind" run:

1. Choose a standard sand, one which is representative of the sand used on the shell molding job.
2. Prepare several small mixes at various resin contents. This will allow determination of the optimum resin concentration.
3. Using the mixtures prepared in (2), make flexural and/or tensile measurements. Molding conditions should be comparable to those which the resin will be subjected in production.
4. Compare these results with those obtained on the resin being used in production.
5. If strength properties are considered adequate, choose the optimum resin concentration and run the tests for "Shell Thickness", "Peeling", "Hot Rigidity", and "Packing on Vertical Surfaces".
6. If the resin still performs satisfactorily, it may be tried in production on a limited scale with some confidence.

For the shop that is just entering shell molding and deluged with samples, choose several from reliable producers for consideration. A small experimental pattern will probably be available by this time and the best method of evaluating resins is to use the manufacturer's recommendations and make shells. By the time five or six binders have been evaluated in this manner, the operator will have gained considerable knowledge about the application of resinous materials. A tentative choice should be made and production started. Once under way, the more detailed testing program may begin, starting with the chosen resin. When test data are available on this material, other resins may be given a thorough evaluation and several sources of supply assured.

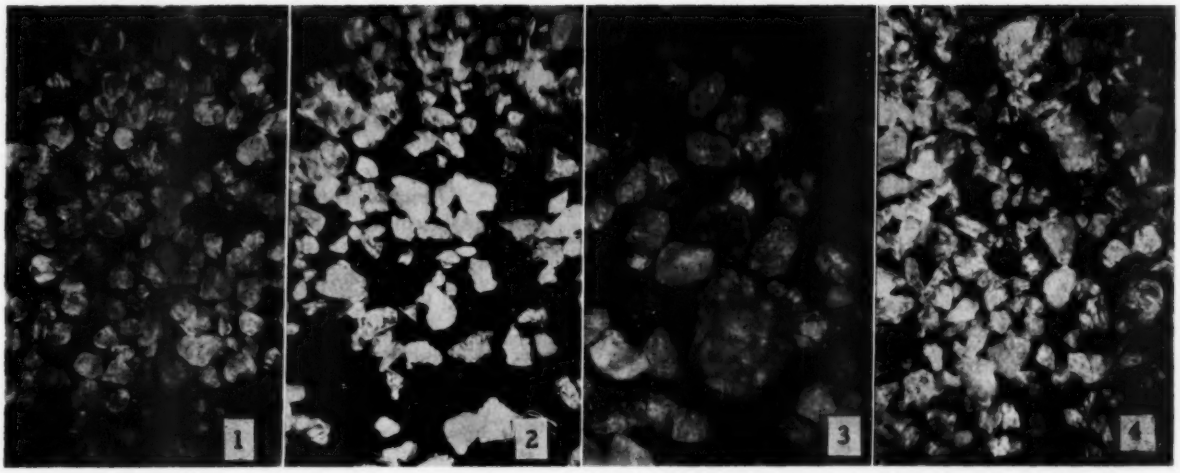
The flexural and tensile tests offer excellent control methods for the shell molding foundry. These strengths vary considerably with resin content and are dependent upon sand fineness, mixing efficiency and other process variables. The determinations may be made quickly by control laboratory personnel and require simple equipment. With proper control of the test procedure, good control charts can be constructed and once the limits within which the molding mixture may fluctuate have been determined, anomalies may be discovered before production is effected.

The Sand

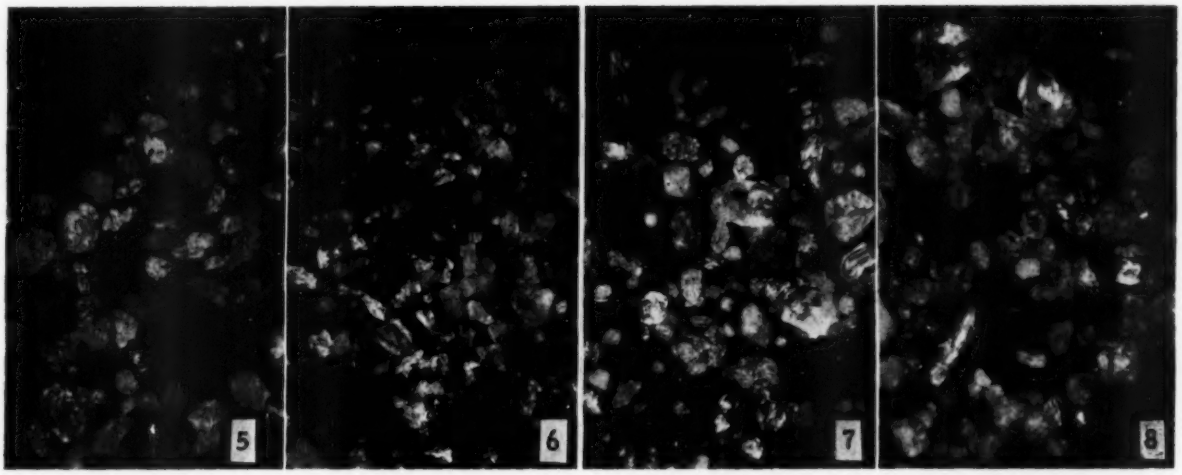
These tests may be used also to evaluate sands for shell molding. Preliminary information may be gained before a sand is placed in production. Optimum resin contents may be established and general characteristics of behavior observed. In this case, a standard resin binder must be chosen. Of the tests described above, those which will be most informing are the flexural, tensile, hardness, thickness and packing characteristics on vertical surfaces.

TABLE 6—FLEXURAL STRENGTHS OF MOLD MIXTURES CONTAINING VARIOUS SANDS AND RESIN "D" IN TABLE 3

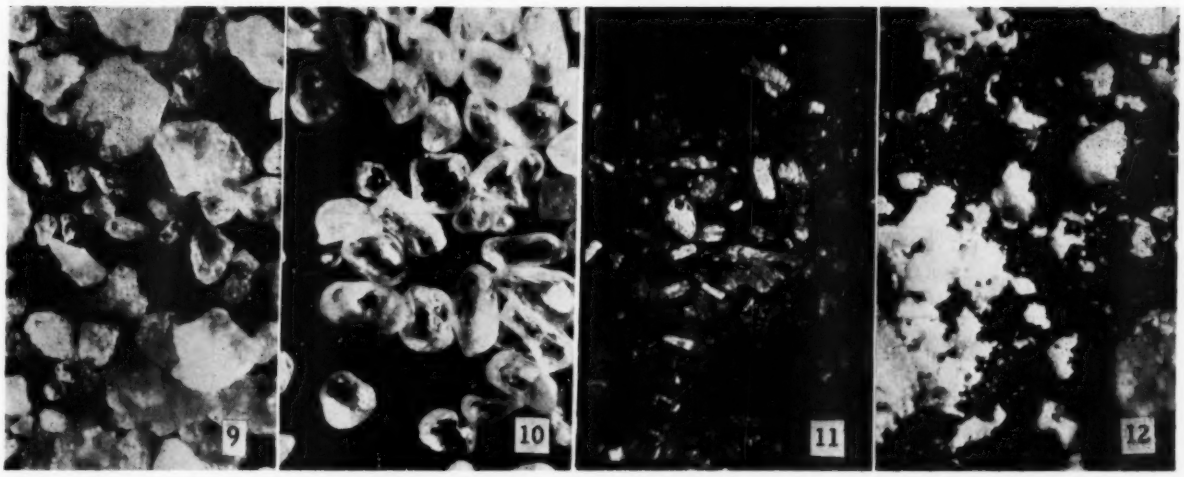
Sand Designation	Per Cent Resin: Flexural Strength, psi 2%	4%	6%	9%
Ottawa	268	764	1150	1420
New Jersey	43	306	497	904
Michigan (1)	118	372	576	1053
Dividing Creek (1)	102	381	742	1168
Dividing Creek (2)	125	528	910	1280
Pennsylvania	80	463	530	1081
Marion	120	425	820	1204
Michigan (2)	62	326	575	1064
McConnelsville	110	450	740	1100
Zircon Sand	857	1740	2132	——
Mullite-Kyanite (50/50)	30	374	923	1617
Calema (Chamotte)	—	85	272	638



Ottawa | New Jersey | Michigan (1) | Dividing Creek (1)



Dividing Creek (2) | Pennsylvania | Marion | Michigan (2)



McConnellsville | Zircon Sand | Mullite-Kyanite | Chamotte

Fig. 12—Photomicrographs of shell molding sands and refractories. Mag.—Approx. 45x.

An example of what may be done is seen in Table 5 and 6. These compare the flexural strengths of several shell molding mixtures made from sands with different grain distributions. All were compared under identical conditions as described above, using Resin "D" in Table 3. Table 5 shows the grain distribution of the sands used in the construction of Table 6. Figure 12 shows photomicrographs of these sands which indicate the general grain shape.

The grain distributions of these sands must not be construed as typical nor are they average distributions obtained from the sand companies. They are the results obtained on a small shipment, usually 100 lb. This is why the exact designations of the sands are not given.

Choosing the Sand

The choice of a sand for shell molding must be governed by many factors. Cost and availablity are obvious, but the nature of the sand itself is also important; particle distribution, grain shape and the condition of the sand surface are of particular concern. One must choose an inexpensive, available sand which will provide the desired casting finish. A cheap sand is not always an inexpensive shell molding sand. Sand cost per mold does not approach even closely the resin cost and the presence of clay or a dirty surface on the sand grains can demand the use of more resin than otherwise needed. This many times offsets the saving afforded by the use of poor sand.

As an example of the difference in behavior of sands being used for shell molding, consider the sands in Table 7. These were graded and blended to give equal grain distribution. Their grain shape may be seen in Fig. 12. These were compounded using the same resin and a flexural test performed as described above. Clearly, the Ottawa sand is superior over those of equivalent density. This may be due to a difference in grain shape, but more likely due to the condition of the sand surface.

A further indication of this may be seen in Table 8. Here the flexural strengths were determined on molding mixtures prepared from synthetic sands of various grain distributions. These sands were prepared by grading and blending the fractions, all from one sand. This Pennsylvania sand is described in Table 5 and Fig. 12. Here there is no significant difference in strength caused by the grain distributions considered.

TABLE 7—SYNTHETIC SANDS: EQUIVALENT GRAIN DISTRIBUTIONS FLEXURAL STRENGTHS OF SHELL MOLDING MIXTURES

Sieve No.	6	12	20	30	40	50	70	100	140	200	270	Pan
Per cent Retention:	0	0	0	0	0	0	0.48	3.78	44.02	34.90	12.30	4.52

Sand Type	Per Cent Resin	Flexural Strength (psi)
Ottawa	4	489
	6	1048
Pennsylvania	4	257
	6	490
New Jersey	4	215
	6	375

Of course, there is no doubt that fine sands require more binder, but this is not the complete story. Also, it should be realized that the test procedures described herein do not require that the molding mixtures be dropped from any height, thus packing effects are not determined.

It must be emphasized that neither a sand or a resin may be chosen on the basis of these tests alone. It is the casting which determines the molding materials. The strength of a shell mold, though important, must be only above a certain minimum which is established by process requirements. Having strengths above this minimum only wastes resin. For safe handling, a flexural strength of 700 psi is adequate for most applications, as is tensile strength of 200 psi by the above method. Nor is it necessary to perform all of the tests described. After some experience in resin behavior is attained, only a brief examination is necessary before a resin may be evaluated in production.

Future Trends

It is typical of all new processes that the first production lines are not the most economical. All resin manufacturers and shell molding foundries are striving toward a reduction in the cost of the process. There are several factors which contribute to the cost of shell molding. The cost of resin is among these, though not necessarily the only factor which requires reduction. Resin cost is determined to a great deal by raw material cost and less to manufacturing expenses. Most shell molding resins are prepared by no unique method, thus the supply and demand principals do not necessarily apply. The production of phenolic resins is already at a high rate, and it is doubtful whether a significant price reduction will result from an increase in output, unless raw material production is more sensitive to quantity changes.

The most logical manner in which to reduce resin cost is to reduce the amount required to do a given job. This is being attempted by several means. Choice of a good, clean sand, is very important. Another approach is reflected by the research being done on the coating of sands with the thermosetting binders,

TABLE 8—SYNTHETIC SANDS PREPARED FROM PENNSYLVANIA SAND IN TABLE 5 AND FIG. 12
Flexural Strengths of Shell Molding Mixtures

Sieve No.	Blend No. 1	No. 2	No. 3	No. 4
6	0	0	0	0
12	0	0	0	0
20	0	0	0	0
30	0	0	0	0
40	0	0	0	0
50	0	0	0	9
70	28	7	0	15
100	20	20	0	20
140	20	50	100	24
200	20	23	0	20
270	12	0	0	12
Pan	0	0	0	0
Flexural Strength (psi) (6% resin)	585	610	590	633

thus a more efficient utilization of resin. Some work has been done with alcohol solutions of the conventional binders. Either part or all of the binder may be added in this manner with some success. Another approach uses liquid resins of the phenolic type. This operation requires very careful control and uses specially heated equipment to advance the resins to their brittle points. Both approaches show some promise, but it is doubtful if either will be used successfully and economically on a production basis in the near future.

Summary

1. The general properties of phenolic shell mold binders show these materials to be amorphous, highly complex materials.

2. The process by which a thermosetting resin hardens is very complex and not determined quantitatively. Each resin, though appearing identical by a measurement of several physical properties, may behave in a somewhat different manner when subjected to heat.

3. Tests have been developed by which a shell molding resin and sand may be evaluated in the laboratory.

4. Tests have been developed which will serve as control methods for the shell molding system. Most useful are the flexural and tensile tests performed on the molding mixture.

5. The use of a flexural test has several advantages over a tensile strength determination. Values are larger and significant differences in mixtures are more readily recognized. The method may be adopted also to the determination of toughness and modulus of elasticity.

6. The performance of a sand in shell molding is dependent primarily upon the *condition of the sand surface, particle* distribution and grain shape.

Acknowledgment

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GUN PLACED SILICA CUPOLA LINING

By

T. E. Barlow and P. D. Humont*

Late in 1948 the first successful air-placement patch for a commercial cupola operation was put in at a Cleveland foundry. Similar processes may have been tried from time to time in the past, but this can be considered the first successful one in that it is the first operation in which the air-placement process continued to be the standard method of patching from then on. Both the equipment and the material at the time of that test were crude by present-day standards. However, there was sufficient success to indicate a definite possibility for improved patching practice using the air-placement technique. The process now is known generally as "gun patching the cupola."

Since that time, modifications and improvements in the design of equipment have been made. Simultaneously, there has been a continuous program of research and development to learn as much as possible about materials used in the process. However, the outstanding change has been in the use of the process itself and its acceptance by the industry. At the time of this writing the process is established to the extent that more than 50 per cent of the gray cast iron produced in this country today, and more than 80 per cent of the duplexed malleable produced, come from units which are maintained by the air-placement technique.

In the process of installing so many guns, field engineers, as well as the operators, have learned much about the technique of this process. Like any other refractory practice for melting units, there seems to be a best way to do most things. When the principle of the best way is followed, optimum results are obtained. On the contrary, if these basic concepts of good air-placement operation are ignored, it is possible to lose many of the advantages inherent to the process. An evaluation of some of these "best ways" is the subject of this paper.

General Considerations

Regardless of the type of refractory involved in cupola practice, there are certain fundamental factors which affect the refractory life. Some of these factors are inherent to the operation and cannot be changed to suit the refractory. They may be considered as "occupational hazards" to the refractory, which are necessary to meet the requirements of the cupola practice. There are other factors, however, which affect the refractory but which are under the control of the operator. These practices may be changed or modified to provide a better refractory life in the melting unit. Unfortunately, many of the tricks-of-the-trade are ignored with the result that some cupola operations show high refractory costs or low refractory life.

Any influence which results in a low refractory life must change the economy of the operation. Therefore, any reasonable change which will improve the refractory life should be considered to be of importance. In addition, as has been pointed out in numerous articles and talks on the subject, the refractory life of a cupola also affects the metallurgical results. The effect of refractory loss upon the uniformity of cupola operation and the subsequent metallurgical results obtained from the cupola is a subject in itself. But it should be pointed out that in discussing refractories in the cupola, consideration should be given to methods of reducing refractory costs by minimizing burn-out, and also a technique for closer metallurgical control by minimizing refractory losses.

Cupola Operation Modifications

The type of flux material used in the cupola may be varied to develop maximum refractory life. Some types of flux are more desirable than others. Therefore, it would be desirable to study the type of flux involved and make sure that it is suitable to both the refractory and the cupola burden.

Dolomitic limestones seem to be easiest on the patch. Since limestone may vary from 0.50 per cent to nearly 16 per cent silica, the effect on refractory loss will vary with composition. Because of the larger volume required with high-silica limestone to obtain the necessary fluxing action, such high-silica stone is normally more erosive on a cupola lining than the more pure forms of calcite or low-silica limestone. Unnecessary or excessive secondary fluxes such as fluorospar and soda ash increase refractory losses. There are, of course, exceptions to these generalities because

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of such factors as glazing, sintering and slag volume. It may be necessary and desirable to modify a simple flux such as dolomite with secondary fluxes to provide the proper rate of glazing. This is particularly apt to be true with monolithic linings which are not preglazed or "fired." Slag volumes may be minimized and glazing rate improved by the judicious use of the secondary fluxes.

Monolithic patches are composed primarily of silica with some fire clay. The composition is not directly comparable to ordinary fire-clay brick. The normal reaction between this cupola slag and the lining composed of silica, clay and ganister can be expected to be different from the action between cupola slag and fired cupola block. Specifically, it usually is possible to reduce the amount of limestone when using this type of patch.

The reduced requirement for limestone does not necessarily show up as an immediate increased slag fluidity when changing from a brick patch to a monolithic patch. In fact, if the percentage of limestone is unchanged when switching from one material to the other, there may not be any noticeable change in the appearance or volume of slag. However, if this practice is permitted to continue it will be uneconomical because it may require more flux than necessary and greater than necessary lining loss. In most operations when changing to an air-placed patch, it is possible to reduce the amount of limestone used without loss of fluidity.

Slag Control

In general, it is found desirable to use 1) the highest purity limestone, 2) the smallest amount of limestone possible, and 3) in most cases a small percentage of secondary flux. It is believed that this practice is desirable for two reasons. In the first place, with this type of lining, it is desirable to use a minimum volume of slag in the cupola to avoid excessive slag attack during the formative stages of glaze. Furthermore, it would seem to be desirable to use some secondary flux to increase the rapidity of glazing of the surface. Essentially, this amounts to control of the viscosity of the slag formation on the cupola wall to minimize slag attack and loss of the protective slag coating on the lining as the glaze is formed. Adjustment of the amount and type of flux may not be essential to obtain satisfactory operation, but it usually is desirable in order to obtain the maximum efficiency and economy from the process. It is particularly important to avoid high slag volumes attributable to impure limestone. It is even more desirable to avoid the use of excessive secondary fluxes in spite of the apparent desire for a small and proper amount of such secondary glazing materials.

The amount of flux usually is discussed only in terms of its effect on slag fluidity. There is not sufficient consideration of the fact that excessive flux will result in refractory loss without compensating benefits from increased slag fluidity. It should be recognized that the refractory loss will be related to the slag volume and the amount of flux controlled accordingly. This is true for all acid refractories.

Slag volume together with slag viscosity are the controlling factors in the refractory losses of the cupola operation. There are very few cases where the actual P. C. E. or refractoriness is insufficient for the application. Unfortunately, this slag volume is not easy to determine on a quantitative basis, but a change in the slag volume will determine to a great extent the actual refractory loss. The rate of loss will be determined by the rate at which the slag reacts with the refractory and the rate at which the reaction product leaves the refractory wall, exposing new refractory for slag attack. The presence of a glaze on the lining of the cupola indicates that during the operation there was a sizeable portion of fused glass on the wall. The existence of this glass minimizes the erosion of the lining provided it is sufficiently high in viscosity. The existence of this fused glass in the surface of the lining will give it a somewhat plastic nature and help to prevent erosion as long as there is not too much glass and its fluidity is not too great. Therefore, the actual loss is directly related to the volume of slag moving through the cupola and the viscosity of the reaction product of the cupola slag and the refractory surface.

Refractory Composition Control

Knowing the normal compositions of foundry slags, the refractory manufacturer of monolithic materials tries to control the composition in such a way that the reaction product of cupola slag and refractory will be sufficiently viscous to retain a protective film for the longest possible time. However, abnormal slag compositions in the cupola will upset this viscosity relationship, as will abnormal volumes of slag.

Changes in slag volume are an essential part of the cupola operation. They are related to 1) the purity of the flux, 2) the density of the patch material, 3) the composition of the patch material, 4) the amount of impurities carried in with the metal charge, and 5) the impurities (ash) carried with the coke charge. A change in any one of these five factors can effect a change in the refractory loss.

From the standpoint of refractory losses, maximum fluidity in a slag is not desirable. It would even be safer to say that minimum fluidity commensurate with keeping a smoothly flowing slag at the slag hole is the best practice. Certainly, the foundryman should always try to obtain a minimum slag volume by using maximum purity fluxing materials judiciously balanced with the proper amount of secondary flux to control slag viscosities at the desired level. Extremely fluid and extremely erosive slags must be avoided. If this is done the refractory manufacturer can so adjust compositions to develop the proper glazing rates without danger of "over-fluxing."

From one standpoint, at least, tuyere design is related to refractory loss. This is true to the extent that intermittent box-type tuyeres will tend to remain clean and cause less bridging than will continuous or ring tuyeres. Figure 1 illustrates how bridging results in increased refractory losses by permitting whirlpools of hot slag formations to form immediately above the tuyere level of the continuous tuyere. An intermittent tuyere with a minimum space between the tuyeres of 8 to 14 in. allows the slag to flow to the well of the cupola without passing directly in front

of the cold air stream. As a result, any bridging tendency is confined to the tuyere face and a continuous ring or bridge is avoided. Good tuyere design also helps to improve refractory life by minimizing the tendency for air to work up behind the refractory and create localized melting zones at different points along the cupola wall. This means that the upper tuyere plate must be designed in such a way that there is no leakage between the tuyere plate and the shell.

Most cupolas today are designed with an overhang or ledge of refractory immediately above the tuyere. This overhang is designed to prevent metal from entering the tuyeres. However, it is surprising how often the amount of overhang is too great. Excessive overhang above the tuyeres results in a dirtier operation and more bridging. Excessive overhang seems to result in both a rapid loss of the overhanging material and also a tendency to collect frozen slag and metal in a position where it can augment or even start a bridge formation. The authors do not know of a theoretical or mathematical explanation for this condition, but have found empirically that the best results from an overhang are obtained when the area at the overhang is not less than 85 per cent of the area of the cupola at the tuyeres. In other words, the reduction of area because of overhang should not be greater than 15 per cent. This is, to many foundrymen, a surprisingly small overhang. For example, the overhang for a 54 in. cupola will amount to less than 2 in. on a side.

Contour Patching

It is not the function of this paper to cover a detailed discussion of contour patching. For complete discussion of this subject reference is made to previous papers listed in the bibliography. However, contour patching must be referred to because it has such a vital effect on the refractory life of most cupolas. There are many examples of refractory savings as great as 50 per cent resulting from this practice. The advantages of contour patching apply to any type of acid refractory including brick, stone and monolithic. Naturally, it is of particular interest with monolithic or air-placement because of the ease with which a contour can be developed and maintained. However, the philosophy and desirability of maintaining the proper contour remains the same.

One of the factors affecting refractory burn-out is the variation in bed height during the operation. This variation in bed height can be related to a number of changes including frequent shut-off of the blast, variation in blast rate, variations in coke strength and variations in melting rate. The two most common causes are 1) the shut-offs which occur at frequent periods in some foundries to conform with production rates, and 2) the deliberate altering of the blast rate to change either the melting rate, the metal temperature or the metal composition. These changes permit the bed to move up and down and thereby change the position of the hot spot of the melting zone of the cupola. They result in a long and rather marked burn-out zone in the cupola. They can frequently be recognized by evidence of two, three or even four burn-out contours at the end of the operation. Variation in bed heights may also account for bridging problems since the hot spot will change from a zone of stabilized lining toward a zone of unglazed or unstabilized lining. Rapid changes may cause a scab or "pop-off" of part of the lining.

The position and design of the slag hole are related to refractory losses. Here again two factors are involved. In the first place, the position of the slag hole has a bearing on bridging in the cupola, which in turn affects the refractory loss. This means that the slag hole must be built in accordance with normal standards of 4 in. below the tuyere level, or preferably 8 in. from the closest tuyere. There is a choice here, of course, because with a continuous tuyere it would be impossible to keep the slag hole 8 in. below the continuous tuyere. In that case it would be necessary to use the 4 in. figure, which is not quite as good. With a box-type tuyere, the slag hole can be set between tuyere openings in such a way that the closest dimension is 8 in. This provides a minimum bridging condition from slag holes. In the second place, a poorly designed slag hole may result in blow-by from the tuyeres. This will include blowing molten metal droplets out of the slag hole. A blast of mixed metal and slag causes erosion or refractory loss immediately above or around the slag hole, which in turn will weaken the refractory above and permit slumping of the patch. In this respect, it must be remembered that most refractories are in a soft, mobile condition at the surface during the melting operation. Some support is required to prevent an actual slumping or sliding during this period.

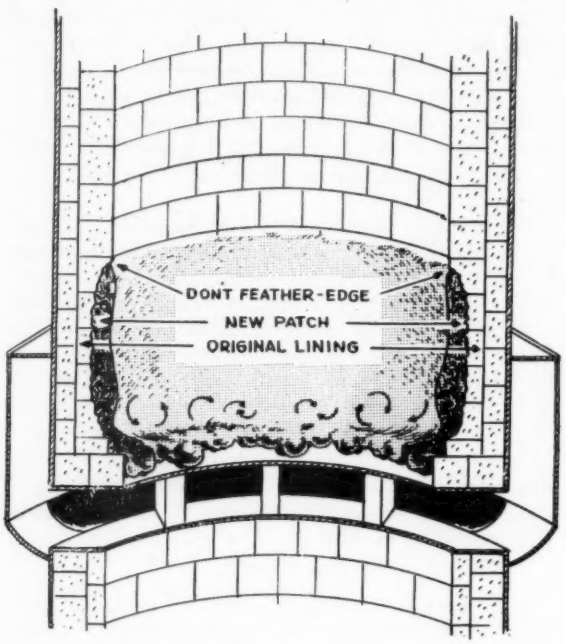


Fig. 1—The bridge forming over the continuous tuyers provides a ledge on which slag whirlpools can form, producing extreme erosion. Box tuyeres usually will relieve this condition.

The comment on slag-hole design refers particularly to the desirability of keeping the outside end of the slag hole slightly higher than the inside end in order to get a tea-pot type effect. This will reduce any tendency toward blow-by and erosion from mixtures of molten slag and metal.

One of the obvious effects of cupola practice upon refractory burn-out results from poor charging equipment. Any charging equipment or charging practice which permits most of the coke to ride one side of the cupola while the metal rides the other will reduce the refractory life. Of particular importance is the tendency to permit flux materials to contact the walls of the cupola. It is not at all uncommon to see charging equipment which places limestone toward either the front or back wall of the cupola. This practice causes uneven burn-out of the cupola and also a greater total loss of refractory.

A minor factor, but still one of importance, is the time of opening the slag hole. It is a practice in some foundries to start the cupola with the slag hole open or to open it before a bed of slag has developed in the well. This may result in erosion of the slag hole and will sometimes augment or even start a "below-the-tuyere" bridge. The resulting bridge reduces refractory life and permits gouging or cutting of the lining immediately above the slag hole, thereby weakening the patch and promoting slump or sag.

Cupola Operations Effecting Air-Placement Linings

The factors discussed apply equally well to any type of refractory patch in a cupola. In addition, there are some factors which are unusually important in an air-placed lining because they involve techniques which, to the new user of the equipment, may be different. Actually, all of these factors could be applied to other methods of patching, but they will be discussed primarily in relation to their effect on air-placement patches to emphasize the characteristics of that method of patching.

There are three phases of cupola operation, in addition to those discussed above, which are of importance or are different in the air-placement process. Among these is the preparation of the cupola, or chipping. Proper preparation is necessary for any refractory patch but the preparation required for the air-placement process may differ somewhat from previous practice. In the first place, the sand-blasting effect of the equipment used in air-placement provides somewhat greater leeway in the amount of chipping. Because this is true, there is then the danger that too many liberties may be taken with the preparation or that the operation becomes careless and leads to a poor patch.

Users of air-placement equipment are informed that in preparing a cupola two things are important. First, it is not desirable to remove the glazed finish left from the previous days operation. In other words, it is not desirable or necessary to break through the ceramic bond which is developed during the previous heat. The glazed portion is monolithic material which has been fired or sintered in place and is, therefore, the best part of the lining. It will be a hard "shell" ranging from ½ to 2 in. thick, depending upon the composition of the refractory and the intensity of heat at the wall.

Adherence of the patch is not a problem with air-placement, as it might be in hand patching of the same cupola. On the other hand, it is necessary to avoid leaving patches of slag or loose coke on the cupola wall. Therefore, the chipping operation should be checked from two standpoints. First, to avoid loose coke or excessive slag deposits on the wall which will permit the subsequent patch to drop or slump. Second, to avoid breaking through the glaze and patching against low strength, unglazed material which may not provide sufficient support to prevent spalling of the new patch. The gun operation is such that patch material will adhere to almost any solid surface, but, it must have something solid on which to adhere.

A second factor of importance in air-placed patching is the amount of support provided for the green-patch material. The refractory has low strength through a given range of low temperature before being heated to the temperature at which glazing or vitrification takes place. During this period it can support its own weight but should not support excessive burdens from above or behind. Therefore, a ledge is provided to furnish support for the load. This may be provided by the upper tuyere plate as shown in Fig. 2. In some operations it may be found that the tuyere plates have burned away to the extent that adequate support is no longer available. Or the design may be such that the tuyere plate cannot provide sufficient support because of the degree to which the refractory must overhang the tuyere plate. In these cases it is desirable to start with one row of cupola block or brick immediately above the tuyeres so that a solid formation is available for subsequent blowing or air-placement of the patch. Ignoring this problem may lead to a slumping or screening and blocking of the tuyeres during the early part of the heat. Some foundries compensate for this slump through the use of excessive secondary flux. This usually results in unnecessary refractory loss.

Contour patching has been mentioned but is of particular importance in air-placement patching, and should be stressed as one of the important factors controlling optimum results. Air-placement is well adapted to contour patching because the shape of the cupola can be controlled with accuracy and ease.

Operation of Air-Placement Equipment Effect on Refractory Quality

Aside from the composition of the material used and the cupola operation influences upon refractory life, there is also a relationship between the technique of handling the gun and the refractory life. There is a certain degree of "know-how" involved to obtain the maximum results from the process. Ignoring some of these rules may result in a low quality patch in spite of the fact that the equipment is designed to be as foolproof as possible.

Probably, the most common source of difficulty with air-placement equipment for patching cupolas is a lack of proper air pressure and air volume. The equipment is pneumatic; it transports material

through air-flow and deposits the refractory on the wall of the cupola as a result of air-velocity. The density of the patch must depend largely on the available air pressure and volume.

The theoretical maximum density of solid silica is 2.65 grams per cc. The maximum dry-packed density that has been obtained in a monolithic type material with close control of particle shape and distribution is 2.27 grams per cc, or a porosity of 14 per cent. Wet-packed density will be somewhat lower than the maximum dry-packed density. The exact density obtained will then depend upon the distribution of the particles and the particle shape plus the imposed effect of velocity and air pressure.

Water Pressure

Lack of sufficient air pressure or incomplete utilization of the air available to the machine will result in a low-density patch. It will also result in surging or slugging, which prevents proper moisture control of the patch. It should be recognized that density of patch is related to the kinetic energy or the energy imparted by velocity. The velocity is related to the size and shape of the materials being thrown, or in other words, to the flowability of the material. However, the fundamental factor is the air pressure or air volume available, all other factors being equal. The earlier models of air-placement equipment were not capable of handling materials at high pressure without plugging or bucking, but with the present designs operating pressures from 45 to 65 psi are common and desirable. This had led to some modification of the materials because the higher pressures permit the use of better compositions, and in turn change the requirements for permeability of materials.

Low water pressure, like low air pressure, can lead to a poor patch and undesirable refractory losses. The reason for the loss in this case, however, is different. Air pressure or air volume is related to the density of the patched material as placed in the cupola. Water pressure controls the ability to wet the clay particles and develop the necessary stickiness or tackiness for adherence to the cupola wall. If water pressures are inadequate, only the outside layers of the refractory coming through the material hose are wetted. The rebound is high. If the water pressures are low, there will be a tendency toward erratic wetting of the materials, resulting in wet and dry spots. The wet and dry spots, in turn, cause spalling of the lining together with apparent slump and increased refractory loss. Best results are obtained with water pressures in excess of 50 psi. Auxiliary pumps and water tanks are available to increase the existing plant water pressure when necessary.

A similar difficulty is encountered while operating with a worn material hose gasket. The material hose gasket directs the stream of material in such a way as to expose it to water to obtain maximum wetting and slaking of the clay. If the restriction or guiding gasket in the material hose becomes worn the material will scatter as it passes through the water chamber of the gun, resulting in improper slaking of the clay. The end result of a worn material hose gasket is the same as for low water pressure and may lead to confusion of the two problems. The moisture control of the patch on the cupola wall is almost automatic. If the material is too dry it will rebound, and if it is too wet it will wash off. However, even with this semi-automatic feature, there is still a range of moisture content from 8 to 9 per cent "on the wall." In some cupola operations it may be necessary to control to within the limits of this semi-automatic range. If insufficient water is used, the dry strength of the patch will be low. The effect may be compared to that from insufficient water in a molding sand (reduced dry strength). If the patch is too wet, the result may be cracking and low density. These difficulties will be emphasized if the permeability of the material being used is not properly controlled by the manufacturer because improper permeability would narrow the permissible range for moisture.

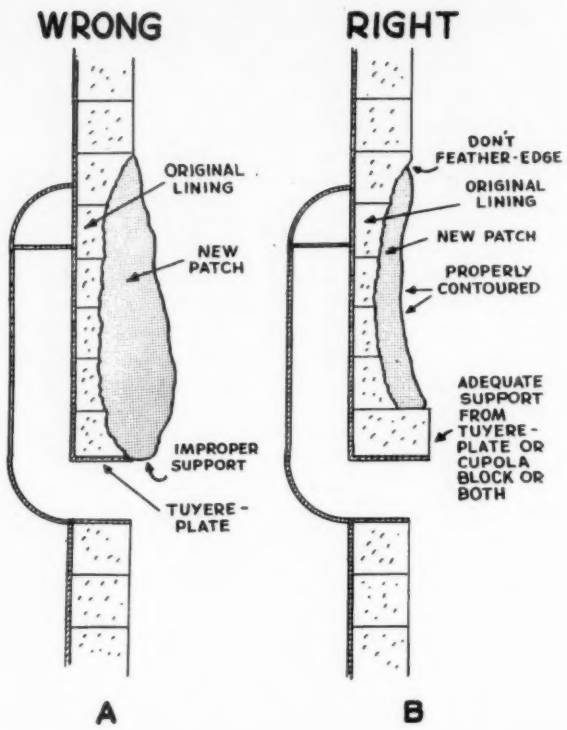


Fig. 2—"A" shows monolithic patch without sufficient support. Slumping or bridging can easily result. "B" shows how the extended tuyere plate or one row of block can provide the proper support for the monolithic lining.

There is less danger of getting material too dry than too wet. When material is shot too dry, both the rebound and the dust will be excessive. The operator will have a natural tendency to increase the moisture to reduce the dust. However, in some operations, the operator may carry this to extremes and run too wet to obtain absolute freedom of dust and prevent rebound. In most operations this change is insignificant, but in the really tough melting jobs the loss of density may be noticeable to the extent that a good patch is not obtained. The operator should avoid putting in a "soft and wet" patch followed by shooting dry material into the soft matrix to "dry it up," causing low density and spalling.

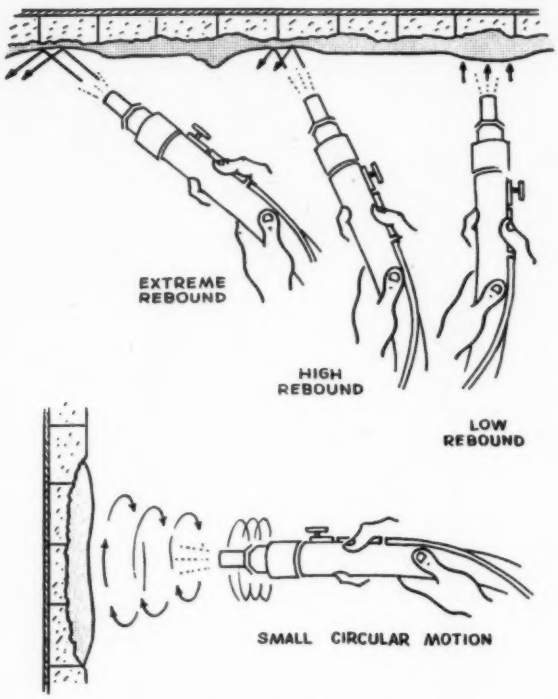


Fig. 3—The percentage of rebound depends upon the angle at which the particles hit the wall. A small circular motion with the gun head in such a way as to provide 90-degree impact provides minimum rebound.

The ability of the patch to operate with excessive water will depend partly on the severity of the melting operation and partly upon the thickness of the patch. Thicker patches normally indicate a more severe cupola operation and require less water than thin patches which can dry quickly. When properly controlled, air-placed patches require no "air dry" before using.

When installing air-placement equipment for patching, the engineer tries to train the operator to use the proper motion in handling the gun head. The proper technique of handling the gun head is difficult to describe on paper in spite of the fact that it is simple to demonstrate. However, the principles involved can be pointed out. When shooting the refractory against a cupola wall the normal laws of rebound must be considered. If the particle hits the walls perpendicularly, it will tend to bounce straight back. With a plastic mix such as that under discussion the rebound straight back will be small because the cushioning effect of the previously placed patch will allow the particle imbed itself and remain on the wall. However, in departing from the 90-degree angle and approaching the 45-degree angle the amount of rebound will increase because the plasticity of the previously placed patch is incapable of withstanding the shearing action which develops when a particle is being bounced at a 45-degree angle (Fig. 3).

The motion in handling the gun is designed to keep the particles shooting directly at the wall to minimize rebound and obtain maximum density. The easiest way to do this is with a small circular motion direction in front of the operation. Such a motion will minimize both the piling up and skidding away of material. The small circular motion working relatively close to the cupola wall will develop maximum density with minimum rebound. However, it will produce an uneven appearance, with the result that some operators will then stand back and spray at rather wide angles to get a smoother surface. The smoother appearance is undesirable and unnecessary. When materials are shot in such a way as to give a high rebound (wide angle) segregation will result because the finer particles will adhere and the coarser particles will tend to bounce away. The result of segregation on the cupola wall is a change in the composition of the material, resulting in unsatisfactory refractory life. Like hand patching, the best gun patching is not necessarily the "prettiest."

Patch Finishing

Tendencies to "featheredge" the patch in an effort to obtain a "prettier" or smoother finish should be discouraged. Some operators like to finish off the patch, toward the upper range of the melting zone, by tapering off to a knife-edge. Such a patch accomplishes nothing because the amount of burn-out at that point is too small to warrant a patch. However, it is worse than just unwarranted because the thin featheredges on the patch are subject to expansion during the drying process. The expansion and contraction will result in curling of the upper edges of the melting zones patch, pulling that portion of the patch away from the wall. Frequently, such a patch will pull away from the wall all the way into the main body of the patch. Early sloughing or slumping of the upper zones of the monolithic patch may result. It is much better to finish the patching where there is still a reasonable thickness of patch to be installed. This sill leaves an indentation or groove at the top of the melting zone, as shown in Fig. 2, but this is harmless because there is little burn-out at that point.

Gun-placed patches should never be smoothed, trowled, hammered or butted-off. Techniques of this type will cause scabbing, buckling and loosening of the adhering surfaces of the patch. Slumping, sliding or disintegration will result. Scabs, similar to sand-mold scabs, may form during heating of the patch prior to reaching the vitrification temperature.

One of the important factors in proper gun-patching is control of the direction of the planes of cleavage of layers which are permitted. Spraying or painting the patch, with a side-to-side motion (or an up-and-down motion) will result in a layer effect. If these layers are parallel to the shell of the cupola it will mean that the cleavage or planes of weakness between layers will be parallel to the melting direction of the cupola. Such a layer effect can result in spalling or scabbing-away of monolithic material as drying and vitrification progresses. Such spalling is comparable to that obtained with firestone when the grain of the stone is placed parallel to the shell of the cupola. When gun-patching spalling can be avoided by utilizing the recommended circular motion and working around the periphery of the cupola.

The patch should start at the tuyeres and build toward the top.

If the operator will stay above the work so that he is shooting downward as he is placing the patch, he will automatically place the layers vertical to the cupola shell, as shown in Fig. 4. The result will be minimum spalling and scabbing of the refractory material. For this reason the operator should be provided with proper scaffolding which will permit him to stay above the work and eliminate placing the material in layers parallel to the shell or above the gun level.

There are a few tricks-of-the-trade which can be used to further improve monolithic linings. The outstanding one is the use of secondary materials to increase the rate of glazing. Such additions may be made to the water tank used in conjunction with the air-placement equipment. When a water tank is available, it becomes a simple matter to introduce molasses in the ratio of 1 to 30, 1 to 20, or 1 to 15 (by volume) to the water. Another material of value is waterglass or sodium silicate. It can be added to the water tank in the ratio of one gallon of silicate (50 per cent purity) to 15 gallons of water. Both molasses and silicate provide a greater stickiness to the patch and reduce rebound. Molasses is valuable in this respect in that it improves the dry strength of the lining and helps to support the lining in position until vitrification can take place at the elevated temperatures. Sodium silicate has this effect and also provides an earlier sintering or glazing of the lining, thereby increasing the refractory life for many applications. Painting the patch with either molasses or waterglass has been tried but such practice is not satisfactory.

Material Composition Effect on Refractoriness of Monolithic Patches

The principal purpose of this paper is to discuss the "know-how" or technique of handling both the cupola and the cupola patching equipment to obtain the maximum benefits from monolithic patches. It should be apparent that the refractory material also plays a distinct part in the success obtained. The uniformity and quality of the material is usually the obligation of the manufacturer, but in some foundries a homemade material is used. Therefore, the foundryman as well as the ceramic engineer is interested in the effect of variations in the material being used. In most cases, the factors which lead to difficulty in monolithic refractories may be classified as "too much or too little." In other words, there is an optimum percentage for each ingredient. The optimum point depends upon many factors including density, permeability, dry strength, green strength, flowability and ceramic bond. This subject has been covered in considerable detail in papers listed in the bibliography.

The refractoriness or the refractory value of monolithic materials used in the air-placement may be misunderstood by some operators. Maximum refractoriness is not necessarily desirable. The loss of refractory in a cupola operation is more likely to be through slag attack, erosion, slumping or spalling than from actual burn-out. For this reason the pyrometric cone equivalent or P. C. E. value of the refractory is not necessarily an indication of its value as a cupola patch. In fact, it would seem desirable to control the P. C. E. value to approximately the maximum metal temperature obtained in the melting zone of the cupola. Certainly there is no point in having a P. C. E. value of over 26 or 2903 F. In some cases as low as cone 23 might be even more satisfactory.

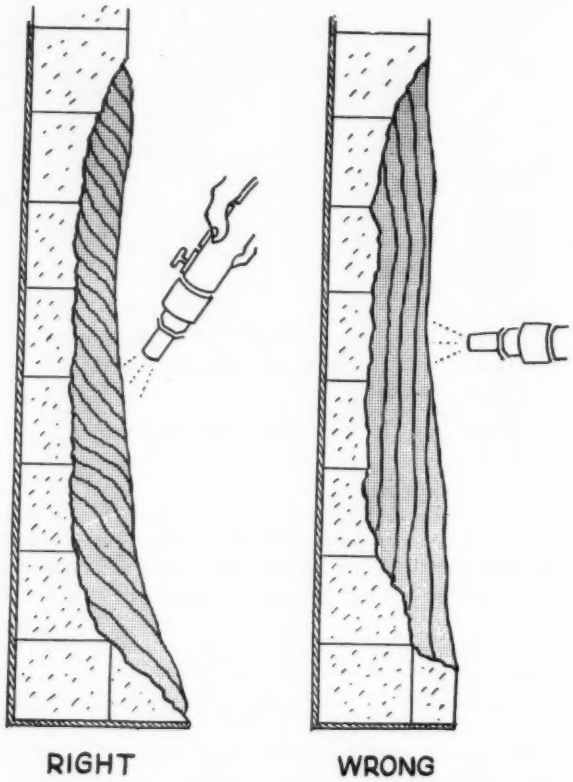


Fig. 4—By working above the zone to be patched, the operator can develop horizontal layers which minimize spalling.

In discussing refractoriness of monolithic-patch material, we should use the term in the broad sense and consider factors other than the P. C. E. value. Of particular importance is the temperature at which initial fusion takes place and the relation between that temperature and the temperature of complete slump. The ideal material for the application will glaze at extremely low temperatures but remain intact up to the temperature represented by the maximum heat of the molten metal moving through the cupola. In addition, the value of this "super" material must include the dry strength or fused strength at low, high and intermediate temperatures. The problem is much more complex than is normally realized.

Because of the mechanism of air-placement equipment, the clay used in the refractory mixes requires a definite plasticity and slaking rate. "Any clay" cannot be used with best results. The refractory value of the clay alone is not a sufficient criterion for success. The correct slaking rate is required to develop pla-

sticity in a clay during the extremely short time which is possible in a gun operation. There is no opportunity for long aging of the mixture and in many cases the cupola is being operated within one hour after moisture originally contacts the clay. Furthermore, water and clay are in contact for a period of less than a second prior to the time that the wetted clay must develop sufficient stickiness to cling to the cupola wall or the previous patch.

Slaking rate alone, of course, is not sufficient to determine the best clay because the refractoriness does enter into the problem. The ideal material must have a rather peculiar combination of fast slaking, high plasticity and high refractories. Peculiarly enough, it is also essential that clay should not be too refractory, excessively plastic, or excessively rapid in slaking. For example, an extremely plastic or quick-slaking clay may give difficulty in the gun nozzle by setting up plasticity and green strength before the material can get out of the gun nozzle and onto the wall of the cupola. The limitation in the properties of clay is a major factor in determining the correct composition of the refractory used for the air-placement of monolithic patches.

Extraneous Influences Upon Refractory Life

In this discussion an attempt has been made to cover those factors which affect the quality of a monolithic patch applied with the air-placement process. However, it would not be fair to conclude this discussion without stating that there are influences outside of the variation in 1) refractory quality, 2) method of gun operation, or 3) method of cupola operation. These influences on refractory life are particularly unfortunate because they are not always easy to identify. In many cases the influence of these extraneous factors may be confused with other influences such as the refractory quality. There have been many cases in which some coincidental influence has to lead to the belief that there has been a change in the refractory quality. To the casual observer, the two problems are almost identical. In some cases, it has required rather careful search to determine which influence was really governing the refractory life of the particular operation.

One of the outside influences on refractory life is a change in the melting rate. All other things being equal, the refractory loss will be related to the number of tons of iron melted per hour. If the melting rate is increased because of increased requirements for metal by the foundry, an increased refractory loss should be expected. This feature may be confused by the fact that the total melt of the foundry may remain unchanged. For example, even though the same number of tons are melted per day in a foundry, the refractory loss per ton of metal produced will increase if the total is obtained from an increased melting rate per hour, but for fewer hours.

Another influence on refractory life is coke quality. This may be difficult to understand because it is so hard to study. The relationship is difficult to determine because of the known lack of coke specifications in the foundry. There is a lack of fundamental information which might permit the foundrymen to interpret a change in coke quality in terms of refractory life. To demonstrate the influence of coke quality in terms of refractory life, it frequently is necessary to determine a direct relationship between a given car of coke and a given refractory.

We are not able to define the exact property of coke which influences refractory losses. The properties involved are reflected in the necessity for increased flux, a tendency toward bridging and similar indications to what the foundryman calls "dirty cupola practice." From what has been done thus far, however, we do know that the properties of coke which are related to the refractory losses will include strength and ash content. Probably, the most important is that coke be strong. Strong cupola coke, low in ash, is markedly superior in this respect to weak high-ash coke. Fortunately, these same properties which are desirable from the standpoint of refractory loss are also known to be desirable from the standpoint of metallurgical control of the cupola and overall economy.

Many observations have been made in which sudden increases in bridging and lining losses were blamed on the refractory practice, but which were ultimately proved to be curable by a return to the previous higher quality coke.

Conclusions and Acknowledgments

Because of the nature of this paper, it is not possible to summarize the conclusions in the usual fashion. The entire paper is a discussion of the conclusions reached by a number of cupola operators concerning the best operating techniques of the 1) cupola, 2) air-placement equipment, and 3) refractory mixing operation to develop optimum results from monolithic patch materials used in the air-placement process. It has been shown that these monolithic patches are subject to all of the usual considerations which affect the life of refractories used in cupola operation. In many cases the exact influence of a given factor will be different for the monolithic patch than for a prefired refractory material, but the basic relationship between cupola practice and refractory life exists. If this discussion were summarized in a single sentence, it would be by pointing out that the cupola operator can do a great deal by controlling the cupola, the charge materials, the air-placement equipment and the refractory to minimize refractory losses per ton of iron melted.

It is impossible to acknowledge all of the help obtained in preparing this discussion because the paper covers the experience of so many people. However, the authors would like to acknowledge the help of R. E. Cleland, W. R. Adams, W. W. Woodman, R. A. Green, C. P. Loucks and Robert Washam who were particularly instrumental in gathering the information during their activities in those foundries using the air-placement process.

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DISCUSSION

Chairman: W. R. JAESCHKE, The Whiting Corp., Harvey, Ill.
Co-Chairman: R. P. SCHAUSS, Werner G. Smith, Inc., Chicago.
Recorder: R. A. WITSCHEY, A. P. Green Fire Brick Co., Chicago.

GEORGE BIDDLE:[1] Do you think that higher P.C.E., higher density and lower iron content in the refractory should be conducive to less erosion?

MR. BARLOW: Not necessarily. Linings appear to be serviceable in many instances where refractoriness does not appear to justify their behavior. We recall one instance in which we took two mixes and ran 2750 F slump tests. The mix that stood up best in service did worst in the slump tests.

CHAIRMAN JAESCHKE: What are the advantages of glazing? How much overhang do you recommend? Can you give more data on the contour linings? Would you not be afraid of such thin linings on long hours of operation?

MR. BARLOW: We desire a glaze to prevent slumping. Likewise, too much of a glaze will result in greater eventual loss during the heat. We do not like to restrict the area by overhang more than 15 per cent. That is the area at the overhang shall not be less than 85 per cent of the area in the melting zone. We definitely favor contour linings. There is a limit to their use, that is, how much contouring can be done. Regardless of size and length of heat there will be less refractory loss. Experience must dictate the extent of contouring. For example, a 4-in. lining will last longer than the first 4 in. of a 6-in. lining.

MEMBER: How do you proceed when you line down a large cupola for smaller than rated heats?

MR. BARLOW: You will of necessity have to use a thicker refractory lining, but remember that the refractory loss, that is pounds per ton, will be more with the thicker lining. In general, the slower the foundry runs the higher the refractory cost per ton. This is to be expected because the reaction face is better insulated and hence slags away at a more rapid rate.

F. E. BATES:[2] Is it not advantageous to preheat the lining before placing the coke bed?

MR. BARLOW: No, it does not help, nor do we appear to get any advantage from an air drying. Monday heats are rarely as good refractory-wise as other weekday heats. A fast burn-in, even in minutes, often gives best results. In one case we actually chipped, patched and lined and had the first iron in 57 min with very good results.

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[1] Illinois Clay Products Co., Chicago.
[2] Solvay Process Div., Allied Chemical and Dye Co., Buffalo.

TRENDS FOR THE RELATION OF CHILL TEST DEPTH AND CARBON EQUIVALENT OF GRAY CAST IRONS

By

E. A. Loria*

ABSTRACT

A large number of chill test measurements and chemical analyses for gray cast irons were obtained at two automotive foundries that employed different cupola melting practices. The relation between composition and chill depth was studied so that such measurements could be more accurately charted with respect to metal chemistry and the marked effect of silicon on depth of chill more thoroughly evaluated. The customary way of plotting chill depth vs carbon equivalent for a wide range of carbon and silicon contents produced a wide scatter band of results for both practices. The data were then reviewed from the standpoint of separating the individual effects of carbon and silicon on chill depth. The selection of a good part of the data on the basis of a constant carbon/silicon ratio narrowed the scatter band considerably. The relation between chill depth and carbon equivalent is improved by the selection of data on the basis of a given carbon/silicon ratio and the important effect of a change in the carbon/silicon ratio on the results obtained is noteworthy. Another means of reducing the spread in results for the relation of chill depth and carbon equivalent is to assign a greater value to the effect of silicon than is given in the currently used carbon equivalent formula. The accumulated data were revaluated on the basis of a new parameter, carbon + 1.5 silicon. Plotting this parameter in relation to chill depth reduced the spread in results obtained for carbon equivalent and suggests that the effect of silicon in controlling chill depth is greater than has been heretofore recognized.

Introduction

Use of the chill test, as we know it today, probably began in foundries that specialized in the production of car wheels and iron rolls. The depth of chill in such castings is an important factor in controlling their service life and performance[1,2] so a rapid and simple method for controlling structural uniformity had to be developed. Modifications of the test have since been devised to suit many different types of foundries. Recently, Krause[3] reviewed the relation between chill tests and the metallurgy of gray iron. He concluded that chill and wedge tests are not a substitute for chemical analysis although they will greatly extend its usefulness. Because the depth of chill is influenced by melting conditions, as well as chemical composition of the iron, the relation between depth of chill and properties of the iron must be worked out for each particular melting practice.

Generally, the depth of chill will vary with (a) chemical analysis of the iron, (b) melting and pouring temperature, (c) degree of oxidation during melting, and (d) make-up of the cupola charge. Specific factors affecting chill depth would include (a) carbon and silicon contents of the iron, (b) amount of steel in the charge, (c) operating on a borderline iron to coke ratio, (d) time of contact between iron and coke, (e) degree of oxidation of iron and slag, (f) amount and kind of gases in the iron, (g) residual elements in the iron such as chromium, and (h) inoculation of the iron. Differences in these conditions together with experimental errors attendant to the casting of chill test specimens will produce a scatter band of chill depth measurements. Differences because of experimental errors would include those caused by variations in (a) precision of measuring chill depth, (b) temperature of the chilling plate, (c) pouring temperature of the iron, and (d) dimensions of the chill test specimens.

Although the composition of the iron is only one of the factors which affects the chilling tendency of the iron, it is a very important one. If the other cited factors are fairly well controlled, the relation between composition and chill depth is fairly good, and in this connection, carbon and silicon are the two most significant elements. It is the purpose of this paper to show how chill depth data can be more accurately charted with respect to the chemistry of the iron and to call attention to the marked effect of silicon on depth of chill. A large number of chill tests were taken as a part of two extensive investigations of the metallurgical quality of cast iron melted by two large foundries and these tests provided the necessary data for analysis. The chill test specimens were poured in dry-sand cores against a water-cooled steel plate. Specimens measured 3 × 1½ × ⅜ in. and had the 3 × ⅜-in. face against the chill plate. They were fractured and measured for depth of chill in 32nds of an inch. Drillings were taken from the base of each specimen and provided the chemical analysis of the iron at ½-hr intervals.

Producer A made automotive castings varying from 3/16-in. to 2-in. thickness with a nominal analysis

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Fig. 1—Chill test and chemical analysis blocks for ladles from which the various test program castings were poured.

range of 3.35-3.50 pct total carbon, 1.80-2.00 pct silicon. Producer B made agricultural implement castings varying from ¼-in. to 1½-in. thickness with a nominal analysis range of 3.35-3.50 pct total carbon, 2.00-2.20 pct silicon. Some pertinent information regarding their respective cupola operations can be found in Table 1. The cupola charge components employed by Producer A were 30 pct steel, 30 pct pig iron, 22.5 pct cast iron returns and 17.5 pct cast iron briquettes. Producer B used 53 pct cast iron returns, 35 pct pig iron and 12 pct steel in each cupola charge. Both used ferrosilicon and/or silicon carbide briquettes to arrive at the desired silicon specifications. A more moderate melting practice was employed by Producer B in comparison to the "hard blown" cupolas operated by Producer A for maximum production. Usually any change in melting practice which tends to make conditions more oxidizing will result in an increase in chill.

TABLE 1—CUPOLA OPERATING PRACTICE AND METAL ANALYSIS

Component	Producer A	Producer B
Melt, tons per day	130-160	100-120
Number of cupolas	2	2
Inside diameter, in.	66	72
Stock height to bottom of charging door, ft	20	16
Type tapping	Intermittent	Intermittent
Type slagging	Rear	Rear
Type lining	Firestone	Firestone
Melting rate, tons per hr	16-20	10-12
Bed height, in.	100-108	60-62
Blast pressure, oz	22-35	16-20
Blast volume, cu ft per min	8000-9850	7500-7800
Iron temperature at cupola spout, F	2750-2850	2800
Type coke	By-product	By-product
Coke charge, lb	575	375
Metal charge, lb	4000	2100
Stone charge, lb	125	80
Approx. silicon loss, %	22	12
Metal Analysis: Total carbon, %	3.35-3.50	3.35-3.50
Silicon, %	1.80-2.00	2.00-2.20
Manganese, %	0.82-0.95	0.75-0.95
Sulphur, %	0.09-0.13	0.13-0.15
Phosphorus, %	0.15-0.18	0.10-0.13

Results and Discussion

Each day, 15 chemical analyses of chill test drillings were taken at ½-hr intervals in order to produce control charts covering total carbon and silicon percentages. Running average or $\overline{X}$-R charts were plotted from the tabulated data for 24 days of cupola operation by Producer A and 10 days of cupola operation by Producer B. Photographs of the chill test fractures were taken for each heat but for the sake of brevity only the results for two heats will be included here as an illustration. Figure 1 shows the chill test fractures for a particular day when the silicon content was very much on the low side of the desired 1.87 to 2.13 pct range, 2.00 pct average. Actually, the running average ($\overline{X}$) for silicon fell well below the 1.87 pct minimum, while the difference between maximum and minimum values (R) exceeded the 0.40 maximum value during a significant part of the heat and was above the 0.18 average value for the entire heat. On the other hand, the carbon content was on the high side throughout the heat, being above the 3.50 pct maximum for the latter half of the heat and always above the 3.43 pct average. Consequently, the R values for carbon were above the 0.10 desired average, being around 0.20 for most of the heat. Figure 2 shows the chill test fractures for a heat in which a somewhat higher silicon content was realized. The running average ($\overline{X}$) for silicon fell within the 1.87 pct minimum for most of the heat and the difference between maximum and minimum values (R) were between 0.20 and 0.30 for practically the entire heat. The carbon content was well within the desired 3.35-3.50 pct range, being close to the desired 3.43 pct average. Accordingly, the R values fell within 0.05 and 0.10.

The many chill depth measurements obtained for Producer A irons were plotted against carbon equivalent (total carbon + 1/3 silicon) and it was found that the spread in results was about $\pm$ $\frac{6}{32}$ in. in the range from 3.80 to 4.50 pct carbon equivalent. This degree of variance was probably brought about by the fact that the irons were produced in "hard-blown"

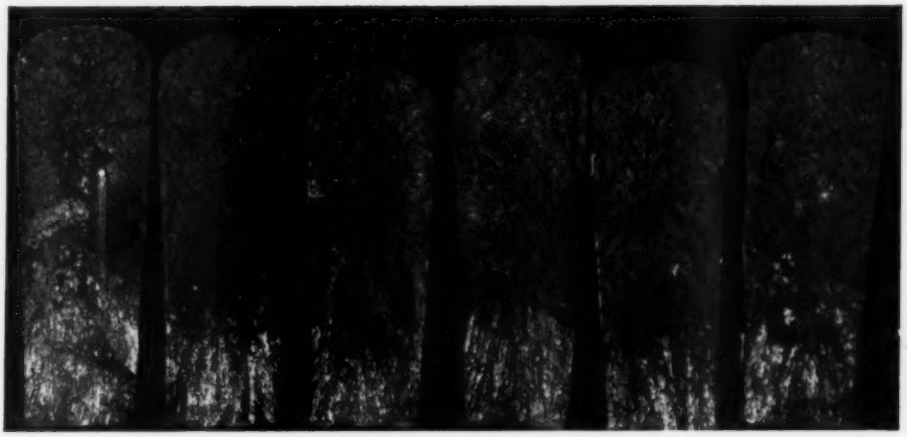


Fig. 2—Chill test and chemical analysis blocks for ladles from which the various test program castings were poured.

cupolas aiming for top production. The use of an appreciable amount of steel in the cupola charge also contributed an effect. When the cupolas were "in stride" the variations in chill depth were minimized. Generally, an increase in either carbon or silicon content of the iron reduced the average depth of chill and, as expected, the chilling tendency was therefore decreased when the carbon equivalent was increased. All of the irons were melted with the same type of acid slag whose composition varied between the limits of 29-35 pct CaO, 45-52 pct SiO_2, 9-11 pct Al_2O_3, 3-5 pct MnO, and 1-3 pct FeO. An examination of the several chill depth readings for Producer B irons showed a spread in values of about $\pm$ $^4/_{32}$ in. at carbon equivalents ranging from 3.75 to 4.40 pct. Less variation was obtained in this series of irons because less steel along with more returns and pig iron was used in each cupola charge and a more moderate melting rate practice was followed. All of the irons were melted with the same type of acid slag whose composition varied between 30-34 pct CaO, 46-53 pct SiO_2, 11-15 pct Al_2O_3, 2-3 pct MnO, 0.5-1.5 pct FeO.

A review by Hamberg[4] of the previous work on the properties of cast iron (principally strength) in relation to the carbon equivalent concluded that the total carbon content is generally three times as important as the silicon or phosphorus. As stated above, employing this parameter in relation to the chill depth measurements obtained in this study produced a rather wide scatter band of results. Consequently, the data were reviewed from the standpoint of separating the individual effects of carbon and silicon. The marked effect of silicon on chill depth, particularly for the lower carbon irons is quite evident. The effect of carbon on the depth of chill varies with the silicon content so that, at low silicon contents, the influence of carbon on the depth of chill is much greater than at higher silicon contents. These facts led to the selection of a good part of the data on the basis of a constant carbon/silicon ratio as a means of narrowing the scatter band for the relation of chill depth versus carbon equivalent. Actually, extent of the latter is shown in Fig. 3, if one considers all the plotted data irrespective of carbon/silicon ratios.

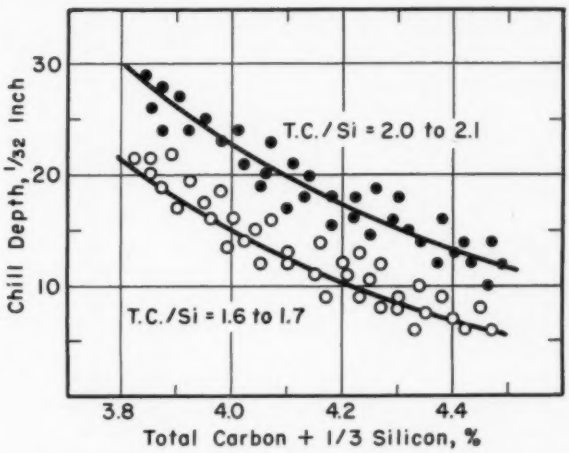


Fig. 3—Effects of carbon equivalent and carbon/silicon ratio on chill depth of Producer A iron.

The data for the Producer A irons could be segregated according to a carbon/silicon ratio of 1.60 to 1.70 or a carbon/silicon ratio of 2.00 to 2.10. Most of the data for the Producer B irons fitted well within the carbon/silicon ratio of 1.50 to 1.60. The relationship between chill depth and carbon equivalent, for a given carbon/silicon ratio, is given in Figs. 3 and 4. The important effect of a change in the carbon/silicon ratio is evident and the reduction in scatter is noteworthy. For the Producer A practice, the spread in results is reduced to $\pm$ $^3/_{32}$ in. at carbon equivalents from 3.80 to 4.50 pct. For the Producer B practice, the spread in results is reduced to about $\pm$ $^2/_{32}$ in. at carbon equivalents from 3.75 to 4.40 pct. Therefore, it appears that a more consistant relationship for chill depth and carbon equivalent can be derived by this procedure. Furthermore, a review of the entire data showed that the carbon/silicon ratio in both types of iron tended to remain rather constant when the respective operations with their own particular metal charge and slagging practices were running smoothly.

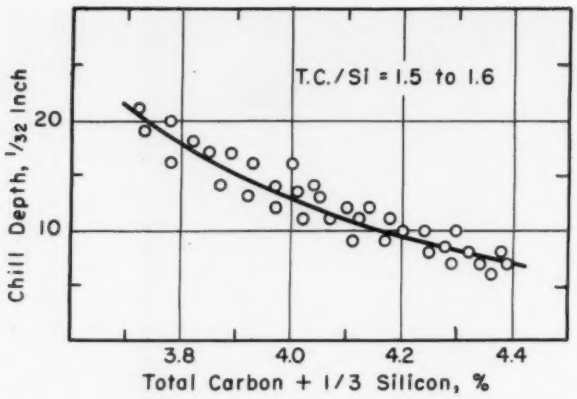


Fig. 4—Effects of carbon equivalent and carbon/silicon ratio on chill depth of Producer B iron.

Angus and his associates at the British Cast Iron Research Association[5] presented a searching analysis of the relation between the mechanical properties of cast iron and the carbon equivalent value. The reason for using the carbon equivalent (total carbon + 1/3 silicon + 1/3 phosphorus) as a basic standard was given and the correlation between it and the tensile strength or transverse strength was shown to be of value for unalloyed and uninoculated irons. In the discussion, MacKenzie[6] mentioned that carbon equivalent could not be used in relation to chilling tendency and added that the tendency to chill seems to be much nearer a function of the carbon plus silicon, disregarding the phosphorus. Notwithstanding, there has been the inclination in the literature to plot chill depth vs carbon equivalent for a wide range of carbon and silicon contents and the usual result has been, as in this investigation a wide band of chill depth values throughout the analysis range. A way of reducing this scatter band, by plotting the data according to a particular carbon/silicon ratio, has been set forth in this paper.

Since the carbon equivalent may not give sufficient recognition to the potent effect of silicon in reducing chill, the data were re-evaluated on the basis of a new parameter, carbon + 1.5 silicon. A factor of 1. 5 for the silicon was arrived at rather than a factor of 1 or a factor of 2 by virtue of the fact that it reduced the scatter band to a greater degree than either of the latter. A good part of the accumulated data was plotted in this manner and the correlation is shown in Figs. 5 and 6. For the Producer A irons, the spread in results is reduced to $\pm$ $^3/_{32}$ in. at carbon + 1.5 silicon values ranging from 5.85 to 6.65 pct. Referring back to Fig. 3, one must admit that there is still a pronounced effect of carbon + 1/3 silicon on chill depth over the entire range of carbon/silicon ratios. The trend is evident but the effect is quite variable in comparison, the spread from the average being $\pm$ $^6/_{32}$ in. For Producer B irons, the use of the carbon + 1.5 silicon factor reduced the spread to $\pm$ $^2/_{32}$ in. in the same parameter range. In comparing Fig. 4 and 6, there is undoubtedly some improvement in that the curve has been straightened out, but the width of the band has not been reduced sufficiently. However, it should be noted that the data in Fig. 4 represent only irons possessing a carbon/silicon ratio of 1.5 to 1.6 rather than the spread of $\pm$ $^4/_{32}$ in. obtained for all irons (irrespective of carbon/silicon ratio). These plots show less spread than carbon equivalent and suggest that the effect of silicon in controlling chill depth is greater than has been recognized heretofore. The trends should be verified by considerably more data and it is hoped that the numerous chill test results that have been accumulated by the foundry industry will be analyzed along the lines suggested in this survey.

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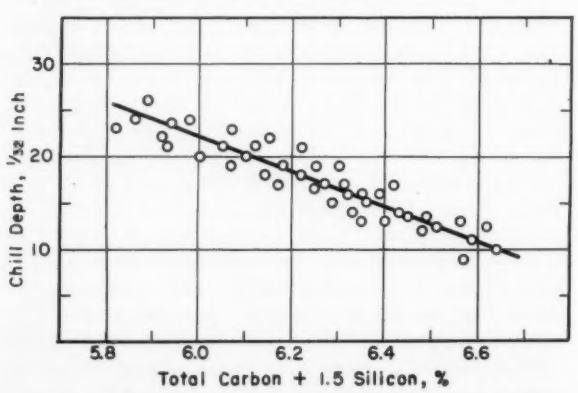


Fig. 5—Relation of chill depth and carbon + 1.5 silicon for Producer A iron.

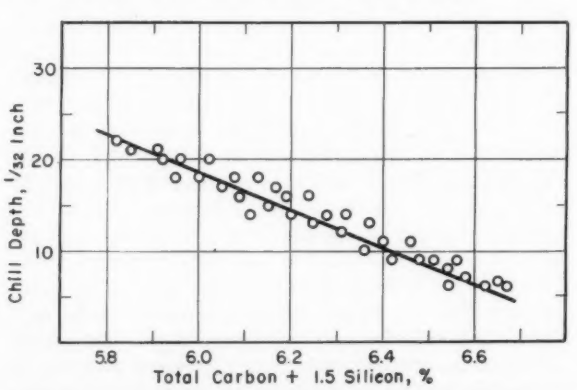


Fig. 6—Relation of chill depth and carbon + 1.5 silicon for Producer B iron.

DISCUSSION

Chairman: D. E. KRAUSE, Gray Iron Research Institute, Columbus.

Co-Chairman: F. T. McGUIRE, Deere & Co., Moline, Ill.

Recorder: D. S. EPPELSHEIMER, Missouri School of Mines, Rolla.

W. T. SHEFFIELD *(Written Discussion):*[1] The main points brought out by Mr. Loria in this paper seem to be these:

1. The ratio of the total carbon content to silicon content of gray iron has far greater significance in evaluating chilling tests than the conventional "carbon equivalent" (% total carbon + ⅓ silicon content).

2. The effect of silicon in controlling chill depth is far greater than has hitherto been recognized.

The writer's experience would corroborate both of these points. He has found the so-called "carbon equivalent" to be only a glittering generality unless the ratio of total carbon to silicon content is considered. As this ratio changes, even though the sum of the two factors be the same, different tendencies toward chilling have been observed.

It has also been observed that the effect of silicon in controlling depth of chill has been an exceedingly variable factor, even when the carbon/silicon ratio is considered.

In the second paragraph of this paper, the author lists "make-up of the cupola charge" as one of the major factors which control depth of chill. Under "specific factors affecting chill depth" he lists "carbon and silicon contents of the iron and amount of steel in the charge, as well as melting conditions." Again the writer's experience would be in accord with the author's statement.

To be more specific regarding the effects of variations in the make-up of the cupola charge, it has been observed that the conventional "carbon equivalent" is more indicative of the depth of chill when all of the silicon content is charged as pig iron and scrap, no other forms of silicon such as ferrosilicon, silicon briquettes or silicon carbide being used.

When silicon is added to the charge as ferrosilicon, silicon briquettes or silicon carbide, the effect of the silicon has seemed to be more potent than would be expected from the conventional "carbon equivalent" of the iron, just as indicated by the author.

The writer has observed at times that chill tendency also has seemed to vary with the type of pig iron being used. A 1X or 2X pig iron would not react in the same degree as a No. 3 or No. 2 plain pig.

The trends noted by the author in his logical evaluation of operational data coincide with actual experience in many shops.

The writer is a firm believer in "heredity" or "personality" of pig iron and scrap. He is of the opinion that the ultimate explanation of many of these paradoxes of experience will be found when an adequate explanation of "heredity" shall have been discovered.

P. C. ROSENTHAL *(Written Discussion):*[2] Mr. Loria is to be commended for providing such extensive data correlating chill depth with chemical composition. The lack of good agreement between chill depth and carbon equivalent shown by Mr. Loria is not surprising in view of the fact that other factors beside the carbon and silicon contents must be also instrumental in controlling chill depth. In fact, the lack of correlation in results between irons from Producer A and Producer B, as exemplified by Figs. 5 and 6 of the author's paper, indicate this. The stability of iron carbide could hardly be expected to be directly related to an empirical proportioning derived originally to relate the composition to the freezing process. Even a property such as fluidity, which might be expected to be closely associated with the freezing mechanism, has been found to correlate better with a factor other than the carbon equivalent.

E. L. LAYLAND *(Written Discussion):*[3] The author has presented two interesting concepts in handling chill depth—chemical composition data for gray cast iron to reduce the scatter of the plots of chill depth versus carbon equivalent. The typical "shot gun" characteristics of plots of chill depth versus carbon equivalent have been greatly minimized by plotting these properties for restricted carbon-silicon ratios and by assigning a parameter of 1.5 to the silicon content instead of the customary 0.33. Both of these treatments of the data resulted in splendid correlation of chill depth with the carbon-silicon composition.

In an effort to apply these concepts, the quality control records for two classes of iron (30,000 psi minimum tensile strength and 20,000 psi minimum tensile strength) produced at our gray iron foundry were reviewed. Approximately 90 sets of chill depth and carbon-silicon data were analyzed for the 30,000 psi iron and 60 for the 20,000 psi iron. Although the data were not analyzed by statistical methods, it was obvious that little if any correlation existed between the chill depth and the conventional carbon equivalent. The use of a parameter of 1.5 for silicon and segregating the data by restricting the carbon-silicon ratio did not result in any apparent improvement in the correlation. It would be interesting to apply these concepts to other data to determine whether or not the author's findings are usually applicable.

The 30,000 psi iron was produced in two cupolas tapped intermittently, one of about 25,000 lb melting rate per hour and 68-in. diam at the bosh and the other of about 22,000 lb per hour and 55-in. diam bosh. The metal charge consisted of 28 per cent pig, 34 per cent steel and 38 per cent foundry returns. All ladles of both classes of iron were inoculated with SMZ alloy, usually in amounts of 0.05 per cent to 0.10 per cent. Some ladles also had a 0.10 per cent addition of ferrosilicon.

Only a few data were available on 20,000 psi iron prior to inoculation and again no correlation was found. It is assumed that the author's results were obtained from uninoculated iron. Perhaps a good correlation by the author's methods is generally applicable to uninoculated irons and not to inoculated irons.

W. C. JEFFERY *(Written Discussion):*[4] The author should be congratulated for his contribution to the understanding of the relationship between chill and the carbon equivalent of gray cast iron. As is pointed out in the paper, the effect of silicon in controlling chill is apparently greater than that attributed to it in the usual methods of calculating carbon equivalent. Our experience with cupola-melted iron used in producing automotive castings and with iron produced in the laboratory in small cupolas, indirect arc and induction furnaces has shown that the silicon content and carbon-silicon ratio are important factors influencing chill depth.

As the author suggested in his paper, an attempt was made to analyze some chill results obtained recently in a series of indirect arc electric furnace melted heats. These cast irons varied from 2.11 per cent to 4.16 per cent carbon and the silicon content varied from 1.36 per cent to 3.22 per cent. All of these irons were poured at a temperature of 2700 F into a dry sand chill mold 4 x 1½ x ⅜ in. When chill depth was plotted against the C. E. using the suggested parameter C. E. = % T. C. + 1.5% Si, it was found that there was apparently no better correlation obtained than when chill depth was plotted against C. E. where the C. E. was considered to be C. E. = % T. C. + 0.3 (% Si + % P).

The writer would like to know if the author has had any experience in applying his suggested parameter to iron having very low and very high silicon and carbon contents.

In our recent investigation where an attempt was made to relate many of the properties of cast iron to the carbon equivalent, it was noted that under certain conditions phosphorus acted both as a carbide stabilizer and as a graphitizer. The writer would like to know if the author considers the element phosphorus important enough to be considered in a C. E. parameter.

S. F. CARTER *(Written Discussion):*[5] The author has emphasized an important principle in chill correlation.

We have never found the chill depth to correlate with the usual carbon equivalent formula of C + 0.3 (Si + P) for all types of iron.

Likewise, we have found variations in the relative effects of Si and C on our various irons. The author has suggested an important principle in considering the ratio of C to Si.

On irons with silicon as low as 1.00 per cent and C to Si ratio of 3.0 or more, we have found Si more influential than carbon. With silicon contents in the 1.60 per cent to 2.40 per cent range and C to Si ratio 1.5 to 2.0, we have considered the

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[2] Professor of Mining & Metallurgy, The University of Wisconsin, Madison.
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[4] Assistant Professor, Department of Metallurgical Engineering, University of Alabama, University, Ala.
[5] Plant Metallurgist, American Cast Iron Pipe Co., Birmingham, Ala.

two essentially equal in chill effect. On another iron with Si content near 3.0 per cent and C to Si ratio of 0.8 to 1.0 the effect of Si appears only 0.2 or 0.3 that of carbon.

We do not believe any one formula will fit all irons. As the author indicates, allowance must be made for the relative concentrations of the various elements.

Our experiences indicates that the same carbon equivalent formula can not be applied with equal accuracy to carbon saturation, strength, fluidity, and chill depth.

H. T. ANGUS *(Written Discussion):*[g] I am extremely interested in this paper, as we, too, found that the use of the chill test for foundry control cannot be very clearly tied up with the carbon equivalent value, particularly when operated in different foundries. We carried out a short investigation upon this subject some years ago, while we were carrying out the work previously published, and came to the conclusion that although the method had marked value in each individual foundry, it could not be readily tied up with the chemical composition, nor could the results be directly transferred from one foundry to another.

I think it is important to have quite clearly in our minds the limitations of the carbon equivalent value as a means of assessing the properties of cast iron. There seems no particular reason why the precise eutectic value of an iron should necessarily influence lineally such diverse properties as tensile strength, tendency to chill, structural changes when passing through the critical points, or liquids and solidus temperatures.

Riley[a] has shown that the conventional formula for the determination of the eutectic value, of ⅓ silicon, ⅓ phosphorus, applies surprisingly enough over a range of carbon contents up to 5 per cent or 6 per cent, as far as the actual eutectic composition is concerned. Evans[b] has shown that by using a modified factor, $\left(\text{T.C.} = \frac{\text{Si}}{3} + \frac{\text{P}}{2}\right)$, the liquidus temperature can be predicted with some degree of accuracy, and the work of Krause[c] and others has shown that some other factors can be quite legitimately employed in their specific contexts. These variable factors do clearly indicate the empirical nature of the relationship which we are attempting to establish, and this new work of yours adds further evidence to this point.

It is important to remember, in dealing with chilling properties, that there may be a marked difference in the response of any particular iron to a forced chill as compared with a natural chill. In the paper published by Angus, Dunn and Marles[d] the onset of chill was judged microscopically by the initial formation of free carbide, usually associated with a phosphide lake. The point at which free carbide begins to form in this manner in sand-cooled castings will not necessarily bear more than a very rough relationship to the depth of which chill will penetrate when the rate of cooling of a casting is markedly increased by the provision of metallic chills, or indeed, by the extension of the depth of chill in a sand-cooled casting.

The standard wedge and chill tests, however, serve the very important purpose of controlling the quality of the iron that is being produced from hour to hour in a given foundry, and although our experience has shown that such control tests can only be partially related to the composition, it must be remembered that even the composition itself is really only controlled in an attempt to attain reproducible mechanical properties in the actual castings. It is well known that these properties can vary quite widely for the same analysis, when, for example, melting conditions or charge composition are varied. It may well be that the standard control test, whether by forced chill or by sand-cooled wedge, will give a better index of the mechanical properties, and in particular machinability, even than the chemical analysis. The use of both methods of test can therefore be completely justified, even though the results of the one do not entirely tie up with the results of the other.

This paper shows quite clearly that the relationship of carbon to silicon is at least as important as the carbon equivalent value in estimating the effect of forced chill on a given iron, and the curves in Figs. 5 and 6 may well prove to be a very useful starting point for any foundry operating tests of this sort. It should not, however, be assumed that the same relationship will necessarily apply for a sand-cast wedge chill, and this work might very well be extended to cover this type of control test.

[g] Development Manager, The British Cast Iron Research Association, Alvechurch, Birmingham, England.

The effect of phosphorus upon the chilling tendency of an iron is, of course, much less important in the United States than it is in Great Britain, where foundries quite frequently run with a fairly wide range of phosphorus contents. Certainly, the effect of phosphorus upon chill cannot be ignored in castings containing over 0.5 per cent phosphorus. Where, however, phosphorus contents are kept reasonably uniform in all grades of castings that are being manufactured, particularly if the content is low, for control purposes it may be perfectly legitimate to ignore its effect.

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MR. LORIA *(Author's Written Reply):* The respective chemical analyses and chill depth measurements for the chill tests shown in Fig. 1 and 2 were omitted from the paper but are given here so that an understanding of the magnitude of the chill depth with metal chemistry can be obtained. The data for the chill blocks no. 1 to 8, shown in Fig. 1, are:

No.	%TC	%Si	%CE	Chill, in.
1	3.39	1.70	3.96	18/32
2	3.39	1.57	3.91	28/32
3	3.58	1.67	4.14	14/32
4	3.55	1.79	4.14	12/32
5	3.50	2.13	4.21	8/32
6	3.50	1.80	4.10	17/32
7	3.60	1.88	4.22	9/32
8	3.60	1.76	4.18	13/32

The data for the chill blocks no. 1 to 6, shown in Fig. 2, are:

No.	%TC	%Si	%CE	Chill, in.
1	3.43	1.79	4.03	17/32
2	3.30	2.07	4.07	11/32
3	3.40	2.01	4.07	7/32
4	3.48	1.92	4.12	13/32
5	3.50	1.94	4.15	8/32
6	3.45	1.95	4.10	11/32

The discussors are to be thanked for their pertinent comments. It is gratifying to have Mr. Sheffield's and Prof. Rosenthal's experience to confirm the main points of the paper. Mr. Sheffield has set forth in clear-cut fashion the importance of the raw materials entering the cupola in their effects on chilling tendency. This is another way of saying that the relation between chill depth, chemistry and properties of the iron must be worked out for each particular melting practice. Prof. Rosenthal has shown that fluidity, another property of cast iron which he has studied extensively, correlates better with a factor other than carbon equivalent.

Mr. Layland has analyzed his data on ladle inoculated irons and found no apparent improvement in the correlation. His use of silicon-bearing inoculants narrowed the spread in chill depth results and brought closer together the measurements for irons of varying carbon/silicon ratios. The late silicon addition produces an additional effect on chill depth to that indicative of the depth of chill when all of the silicon content is charged in the cupola, as was the case for the irons described in the paper.

Prof. Jeffery has certainly studied a wide chemical analysis range of high phosphorus irons and correlated several properties with carbon equivalent. Our own data pertain to very low phosphorus irons and consequently we neglected to consider it as having any effect on chill. One can also rely on Dr. MacKenzie's statement in the paper that chilling tendency seems to be much nearer a function of the carbon and silicon, disregarding the phosphorus. In relation to section sensitivity to hardness, for low phosphorus northern irons, the effect of silicon is, without question, most important and the relatively small, additive effect of the small amount of contained phosphorus is usually omitted.

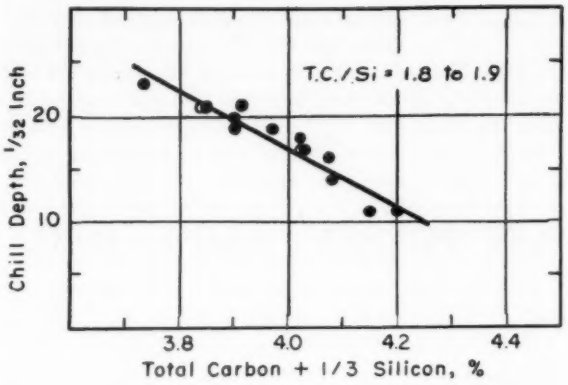


Fig. 7—Effects of carbon equivalent and carbon/silicon ratio on chill depth of a series of gray cast irons melted in the Battelle 10-in. diam cupola. Data of Krause and Lownie.

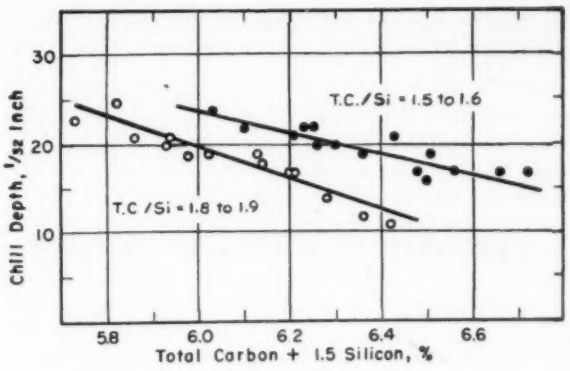


Fig. 8—Relation of chill depth and carbon + 1.5 silicon for two series of irons of different carbon/silicon ratios.

Since the chill test is devised to control the uniformity of cast iron and to predict the structure desired in a commercial casting, one might mention that tool life machinabiliy tests on ½-in. thick rings (7 in. ID and 9 in. OD) of around 0.50 pct phosphorus gray irons showed that this section was as machinable, if not more so, than around 0.20 pct phosphorus irons of the same section (casting) and otherwise comparable chemistry.[7] This would indicate that as much as 0.50 pct phosphorus produced no additional hardening effect. However, the reason for establishing a maximum of 0.20 pct phosphorus in automotive cast iron is that an excessive amount of phosphorus tends to promote shrinkage. Other drill penetration test data on step bars comparing 0.20 pct phosphorus iron with 0.70 pct phosphorus iron showed that drillability was lowered 10 pct in the latter which would mean that above 0.50 pct, phosphorus would have a hardening effect. Of course, this is tied in directly with section size and metal chemistry. In the relatively small chill test casting, the conditions would be appropriate for producing an additional chilling effect from high phosphorus in low carbon equivalent irons. Since a phosphorus of 0.70 pct actually means a steadite content of 7 pct by volume, it is appreciable quantity of a micro-constituent that would be expected to impair machining properties, especially if the cooling conditions are critical and the graphitizing elements in the iron are low. In a chill test specimen, a high steadite content would be partially entrapped with the carbide and might be read as part of a higher chill depth because of its somewhat similar appearance to carbide.

It should be noted that Prof. Jeffery's data are for irons melted in the electric furnace. In such a melting unit, the graphite nucleating conditions are different than occur in a cupola where the iron is in contact with an excess of carbon (from the coke) at all times. Superheating in the electric furnace produces metallurgical changes influencing chill and mottle formation particularly with respect to manganese sulphide inclusions, increase in chill crystal formation, and changes in oxygen and nitrogen contents.

Mr. Carter has confirmed the variations in the relative effects of silicon and carbon on the chill depth of different irons with some actual data. He has expressed the opinion that no single equation will fit all irons and that the same carbon equivalent formula cannot be applied with equal accuracy to chill depth and other divergent properties of cast iron. Dr. Angus also stresses the limitations of the carbon equivalent value and presents an excellent discussion of the subject. His comments on forced chill and sand-cast wedge chill are well taken. An extension of the present study covering sand-cast wedge chill tests will be presented in the near future. The producer A irons were also poured into two wedge sizes cast in open core, the smaller, more sensitive chill test was a V-bar about 1¼ in. deep by ¼ in. at the top of the V and knife-edge at the bottom and 6 in. long. The other wedge, illustrating the effect of more mass on chilling tendency, was 4 in. deep by ⅞ in. at the top and 5 in. long. Both the small and large wedges were cast from the same iron after holding the metal in the ladle to various temperatures below 2500 F, as measured by an immersion pyrometer.

Since the paper was written, the data contained in Table 5 of Krause and Lownie's 1949 AFS paper on the effect of moisture in the blast on the properties of cast iron melted in the Battelle 10-in. diam cupola have been plotted and the relation between chill depth and carbon equivalent for those irons having a carbon/silicon ratio of 1.8 to 1.9 is given in Fig. 7. The individual values are close to the trend line. Plotting the same data, employing the parameter carbon + silicon, produces the pattern shown in Fig. 8. Again, the data appear to follow a straight line and the width of the band is rather narrow. Also included in Fig. 8 are data for another series of irons melted in the Battelle 10-in. cupola, being a part of a more recent unpublished study covering the effects of oxidizing and normal cupola operating practice on the properties of cast iron. Plotting the data for those irons possessing a carbon/silicon ratio of 1.5 to 1.6 produced the trend shown by the upper line in Fig. 8. Again, the values seem to fall pretty close to a straight line function.

In both investigations, the chill test specimens were poured in core against a horizontal water-cooled plate. They had a $\frac{5}{16}$ by 3-in. face against the steel plate and were 1½ in. high. The depth of solid chill from the cooled face was reported as the chill depth. In the Krause and Lownie study, the relationship of chill depth to carbon equivalent showed that chemical composition was the main factor controlling chill depth, the scatter band for all tests being from 3.80 to 4.20 pct. The results for the second study showed a similar trend, the scatter band for all tests being about $\pm$ $\frac{3}{32}$ in. for 3.60 to 4.15 pct carbon equivalent. In the former, the 10-in. cupola charges contained 37 pct steel, 31 pct malleable pig and the cupola was operated with various amounts of moisture in the blast; in the latter, 30 to 50 pct steel, 48 to 60 pct malleable pig with changes in materials and operating techniques to produce both oxidizing and normal melting conditions. Because of the effects of variations in the make-up of the cupola charge (particularly with respect to the type of pig iron and scrap) as well as the individual melting conditions, chill test results can only pertain to a particular melting practice and variations can be produced that are not explained by the carbon equivalent factor alone.

TRENDS IN DUST CONTROL—PAST, PRESENT AND FUTURE

By

Knowlton J. Caplan*

There are usually no sharp dividing lines between past, present, and future where technical and industrial developments are concerned, and even if such dividing lines should appear from the broad historical viewpoint, we are unable to recognize them when they are happening. The writer should like to present, instead, on a philosophical rather than on a technical basis, a picture of the transitions that are occurring in this field.

In the broad consideration of dust control and allied problems, several different aspects should be considered. Let us discuss the picture in its following aspects:

Attitudes of management, labor, and the general public.
Available design data and knowledge.
Practice in design procedure.
Educational aspects—with particular regard to engineering.
Equipment trends—in dust-producing equipment as well as in dust control equipment.
The results obtained from the above factors.

The Past

There is not too much benefit to be gained from dwelling at length on the past history of dust control. We are all aware of the mistakes, both technical and in basic conception, that have been made and the resulting misconceptions and poor design that have become rooted in our practice and are offering some difficulty in the overcoming. We probably are less well aware of the work of the pioneers, and especially the "second generation" of pioneers who finally put us on the right track.

We have almost completely left behind the old attitude symbolized by the feeling that smoky chimneys were a sign of activity and progress. Not yet completely behind us, but on the way out, are the scare attitudes which were manifested soon after widespread and active interest in dust problems began in this country, in the early years of the century. During the scare days, management consistently denied the presence of any dust or fume problem on a categorical basis; labor, equally categorically, was sure that any and all dusts were killing workmen by the thousands, but frequently demanded extra "hazard pay" rather than the correction of the problem. Industry resisted the admission of any degree of hazard, either health or economic, from the dusts and fumes which were regarded as an integral and natural by-product of the operations.

Early Dust Control Problems

Design procedures were poor, plants being constructed usually with absolutely no regard to the dust control problems. What dust control was installed was added afterwards, in make-shift fashion, at high cost and frequently left much to be desired in the way of results. General ventilation, rather than local exhaust ventilation, was the mode of approach frequently used; technical papers were even written about the ventilation to control the hazards in British dye works, for example, and the ventilation discussed was natural draft general ventilation.

What technical data were available were often entirely empirical, and often based on a mistaken conception, we know now, of the primary factors involved in the problem. We are all familiar with old code requirements—in fact, we have some of those codes still with us—where the ventilation of grinding wheels was based on a criteria of static suction at the throat of the hood. A little analysis reveals, of course, that a higher static suction could be obtained by completely blocking the hood, preventing the flow of any air whatsoever, than would be obtained with adequate capture volumes of air. Grinding wheel exhaust systems have been deliberately designed with restrictions to the flow of air in the hood, in order to meet these codes, with a resultant higher capital cost and operating cost than would be necessary to do an adequate job of dust collection. Similarly misconceived are general ventilation rates based on "air changes per hour," a basis which completely ignores important factors such as ceiling height, room dimensions compared to the dust producing operations, etc. We still occasionally resort to an "air change"

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basis of design, but only when forced to through ignorance of the real factors involved.

Dust-producing equipment, of course, was expected to produce dust. Whether or not anything was done about it was strictly up to the purchaser or user. Dust control equipment was available to handle most different types of problems, but was usually bulky and expensive on the one hand, or none-too-satisfactory on the other. Developments of major importance in dust control equipment were practically nonexistent for many years prior to the early 1930's.

The result of these factors, of course, was in general a pretty poor picture with respect to dust control and atmospheric pollution. However, in pointing out the flaws and mistakes of the past, we must not lose sight of the fact that out of this situation and the efforts of the people involved, grew the present quite optimistic situation, where we are in the beginning years of a renaissance in the field of dust control.

The Present

Attitudes—At the present time, we find enlightened industry quite ready to admit the potential hazards, both health and economic, of the dusts, fumes, and atmospheric pollution associated with industry. Quite a few industries point with pride to their accomplishments in the elimination and control of such problems, and show resistance to the admission of the problems only when an unreasonable, rabble-rousing, non-scientific demand is made. Industry is less afraid of dust now than in the recent past, and more ready to do something about it. Let us not forget that industry must foot the major portion of the bill when it comes to the correction of dust and pollution problems, that such bills are not small, and we should therefore give a very large portion of the credit for past, present, and future accomplishments to industry itself.

We have passed the days when every dust was hazardous, and are now in a period where the demand for scientific evidence of health hazard or economic damage is frequently overdone. This is a temporary situation. The author believes, because he is convinced that an eventual realization will grow among the industrial leaders, labor, and the public that this is frequently too short-sighted a yardstick. Even though it might not be possible to prove pathological damage from a certain dust or atmospheric pollutant, or to put a dollar sign on the damage it does, it seems rather self-evident that excessive amounts of any unnatural material in the air cannot be regarded as beneficial, and is most likely harmful to a slight, though unmeasurable, degree. Perhaps in the aggregate, the degree is not so slight and not so unmeasurable.

A clean, dust-free working environment, with good housekeeping, increases the efficiency of labor, improves morale, causes workmen to be neater and more careful, and even permeates into the homes and personal lives of the men. This is psychologically sound, and the writer has personally observed it to be true in several instances. This attitude and knowledge, slowly but surely, will spread until it is a well recognized and practiced part of industrial management.

Available Design Data—The status of the technical design data is considerably improved, with quite a body of good design data tabulated and published for the foundry, woodworking, metal finishing, rock crushing, quarrying, mining, and materials handling industries, among others. Although a large portion of this information is empirical, it nevertheless is perfectly usable and produces good results if applied with a generous leavening of common sense and experience.

One hears complaints about the lack of fundamental design data, and experiments are in progress at various institutions to provide more such data. However, good empirical data is frequently more usable than fundamental data, because it is too difficult to determine quantitatively the fundamental facts necessary to apply the fundamental equations. For example, the thermal updraft over a hot shake-out is a function of the size, skin temperature and shape factor, among other things, of the hot castings, and if these factors were known, it would be possible to calculate the velocity of the dust-laden thermal updraft.

However, in a case such as this, it is more practical, easier, and more certain of results to actually measure the updraft from the shake-out in question; or if a projected installation, to predict the updraft on the basis of experience with existing installations. The determination of the fundamentals is valuable, however, in the sense that the nature and approximate relationships of the variables involved should be known to the design engineer in order that he may properly apply the empirical data, and to ensure that the empirical data is of sound conception and based on the important variables.

An example of this was the work of Brandt at the American Society of Heating and Ventilating Engineers Research Laboratory which verified, refined and extended the early work of DallaValle on velocity contours and entry losses for common types of hoods. Further work has been done at the Harvard Air Cleaning Laboratory on air cleaning devices, and work is also going on at the University of Pittsburgh under the direction of Hatch, as well as at several other institutions. The American Society of Heating and Ventilating Engineers is supporting research in the general field, and other engineering and technical societies such as American Foundrymen's Society, American Society of Mechanical Engineers, American Society of Chemical Engineers, and American Standards Association are showing a re-awakened interest through the holding of symposia and appointment of special committees. Both laboratory and field research is being conducted to a greater degree by air cleaning equipment manufacturers.

Practice in Design Procedure

At the present, the real and urgent need is not for more data, but for more widespread and intelligent utilization of the data and knowledge which we al-

ready have. Present data are adequate for the vast majority of problems, but are not used on these problems. Those engineers who specialize in dust control are too few in number, and all too frequently are not called upon. The general all-around plant engineer and design engineer is well equipped in the fundamental engineering knowledge to do a good job of dust control, but usually lacks the ready access to good design data. Also, by virtue of the fact that only rarely does he come in contact with such work, he is unfamiliar with the methods and techniques required in order to bridge the gap between the evaluation of the dust problem, and the necessary conception of ways and means to establish control.

Make Information Available

The best apparent means of correction is to provide more widespread education and dissemination of knowledge of these techniques. The American Foundrymen's Society, in 1938, made a good start with their "Fundamentals of Design, Construction, Operation and Maintenance of Exhaust Systems." This publication is now being revised and brought up to date. Two or three years ago the American Conference of Governmental Industrial Hygienists published their Ventilation Manual, an outgrowth of a manual originally prepared for Michigan industry by the Michigan Department of Health. This manual has sold over 3000 copies. Several good books have been published in recent years.

A new American Standards Association code on Fundamentals of Exhaust Systems is in preparation. A short course in Industrial Ventilation is being given once a year at Michigan State College; night school courses are being offered at Washington University in St. Louis and probably at other schools. Special lectures are being given in general design courses in the engineering schools to at least inform the young engineering student that there is a useful body of knowledge in this specialty. Thus it is apparent that we have a good start toward the education of the plant engineer and design engineer, and this is well, because surely they are the men who actually design the vast majority of dust control systems. The specialists never see most of the jobs.

Modern industrial practice and procedure is now beginning to show signs of incorporation of dust control thinking and design in the original plant design, rather than as an after-thought. Not too infrequently nowdays, one hears of a plant so constructed, and this was almost unheard of not too many years ago. True, many such plants are in those industries which have a well recognized problem of long standing—such as the foundry industry—but in time the practice is bound to become more widespread because it is so logical and so economical.

The writer recently had the privilege of being associated with the design and construction of a plant for handling and processing tonnage quantities of a raw material used by the Atomic Energy Commission. Dust "tolerance"—the maximum allowable concentration of dust in the air in working areas—was established by the AEC at 50 micrograms per cubic meter of air. This concentration is roughly equivalent to 1 lb of dust dispersed in 675 million cubic feet of air. When the plant was complete and in full operation, a complete dust study was made. Results indicated that the operators exposure was between 20 pct and 80 pct of tolerance.

There are two morals to this story. First, it was proved possible, by means of careful dust control design and intelligent process engineering with the dust problem constantly in mind, to design dust control to "specifications." Secondly, it was proved possible to achieve such a low dust concentration in an economical way. Such concentrations had been achieved before, but only by resorting to full-remote control operations, with the workmen separated from the operations by solid walls, known as "canyons" in AEC parlance. Canyon design is effective but very expensive to build, operate, and maintain. On the other hand, less-than-adequate dust control would have required expensive revision after the plant was in operation, and might have meant harmful exposures to workmen meanwhile.

One significant part of this trend is that industrial hygiene engineers and safety engineers must become more familiar with, and more competent in, the actual design engineering phases of their art. They must be able to explain to the design man the various ways in which the desired end can be accomplished, and in a quantitative way, including the cost considerations. Only by indicating to the plant engineer some degree of knowledge and competence in the things familiar to the plant engineer, will respect be engendered concerning things he is not familiar with —in this case, engineering correction of dust and fume problems. No longer will it suffice for the safety engineer or the industrial hygiene engineer to make a study of a dust problem, and say "This is a hazard, please fix it." He must also be prepared to help in the fixing.

Equipment Trends

The present picture also shows noteworthy advances in the two categories of equipment of concern—that which produces dust, and that which controls dust. Dust producing equipment, such as grinding wheels, mixers, conveying systems, packaging systems, pulverizers, textile machinery, etc. are now being offered in designs which largely eliminate the dust generation, or at least provide built-in dust collection hoods and sometimes built-in dust collectors. The manufacturers of such equipment have no doubt found that they are not merely contributing to the well-being of their fellow man, but that they also have a good selling point and a sales advantage over equipment not so designed. One grinding wheel manufacturer has even gone so far as to provide built-in filters for the removal of the grinding wheel dust from the exhaust air. True, these filters are of the stationary, non-cleaning type and require frequent changing—but that problem is a challenge to the makers of dust collection equipment, and the grinding wheel manufacturer certainly has the right idea.

Modern, high-speed machinery has tended to intensify and concentrate dust sources, requiring the installation of more, and better, dust control equipment. Emphasis on quality control in the industrial plant has also created a stronger need for cleanliness and dust control.

Dust collection equipment has seen some noteworthy advances during the last ten years. All types have seen improvement, of course, but there have also been some really new types developed. In wet scrubbers there is a new extension of types of duty and higher efficiencies than had been achieved previously by wet methods. The fog filter is a similar advance using a different principle. In filters, the Hersey reverse-jet developments offer considerably higher flow rates at constant resistance with considerable saving in space.

The A. D. Little asbestos paper, a throw-away filter developed during the war for the government, offers a very high efficiency and is suitable for very light "air filter" loads. The Millipore filter developed by Goetz for bacteriological work, is a challenging new tool for sampling and analytical work although not yet applied on a commercial air-filter scale.

In mechanical collectors, such as cyclones, there have been numerous improvements in recent years but nothing that the writer would personally classify as really noteworthy advancement. In this connection Kane has said (paraphrased)

"Don't expect collectors employing principles used for lower efficiency primary collection to be developed that will give efficiencies equal to those of more expensive high efficiency units. * * * The history of new designs of collectors using, for example, centrifugal forces in a dry unit are one of many expensive misapplications based on the hopes * * and the expectation * * * that by some twist of a piece of sheet metal fine dust particles will forsake the laws of aerodynamic behavior."

The composite picture of the various facets of the present situation, then, is extremely good. We see considerable improvements over past mistakes, and the trends in all categories seem to be in the right direction.

The Future

The future I hestitate to predict except in extrapolation of trends seen at the present. I would expect the art, science, and business of dust control to continually improve. Its improvement should be very rapid, for as a whole the writer feels it is somewhat behind many other aspects of our industrial economy. We must not forget, however, that it is industry that pays the bill, and unless industry prospers and grows in general, dust collection cannot. A possibility exists that dust control and atmospheric pollution work, because it has health and social significance, might undergo some pump-priming in one form or another by the government during times of economic recession. One could not say, however, that the art, science, and accomplishments in dust control will really advance unless industry as a whole advances.

The future should see fewer departures from the scientific and economic facts than we have seen in the past. Some of these facts, especially with regard to air pollution, may be very broad in their implications, difficult to get, and apply to industry as a whole rather than to any one plant. Many and difficult are the unsolved problems in air pollution—in fact, some of them are not even clearly defined. For example, those who work in downtown locations have probably wondered, on occasion, just how much of the "pollution" blowing into their eyes and settling on their desks is due to stack emissions of industry, and just how much is due to abrasion of rubber tires, steel wheels on tracks, brake linings, trash burning, etc. This is but typical of the many ill-defined problems we face. If all industrial stacks were clean, would the problem be solved? Should we all work out in the country and suffer the isolation and transportation difficulties? Must we provide a filtered air supply for every space we want to keep reasonably clean? Should we do nothing, and suffer the consequences? If we do nothing, how serious are these consequences likely to be?

Importance of Dust Control

Design data will become more refined, more complete, and will reflect a more proper balance between empirical data and theoretical fundamentals. As the importance of dust control in our economy grows, more and more high grade practitioners may be expected to enter the field, thereby further improving our lot. More widespread education of all engineers is to be anticipated, until the day arrives, we hope, where the plant engineer handles a dust collection problem in as easy and familiar a way as he now does a piping or electrical installation. Management will, we hope, demand that maintenance of dust control equipment be given the same attention now given to production machinery. Dust control will become more and more a portion of plant layout and design, just as is lighting and general ventilation frequently at present.

In the equipment categories, we may expect more and more "dust producing equipment" to be furnished with built-in dust control of one form or another. Dust collection equipment still has a number of interesting challenges to meet— the problems of efficiency, high temperatures, cost, space, and convenience. Dust collection, usually not an integral part of a process, must be regarded as an overhead item—and therefore provision of dust collection equipment ought to be as easy as hanging a new light fixture.

The overall result and goal, let us resolve, shall be a significant contribution to the well being of industry and those who work therein and live nearby.

DISCUSSION

A. G. GRANATH *(Written Discussion)*:[1] There is little doubt that a definite trend by States and Local Communities to clean up health hazards in and out of foundries has been under way for some time. Most foundrymen although they realize this are somewhat at a loss as to what to do and how far to go; therefore would like to have answers to three basic questions: *First*, how far will this trend go in their particular community; *Second*, to what extent will local and state regulations be enforced and

how will it affect them, and *Third* what can they do now to help themselves.

It is hardly necessary to comment on the first question as most of us know to what extent some communities have gone to in the formulation of regulations governing health hazards. What has happened in these communities could very well happen in your own. More stringent regulations are almost certain to come if they have not arrived already, but as to the extent of these regulations time alone will answer.

Likewise most of us have an idea, although we hate to admit it, as to the answer to the second question. More and more frequently do we hear of foundries that are in trouble either because of health hazards in the foundry or smoke and dust emissions from the foundry. A number of foundries have been quite fortunate for some time due to the location of their plant with respect to the community or possibly a friendly employee relationship. How long these conditions can exist is hard to say. We do know that many foundries that were located on the outskirts of a city or town a few years ago are now surrounded by homes or other business establishments, each of which becomes a source of possible complaint. By the same token many foundries that have a friendly employee relationship today may be in trouble tomorrow as the labor situation changes.

The answer to the third question—"What can foundries do now to help themselves"—will vary, depending primarily on plant location with reference to the community and the degree of enforcement required of prevailing regulations. Basically I believe all will agree that the first place to start is *inside* the foundry. Many foundries today could greatly improve health hazards prevailing in their plants, and by so doing would remove the pressure of enforcement on neighbor foundries that have made attempts to help themselves. It has been the practice for years—and still is in the majority of foundries to ventilate by means of stack fans in the roof. This is not a desirable condition as the contaminates are permitted to spread freely throughout the plant before being removed while the objectionable fines that constitute the greatest health hazards are permitted to settle out in the foundry like in a large settling chamber, and deposit on the floor or roof beams where they can be stirred up again with little provocation. The correct way to exhaust is to catch the contaminates at the source before they have the opportunity to spread.

Other plants have mechanized, some to a small extent and some to a larger extent. They have done this because they can effect a calculated savings on their investment, and at the same time obtain the benefits of concentrating the dusty operations in a manner that will permit exhausting at the source of the dust concentration. Exhaust equipment to some is considered to be of secondary importance as they believe an investment for this type of equipment will give them little or no return. This, of course is not true as there are many intangible savings that can be affected such as better working conditions, better neighbor and employee relationship, and insurance against health hazards. Some install fans or simple cyclones to clean up inside the foundry with good intentions of installing proper exhaust equipment later on the savings affected by the mechanized equipment. This line of reasoning, while not strictly ethical in some states and communities is to be commended above a do-nothing-at-all attitude. There is no question that exhausters and simple cyclones are equally efficient as the more expensive high efficiency type collectors for cleaning up the contaminates inside the foundry. The main difference lies in the condition of the air as discharged to the atmosphere. High efficiency type collectors discharge relatively clean air while the exhausters and simple cyclones discharge air that is contaminated to the extent that many state and local regulations do not permit their use.

Stack fans are an inexpensive means of cleaning up a foundry, but they should be used with caution and confined to the exhaust of smoke and fumes only, preferably at the source. They should not be used for handling air with a high concentration of silica fines.

The A.F.S. sub-committee on ventilation inside of foundries realizes the need of a simple practical manual that will assist foundries to first identify which contaminates constitute a health hazard and then catch these contaminates at the source. Considerable work has been accomplished along these lines and it is the hope of the committee that soon a manual of good practice can be published, confined to foundries only, that will give simple definite recommendations as to the volume of air required for various conditions, hood construction, etc., and all data necessary to design and maintain a complete exhaust system.

[1] Vice President, National Engineering Co., Chicago.

PROGRESS MADE IN FLUIDITY TESTING OF MOLTEN METALS DURING THE LAST TEN YEARS

By

A. I. Krynitsky *

Introduction

Fluidity testing of foundry alloys was ably discussed by Clark[1] in his official 1946 exchange paper from the American Foundrymen's Society in the Institute of British Foundrymen. The purpose of the present discussion is to review the progress made in fluidity testing during the last decade.

It is well known that the term "fluidity", as applied to the ability of the molten metal to fill a mold cavity, is a misnomer. The "fluidity" which is the converse of viscosity, is a fundamental property of liquid, whereas the quality of a molten metal to fill a mold depends upon a number of factors of which its fluidity is only one. For this reason different authors coined their own terms for this property of the molten metals. The term "coulabilité," originated by Portevin[2] has been adopted by French workers. The terms "running quality", "running capacity", "runability", "castability", "flowability", "flowing power" were used by American and English authors. Many writers, however, continue to use the old term "fluidity" and it seems to be the most common in foundry terminology. It is understood that this "fluidity" is not the fundamental property of the molten metal but is simply a foundry term which will be used throughout this paper in broad sense.

When liquid metal is poured into a mold, it flows through the more or less intricate design of the mold cavity as a result of its hydrostatic head pressure. A number of factors combine to oppose this flow. The following main factors were listed by Portevin[3]:

1. Factors related to the shape of the test piece;
2. Those related to the mold;
3. Those depending on pouring; and
4. Factors depending on the metal.

For pure metals the effect of these factors on fluidity was expressed by Portevin and Bastien[4] as follows:

$$l = a\frac{Cd\,(T-F)}{F-t} + \beta\frac{Ld}{F-t}$$

where l is the length of the casting (spiral)

a and β — Factors depending on the nature of the mold and on the viscosity of the metal

d is the density of the metal.

C—Specific heat of the liquid metal.

T—Casting temperature.

t—Temperature of the mold.

L—Latent heat of solidification of the metal.

F—Temperature of solidification of the metal.

Portevin and Bastien[4] established that fluidity varies inversely with the solidification range and depends on the crystal form of solidification, e.g., it is greater when the liquid forms crystals with convex faces than if it forms dendrites.

The nature of the mold and the technique of casting are of great importance, but as Evans[5] states, the effects of these factors on the fluidity of the metal can be neglected if a sufficient degree of standardization of technique is adopted. The physical properties of the metal which are concerned are placed by Evans in the following order of importance: 1. Surface tension; 2. Solidification temperature; 3. Viscosity; 4. Specific heat; 5. Solidification range and form of crystallization; 6. Thermal conductivity. These properties all depend entirely upon composition and temperature.

In Ruff's comprehensive paper[6] on fluidity of liquid malleable iron and steel, different fundamental physical factors which may effect the flow of the liquid metal were considered. The discharge velocity of the flowing metal, drop in velocity in relation to the length of run and diameter of flow, turbulence phenomena, and resistance to flow index in relation to the Reynolds' number were discussed at length. It may be noted that according to Reynolds[6], all flow phenomena under dynamically similar conditions are characterized by the fact that the product of the velocity and the diameter of the flow channel divided by the kinematic viscosity always gives a constant. The resistant to flow in a mold, Ruff remarks, increases rapidly with the velocity of flow, with the diameter, and with the Reynolds' number.

Although considerations above are of a great theoretical importance, it is doubtful that mathematical analysis can solve this practical problem. The writer

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is in complete agreement with Clark[1] that, at least for the present, the only convenient method of evaluating fluidity is an empirical one.

It is to be remembered, of course, that carefully standardized test conditions must be established and maintained throughout all tests and that values obtained are only relative.

Many practical methods for measuring fluidity of molten metals were advanced by different investigators. It appears that the most commonly used methods are as follows:

1. Spiral; 2. Straight rod type and 3. "U" fluidity test pieces.

Spiral Types of Fluidity Test Piece and Their Modifications

The Saito and Hayashi[7] idea of using a spiral piece for measuring fluidity of molten metals is still considered by many investigators as a sound idea.

It is difficult to ascertain which type of spiral fluidity test piece is now most commonly used in foundry practice. It appears however, that during the last decade the following three spiral test methods were most used by the various investigators: 1. The Bureau of Standards[8]; 2. Taylor, Rominski and Briggs[9] and 3. Eastwood and Kempf.[10]

None of these methods were used in their original form. Every investigator found that some modification of the method chosen by him had to be made in order to secure more constant results.

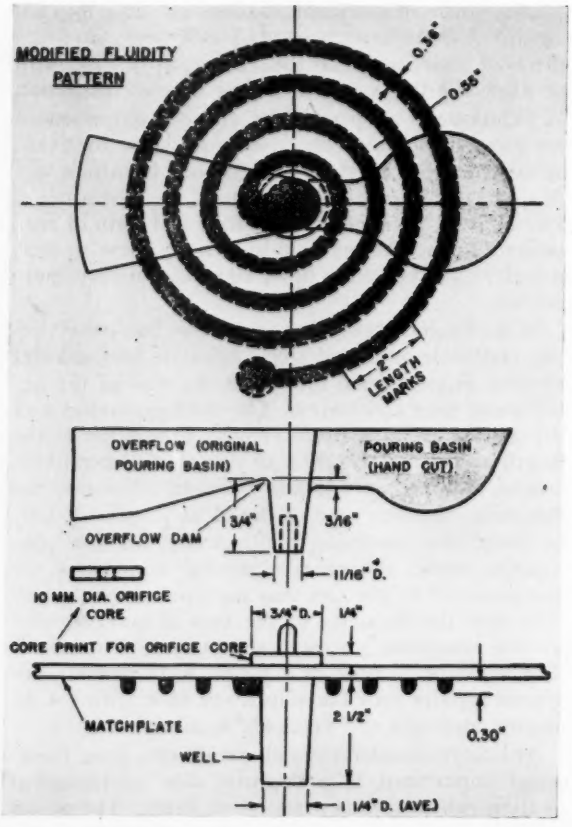


Fig. 1—Drawing of modified fluidity test pattern (Lange, Porter and Rosenthal[11,12]).

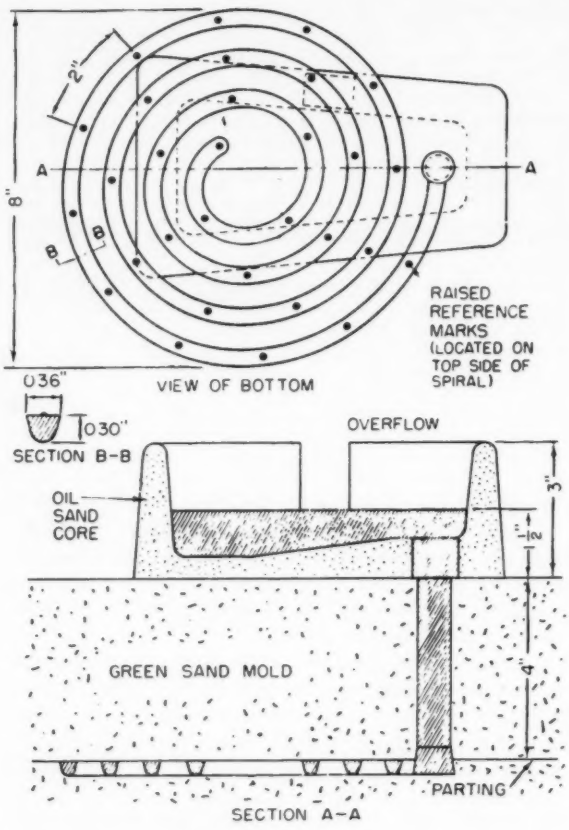


Fig. 2—Fluidity spiral test used to investigate effect of moisture in blast on metal fluidity. Dimensions of spiral are similar to those of Bureau of Standards spiral (Krause and Lownie[14]).

Modifications of the Bureau of Standards fluidity test mold—Porter and Rosenthal in their preliminary fluidity tests of cast iron employed the Bureau of Standards testing design. The observed, however, that the spiral lengths varied from 29 to 35 in., a deviation from the mean being 9.4 per cent. They also observed that is was difficult to maintain a steady flow of metal and a constant metal head. Therefore they decided to change the gating system and employed the design developed by E. A. Lange, L. F. Porter and P. C. Rosenthal,[11,12] shown in Fig. 1. The changes made in the original pattern consisted of substituting a well and orifice core for the horn gate, and providing a means of maintaining a constant level of metal head through use of an overflow basin. The metal entered the fluidity channel at its inner end.

The cross-section dimensions of this spiral were the same as those of the Bureau of Standards spiral. The results obtained with this design were found to be more consistent, a mean deviation being approximately 2 per cent. Standard melting procedure consisted of melting a constant weight of the base metal scrap in an induction furnace. Temperatures were measured by a Pt/Pt, Rh (10%) thermocouple in a silica protection tube immersed 2½ in. into the melt. When the temperature was stabilized at the desired

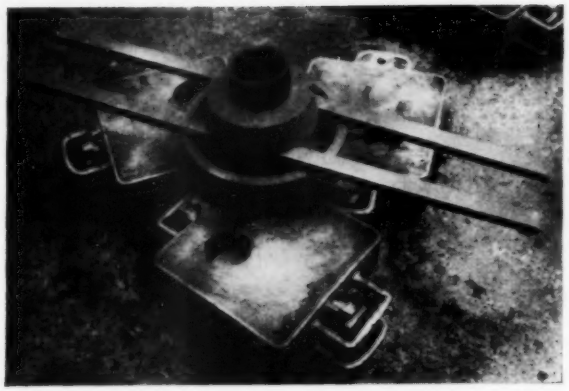


Fig. 3—B.C.I.R.A. laboratory type fluidity test piece ready for pouring (Evans[5]).

pouring temperature, the couple was removed, the power turned off, and the fluidity mold poured as quickly as possible. The above modification is essentially Curry's method[13].

In the Krause and Lownie[14] modification of the Bureau of Standards fluidity test mold (Fig. 2) no horn gate was used. The pouring basin and overflow were also different. Metal flowed from the pouring basin into a straight downgate located at the outer end of the spiral channel. Pouring temperatures were measured with a Pt/Pt Rh thermocouple. No details are given regarding the place of the temperature measurement. It may be inferred that the temperature was measured in the ladle just before pouring fluidity mold.

Another modification of the Bureau of Standards design was reported by Evans[5] in his work on fluidity of molten cast iron. The horn gate was omitted and replaced by a cylindrical down gate leading directly into the spiral channel. An overflow basin was added. The raised reference marks of the spiral itself were also omitted because it was thought that they created or aggravated undue turbulence in the stream of metal. Pouring temperatures were measured in the ladle immediately before casting each test piece by means of a Pt/Pt. Rh (13%) thermocouple. In the B.C.I.R.A. laboratory type test piece (Fig. 3) a core sand pouring head was designed with a circular channel containing three downgates. In the center of the pouring head was a receiving basin from which three radial channels led to the down gates.

Three fluidity test molds were arranged with the core sand pouring head positioned so that each down gate was directly over the deepest part of the pouring basin of each fluidity mold. A core sand bush (reservoir) was then placed over the center of the pouring head supported by two steel strips. About 30 lb of molten iron were poured as rapidly as possible into the central bush. The bush could be removed by means of the steel strips and placed over another set of molds. The advantages of this type of test piece, Evans stated, were that three spirals could be cast simultaneously from a ladle of metal and an average length could be determined, and that the rate at which the metal entered the mold could be controlled. The only precaution taken was to pour rapidly enough to keep the three down gates full.

For measurement purposes the end of a spiral was taken as the last point where the cross-section of the channel was completely filled with metal, but a note was also made of the total length taken to the tip of the spiral. In routine foundry practice a single spiral fluidity test mold was used and the pouring head core was redesigned. The single pouring head core consisted of a receiving basin and down gate. A standard testing procedure adopted consisted of measuring the pouring temperature, the length of spiral casting produced and the liquidus and solidus temperatures. It seems to the writer that one may expect a considerable drop in the pouring temperature of the metal due to its long travel before entering the fluidity channel. This drop in temperature may cause some errors in the observations.

Modifications of Taylor, Rominski and Briggs Fluidity Test Mold

A modification of Taylor, Rominski and Briggs spiral test design was used by Lillieqvist[15] in his fluidity tests of molten steel. After considerable investigation of various types of fluidity tests, it was concluded, that the double spiral test was the best for the purpose of his studies at the American Steel Foundries. His rough fluidity casting weighing about 90 lb is shown in Fig. 4. According to the author, consistent results were obtained when this type of test mold was used both with shank pouring and bottom poured ladles. With the bottom poured ladle the size of the well was increased.

It is very probable that the consistency of results obtained by Lillieqvist was mainly due to the precision of the temperature measurement employed by him and described below.

In discussing Lillieqvist's paper Jacoby[16] demonstrated his modification of the Taylor, Rominski, and Briggs fluidity mold. He stated that the relatively large amount of metal involved in pouring the standard spiral with pouring basin prompted his redesign of the spiral core mold so as to eliminate the pouring basin. Since adoption of the simplified spiral mold introduced the possibility of error due to speed and height of pour Jacoby's standard practice constituted

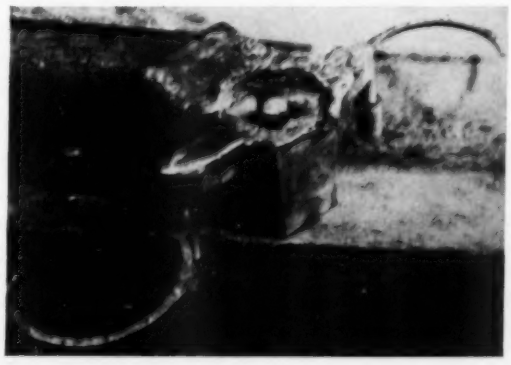


Fig. 4—Rough spiral casting (steel) weighing about 90 lb (Lillieqvist[15]).

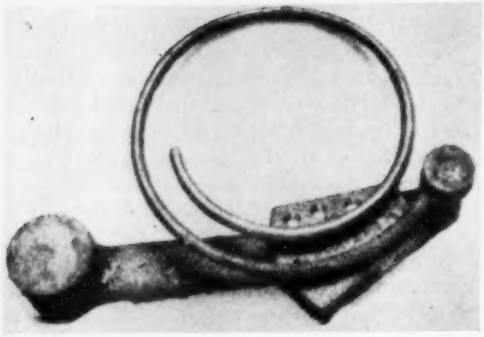


Fig. 5—The final modification of fluidity test casting used by Worthington[17].

pouring three molds in succession (three separate dips in the bath). Results obtained indicated a maximum variation of 8.5 per cent with the average at 5 per cent.

The writer doubts that the elimination of the pouring basin in the fluidity mold was a good practice. He agrees with Clark[1] that the kinetic energy of the metal being poured is largely dissipated in the pouring basin before it flows into the sprue. Therefore, the elimination of the pouring basin may introduce additional errors in the fluidity tests.

The Taylor, Rominski and Briggs design was adopted by Worthington[17] for his fluidity test of steel. Worthington believed that the most probable cause of inconsistencies observed was due to the fact that no provision was made to standardize the speed of entry of the metal into the mold. In order to control this speed he introduced a constant speed pouring ladle, which was mounted on a carriage and could be tilted mechanically at a permanent rate. The pattern of the spiral was slightly modified in order to simplify the pouring of the mold. The final modification is shown in Fig. 5. The pouring basin was positioned so that it was located parallel to the side of the flask near to the ladle. This revised orientation considerably simplified the pouring of the molds. The temperature was measured in the ladle just before pouring. All temperature readings were taken by means of a Pt/Pt, Rh (13%) thermocouple.

Modification of Eastwood and Kempf Fluidity Test Mold

Sicha and Boehm[18] in their fluidity measurements on aluminum alloys, used a test casting pattern in the form of a ribbon 1/16 in. thick by 1¾ in. wide with an arbitrary selected spiral shape (Fig. 6) mounted on the drag face of a wooden match plate. Green sand was used in molding and a ¾ in. diameter sprue cut through the cope, was located so as to open into the spiral cavity at about its mid-point. Two ¾ in. diameter vents were located at each end of the spiral. The pouring basin was made as a core and positioned over the sprue. In order to insure more uniform conditions of flow into the spiral arms, a ¾ x ⅞ in. deep well was provided in the drag side and directly under the sprue (Fig. 7). The authors found that addition of the well at the base of the sprue was effective in improving the consistency of fluidity data.

The volume of the two spiral ribbon sections was taken as a measure of fluidity. It may be seen that the ends of the castings were of an irregular shape. The authors found that even when using a pouring basin core on top of the mold too many variations were observed. The direction from which metal entered the pouring basin, the exact location at which the stream of metal encountered the pouring basin, and the rate of pouring influenced test results. To minimize the effect of these factors they adopted the practice of inserting a piece of aluminum foil over the sprue opening on top of the mold and under the pouring basin core. This foil served as a temporary dam which was found to be effective in reducing the influence of variations in pouring technique. The melt was skimmed with a preheated skimmer prior to the last step in temperature adjustment.

Temperature was measured by a beaded chromel-alumel thermocouple mounted in a small diameter protection tube. Two thermocouples were employed. One was placed in the center of the melt and the other against the side of the crucible. Manipulation was found necessary to bring the alloy exactly to the pouring temperature at the moment when the crucible and furnace were at practically the same temperature. A crucible with a capacity of about 20 lb of aluminum alloy was found to be suitable because it could be handled by one man for pouring. Melts prepared in this manner, the authors state, could be poured promptly into several fluidity spiral molds in succession with assurance that the temperature drop during the entire operation would be negligible.

The authors stated that variation in weights of individual spirals poured from a single melt was usually less than 15 grams. Since the average weight of an individual spiral was about 60 grams, the writer believes that 15 grams is a fairly large variation. In the writer's opinion some of the errors of observations may be due to the temperature measurement technique employed by the authors.

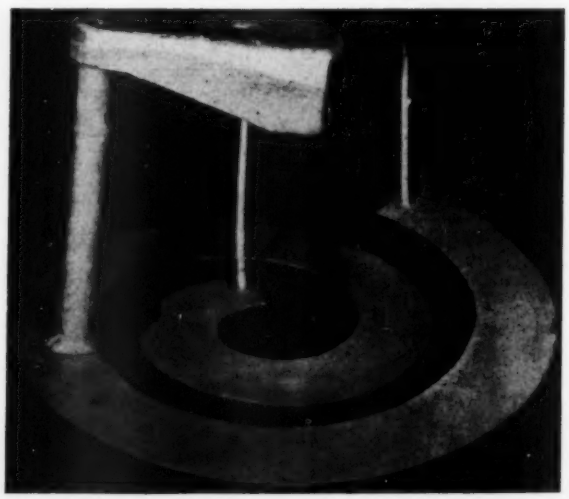


Fig. 6—Flat spiral casting of original design for determining fluidity of aluminum casting alloys (Sicha and Boehm[18]).

No matter how fast the metal is poured into several fluidity molds in succession, there is a temperature drop.

It is true that with metals, such as aluminum alloys, possessing high specific heat value, the temperature drop in such a case should be much smaller as compared with some other metals.

Nevertheless there must be some differences in the temperatures of the metal entering different spiral test molds.

Other Methods Used for Measuring Fluidity of Molten Metals

In his "new method of testing the running capacity of very small quantities of metals" Ruff[6] used a straight flow channel of 5 mm diameter and 720 mm (24 in.) long.

The writer agrees with Worthington[17] that the undesirable features of the straight-flow channel are the length of the mold required, and the necessity for extreme accuracy in leveling the mold. The cross-sectional area of the straight-flow channel has necessarily been restricted in order to keep the mold length within convenient dimensions.

The "U" type test piece was recommended for a control test by the Institute of British Foundrymen[19]. In this test a permanent mold, Fig. 8, was used. It was stated that one of the principal reasons for recommending the "U" type test piece was that many of the variable factors such as molding technique, sound condition and running arrangements are overcome by the adoption of a chill mold. The iron mold was constructed in two parts which could be hinged at one end and clamped at the other for accurate and speedy opening and closure.

The test was conducted by pouring the metal into the runner box until it runs level in the riser and then measure the length in millimeters of the offshoot or "fluidity limb". It will be noted that three "fluidity limbs" of different diameters (2 mm, 4 mm and 6 mm) are provided in the mold in order to facilitate the reading of results from irons, the characteristics of which will vary with the type of casting being produced. Thus, for metal suitable for light, thin sectioned castings the smallest would be used, while the medium or largest limbs would give more readable results with metal suitable for heavier work. The measured length of the fluidity limb in millimeters is termed the fluidity number which may be expressed, for example, "U" fluidity 12 mm, A, B or C" according to the limb used.

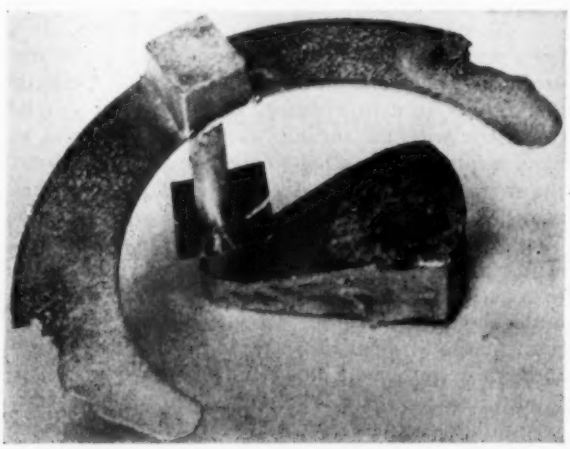


Fig. 7—Flat spiral fluidity test casting of modified design inverted to show the well at base of sprue (Sicha and Boehm[18]).

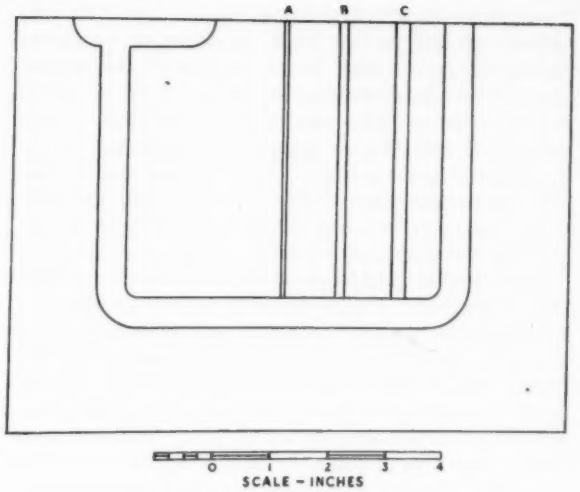


Fig. 8—"U" type fluidity test piece[19].

In recommending the "U" shape the subcommittee of the technical council realized that conditions might arise which would make the use of the Ruff or spiral test desirable. The following quotation is probably of interest: "If the spiral test be chosen, then the subcommittee recommends the design which is run from the outside through horn gate runner and without risers". The design recommended is the Bureau of Standards design.

Sanders and Kain[20] believed that both spiral and straight flow test pieces were small and primarily suitable for laboratory work. Owing to their smallness, the authors stated, they were difficult to pour under conditions of commercial steel production. The ratio of mold surface area to mass was much higher than would be expected in the production of castings, and the effect of mold conditions, etc., was greatly magnified. Therefore the authors adopted in their fluidity test of molten steel a test-piece consisted of a mold cavity (6 in. sq, and 2 in. high) into which the metal was poured through a restricted ingate (1⅛ in. diam and 2 in. long). The weight of steel entering the mold was used as a measure of fluidity.

In order to produce normal mold conditions as closely as possible, the runner channel between the down gate and in-gate was made as long as possible and given three changes of direction. At first an ordinary funnel-shaped down gate was used. This was found to produce variations of results. To overcome this variable a pouring basin was provided. The pouring basin was formed on the top of the mold by an oil-sand core. It was stated that consistently reproducible results might be obtained in narrow limits.

In the writer's opinion such type of a test may be entirely satisfactory for some foundries as a routine control of their melts, but it can hardly be recommended for the determination of the fluidity—temperature relation. The long distance run between the down gate and in-gate suggests a considerable drop in the pouring temperature of the molten metal. One may also debate the author's remark that with the spiral the effect of mold conditions is greatly magnified because the ratio of mold surface area to mass is much higher than may be expected in the production of castings. This opinion is contrary to that of Eastwood and Kempf[10] who, as it was already pointed out believed that the ratio of the perimeter of the round cross-section to the cross-sectional area was too small. According to them this type of test piece minimized the surface tension factor which was of a particular importance when dealing with thin castings.

Guillemont[21] used the fluidity test piece of a snake-like form. The trapezoidal channel of this mold was 848 mm (33.1 in.) long. Cross-sectional dimensions of his test-piece were: 5 mm at bottom, 8 mm at top and 10 mm in height, the cross-sectional area being approximately 1/12 sq in. The mold was made of sand and provided with a pouring basin, vertical down gate and well from which the metal entered the fluidity channel. This well was also connected with an adjacent reservoir. In order to avoid the difficulties of exact measurement of pouring temperature which is the most critical factor in fluidity tests Guillemot advanced the method for determining the relative and not absolute fluidity. Essentially this method was the method of comparison. Two fluidity test pieces were cast simultaneously into the same mold from a special divided two compartment crucible with two pouring lips. One test piece was cast of a standard metal, while the other one was the metal undergoing test. The lengths of these two castings were then compared.

The standard metals selected were as follows:

Pure magnesium—standard for extra light alloys.

Aluminum 99.99%—standard for light alloys.

Electrolytic copper—standard for copper alloys.

Armco iron—standard for ordinary and special steels.

For example, the following procedure was followed for the determination of the relative fluidity of the A-S4G aluminum alloy (Cu, 0.10; Mn, 0.60; Mg, 0.55; Fe, 0.33; Si, 4.26%). One compartment of the divided crucible was filled with molten pure aluminum (standard metal) while the other half of the crucible was filled with molten A-S4G aluminum alloy. The pouring temperature, in both compartments, as measured by a chromel-alumel thermocouple, was 1328 F (720 C). Then two molds, each containing two fluidity test pieces were poured. The results reported were as follows:

Mold No. 1—Pure aluminum (standard)	
Length of test piece	68.7 cm
Mold No. 1—A-S4G aluminum alloy ..	60.8 cm
Mold No. 2—Pure aluminum (standard)	68.0 cm
Mold No. 2—A-S4G aluminum alloy ..	56.6 cm

The same method was used by the author for casting fluidity spirals (two spirals in one mold) in permanent molds.

It is not entirely clear to the writer as to how the same pouring temperature, particularly with the copper and ferrous alloys, could be maintained in both compartments of the crucible. This procedure is probably satisfactory for aluminum alloys, whose specific heat value is high, but it is doubtful whether it can be adequate for the copper and some ferrous alloys. Here, again, we have a method which may satisfy the local needs but can hardly be recommended for the study of fluidity-temperature relations or adopted as a standard foundry test.

In order to determine fluidity of cupola iron and to see what correlation could be obtained between fluidity, metal temperature, and type of fuel used, a test was devised by Wright and Reagan[22] which simulated the casting of metal in permanent mold units. The permanent test mold used was designed by Adam Mahonske. The mold was about 12 in. high and bottom run from a $\frac{1}{2} \times \frac{1}{4}$-in. section runner. It consisted of five vertical zig-zag channels 15 in. long, connected in series with a feeder gate and a riser. The cross-section of the fluidity channel is trapezoidal in shape ($\frac{1}{4}$ in. at bottom $\frac{5}{16}$ in. at top, $\frac{1}{4}$ in. high). The cross-sectional areas was about $\frac{1}{12}$ sq in. The mold was maintained at relatively uniform temperature, after preheating with hot metal, by pouring tests at fixed (15-min.) intervals. The mold was brushed and smoked after each test. The authors found that over the range of fuels and conditions studied, fluidity did not vary greatly except as a function of temperature.

Taylor Fluidity Measurement Test

A rapid test for fluidity measurement was developed by G. B. Taylor[23] to eliminate the time lag in obtaining a reading. His fluidity mold consisted of two oil sand cores. The lower half contained a spiral and the upper half the pouring basin and strainer core. The pouring basin was divided into two parts; the side part, reservoir, and the central part, outlet (wide down gate). Metal was poured into the reservoir but not directly into the outlet. The strainer core was set well below the top of the outlet to allow for a reasonably constant ferrostatic head above the strainer. The metal flowed from the well beneath the strainer core to a runner bar (modified spiral) concentric with the outside wall of the core and at regular intervals branches were made to radiate from the ring to the outside of the core. These branches provided the means of determining, without stripping the core how far the metal had run in the concentric ring, since it could flow out of each hole in turn as it traveled around the ring. There were ten outlet holes $\frac{1}{8}$ in. wide, about $2\frac{1}{2}$ in. apart. It seems to the writer that this rapid method may satisfy the local requirements but cannot be considered as a fluidity test method.

A rather complex apparatus for determination of the fluidity and rate flow of the molten metal was proposed by Kunin.[24] This apparatus permitted simultaneous determination of the length of time for filling the mold and the rate of flow of the molten

metal. The fluidity test piece consisted of a straight round rod. It was poured through a gate into the fluidity channel. Along this channel graphite discs were placed vertically and at equal distances. The discs had a central hole of the same diameter as that of the channel and they were placed in such a manner that the center of the hole was located on the central axis of the channel. Therefore, there was continuous flow without any interference. Each of the discs was connected by means of wires with an individual electric bulb. These bulbs were known as fixing bulbs. They were connected in parallel.

A graphite or metal rod was placed in the middle of the pouring gate. This rod as well as the fixing electrical bulbs were connected to a common electrical circuit. There was also one electric bulb-speed recorder, one pole of which was connected to the common electrical circuit while its other pole was attached to an automatic breaking mechanism which closed the electrical circuit every 0.5 sec. The fixing and speed recording electric bulbs were mounted on a special frame in such a manner that, when lighted, their light was directed by means of a lens and diaphragm into a photographic camera. Inside the photographic camera there was a moving carriage on which a photographic film holder was mounted.

At the instant when the metal was poured into the mold the photographic film was set into motion. As molten metal traveled along the channel and passed through the graphite discs it closed the electrical circuit of each of the fixing electric bulbs. Thus, the flow of metal was recorded by each fixing bulb on the moving photographic film as a continuous narrow band. The length of the bands gradually decreased, the longest band being obtained for the disc nearest to the pouring gate. On the same photographic film, the time of flow was recorded by a speed recording device (speed-recorder bulb and automatic break) as a broken band which was used as the time-scale. Knowing the distance between the graphite discs and the time of travel of molten metal through the channel one could determine the rate of flow and the fluidity of the molten metal. In the writer's opinion, the idea is interesting and the apparatus is probably useful in laboratory work but is not practical for foundry tests.

Fluidity Mold Characteristics

Shape of Cross-Section and Cross-Sectional Area of Test Piece—The cross-section of the Bureau of Standards spiral has a rounded trapezoidal shape with an area of approximately $\frac{1}{12}$ sq in. It is considered from the molding point of view that the rounded trapezoidal shape is more satisfactory than a circular section, because there is less danger of mismatches as compared with a round section.

The Taylor, Rominski, and Briggs' spiral also has a rounded trapezoidal shape but a larger cross-sectional area, $\frac{1}{7}$ sq in.

According to Eastwood and Kempf[10] the relationship of surface area to volume of the fluidity test piece is an important factor. The spiral, they used in their preliminary tests, had a cross-sectional diameter of $\frac{3}{8}$ in. and cross-sectional area of about $\frac{1}{9}$ sq in. Since the cross-section of their spiral was round, the ratio of the perimeter of the cross-section to the cross-sectional area is a minimum. Eastwood and Kempf stated that such a test casting gave the opposite of the conditions most desired because it minimized the surface tension factor that must play a considerable role when the foundryman pours castings having thin sections.

Therefore they redesigned the test spiral used in their preliminary work to produce a flat spiral having cross-sectional dimensions of $1\frac{3}{4}$ in. wide by $\frac{1}{16}$ in. thick. This flat spiral also had a cross-sectional area of about 0.11 sq in. (about $\frac{1}{9}$ sq in.) but the surface area per unit of length was slightly more than three times greater than that in the spiral previously used. They found that two kinds of spirals (round and flat) produced quite different results with aluminum alloys, those obtained with flat spiral more nearly agreeing with foundry experience. The authors realized that when using the flat spiral it was necessary to take a reasonable amount of care to insure that the thickness of the spiral was the same in each mold. But, according to them, this was done with very little difficulty. The volume of the metal which flowed into the flat mold cavity was taken as a measure of the fluidity, and alloys having different densities could be compared.

General indications are that the cross-sectional area of $\frac{1}{12}$ to $\frac{1}{7}$ sq in. of the spiral fluidity test piece is still most commonly used and most practical in the foundry practice.

Gating

From the preceding discussion it may be inferred that the different modifications of the three basic fluidity test methods concerned mostly the gating system of the mold. In general, the gating system of a fluidity test piece should be designed so that the metal enters the mold with a minimum of turbulence and a constant hydrostatic pressure maintained on it during the running of the mold.

The influence exerted by hydrostatic pressure in the casting of spirals was first examined by Saito and Hayashi[7] who observed an increase in the lengths of their spirals as the pressure increased. In discussing the results obtained with the Bureau of Standards spiral when using different gating systems, Ruff[6] referred to the Widin and Girschowitsch[25] observations on the effect of ferrostatic pressure on the length of white iron spiral castings. These authors observed that the length of the metal flow increased with the increase in pressure up to certain limiting values, beyond which there was a considerable decrease in length of spirals. It is of interest to note that Ruff's observation on the effect of ferrostatic pressure on the length of the fluidity spirals was of the same order.

The hydrostatic pressure and velocity of metal flow into the mold cavity have always been a great concern in fluidity testing.

In attempts to control the hydrostatic pressure of the molten metal entering the fluidity channel, the writer in his early experiments, adopted the device developed by Saito and Hayashi.[7] It consisted of a

core pouring basin mounted over the down gate of the mold. A graphite or sand core rod plug was inserted in the discharge outlet of the pouring basin into the down gate. During the test, the molten metal was poured into the basin, the temperature was measured with a thermocouple in the crucible at time of pouring each mold, the rod plug was lifted and the metal was allowed to flow into the mold.

Two principal difficulties were experienced with this device: (1) the rod plug had a tendency to float during the pouring of the metal into the basin so that it was necessary to hold it tight in its place. (2) It was difficult to prevent some leakage of the molten metal between the plug and its seat before allowing the metal to flow into the mold. Therefore this idea was finally abandoned.

It has been already mentioned that Sicha and Boehm[18] in their efforts to eliminate the effect of rate of pouring inserted a piece of aluminum foil over the sprue opening on top of the mold and under the pouring basin core. This foil served as a temporary dam which was found by the authors to be effective in reducing the influence of variations in pouring technique. This idea is similar to that of Courty[1,26] who measured the fluidity of aluminum alloys in a test mold having the sprue stoppered by means of two fusible plugs. The time of melting of the plugs was regulated by their thickness.

The Sicha and Boehm's method as well as the device used by Courty may serve their purpose in the case of aluminum alloys but in the case of copper and ferrous alloys one may construct similar thin copper or iron plugs covering the sprue opening under the pouring basin for the fluidity measurement of the copper or ferrous alloys.

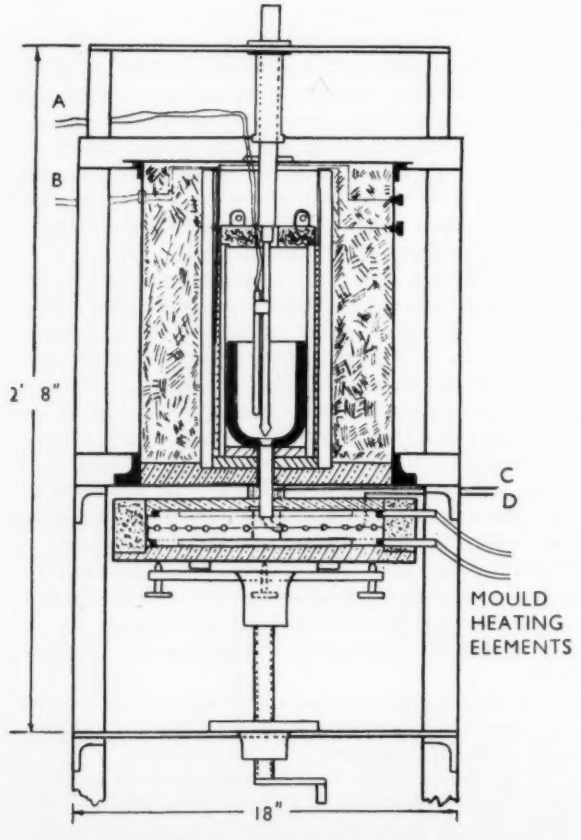


Fig. 9—Fluidity apparatus (Kondic and Kozlowski[27]).



Fig. 10—Cross-section of fluidity mold (Kondic and Kozlowski[27]).

Another clever device developed with the same purpose in mind is the Kondic and Kozlowski's[27] fluidity apparatus illustrated in Figs. 9 and 10. In their experiments the melting was carried out in a resistance-wound electric furnace, the crucible being directly connected with the fluidity mold which is situated beneath the furnace. The mold, also resistance heated was made of two halves, and provided a ¼-in. diameter circular groove of Archimedes spiral. Cast iron or graphite molds were used. At a predetermined mold and metal temperature the molten metal was poured into the mold by raising the plunger. Both, metal and mold temperature were controlled at ± 2 F (1 C).

Hydrostate pressure was varied by changing the height of metal in the crucible. The effect of hydrostatic pressure and mold temperature on the fluidity values for aluminum were obtained. It was demonstrated that the hydrostatic pressure could be readily controlled to a sufficient degree to eliminate its effect on the fluidity values. There was a noticeable increase in the fluidity when the height of the molten metal in the crucible was raised from the 1-in. to the 2-in. level. However a further increase in height (to 3 and 4-in. levels) had little effect.

It was also found that the mold temperatures below 572 F (300 C) were of no importance, but with higher mold temperatures it was necessary to apply a correction factor. Here again we have a fluidity apparatus which is useful for laboratory work but which is not practical for foundry tests.

An interesting method for controlling head pressure was developed by Ash.[28] From his observations in a foundry producing malleable iron Ash found that

the fluidity, as measured by the Bureau of Standards method, was not always a function of pouring temperature and composition. In order to obtain a higher degree of sensitivity he modified the original method. In this modification (Fig. 11) the only change involved the manner in which the metal was introduced into the horn gate. A core pouring box was mounted over the down gate and a ¾-in. core rod was inserted in the ½-in. discharge outlet into the down gate. In making a test the reservoir was filled with molten metal to the level of an overflow, the core rod plug was then removed, and a constant volume of metal allowed to run into the mold. The temperature was measured by means of an optical pyrometer sighted on the metal surface just before pouring the mold. According to the author, quite satisfactory results were obtained with this fluidity test mold.

Evidently Ash did not have the difficulties that were experienced by the writer in carrying out his fluidity tests using a somewhat similar plug device, as described previously.

Temperature Measurement

It is now a well established fact that the mold entering temperature of the metal is the most important factor in the control of measurement of "fluidity." Therefore, the importance of correct measurement of this temperature cannot be overemphasized. It is almost certain that some inconsistencies observed by different investigators were due to errors of temperature measurements. For instance, one may doubt the significance of results obtained from the tests made with the aid of an optical pyrometer. In the early fluidity tests conducted at the Bureau of Standards it was well recognized that the temperature of the molten metal flowing into the fluidity channel must be accurately measured. For this reason, at the very beginning of investigation the use of an optical pyrometer was abandoned. Instead, thermocouples (chromel-alumel for nonferrous metals and alloys and Pt/Pt Rh (10%) thermocouples for ferrous alloys were adopted.

The thermocouple wires were separated by means of two-hole porcelain insulators and protected by a closed-end glazed porcelain tube. In case of ferrous alloys this assembly was inserted into a closed-end graphite tube coated with a layer of aluminum oxide and zirconium silicate. For the temperature measurement of non-ferrous alloys a closed end uncoated graphite or iron tube was employed. It was realized, however, that with such heavy protection tubes being employed, rapid response of the thermocouple could not be obtained. Since the thermal lag was more or less constant, it was assumed that the difference between the true temperatures and the temperatures observed was also constant and the plotted fluidity—temperature relation was correct. Nevertheless, such thermocouple assembly was never satisfactory to the writer.

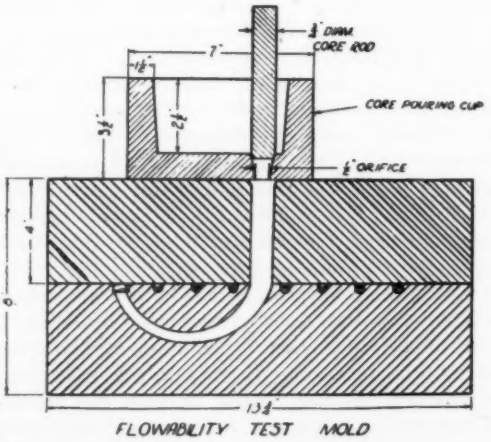


Fig. 11—Cross-section of modified Bureau of Standards fluidity spiral mold (Ash[28]).

In this connection the following quotation from the verbal discussion on Ash's paper[28] by Sims is very instructive. Sims said: "I talked to Dr. Sargent within the last two months and found he has some more recent information. A good many of those earlier tests in which he obtained these very discordant results, fluidities running all over the map without apparent correlation with fluidity, were made with optical temperature readings. Since then, he has repeated a great deal of that work, taking temperatures with thermocouples, and since the results now fall into nice smooth curves, he has eliminated a great many of these differences. In other words, what it amounts to is that emissivities of these metals varied greatly and threw him off on the temperature readings. . . . The more accurate reading obtained with the thermocouples straightened out a lot of the apparent discrepancies."

During the last decade great progress has been made in the development of light sheething material for the immersion type thermocouples.

After some experimentation with various types of protection tubes a standard procedure for temperature measurement was developed by Wright and Reagan.[22] This consisted of using a Pt/Pt,Rh couple encased in double fused silica protection tube. The inner tube was 3/16 in. O.D. and the outer tube ⅜ in. O.D. These tubes were mounted in a graphite head, and the insulated thermocouple leads carried out through ½-in. iron pipe connected to the head, to a temperature compensated potentiometer. The thermocouple, inside its protecting tubes, was immersed in a well built into the cupola spout on the ladle side of the slag skimmer. Response to temperature after immersion of the thermocouple was quite rapid, and equilibrium was attained in about 30 sec.

Usually, six to eight immersions were obtained before the outer protection tube needed replacement, and at one time the thermocouple was allowed to remain in the metal continuously for 2 hr. No details were given by the authors concerning the location at which the temperature of molten iron was measured during pouring of fluidity test molds. It may be inferred that fluidity molds were poured directly from the ladle after the temperature was measured at the cupola spout.

It does not seem to the writer that the protection tube arrangement used by Wright and Reagan was

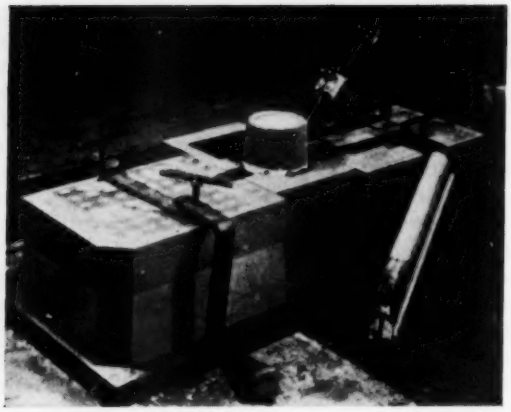


Fig. 12—Fluidity mold assembly, temperature measurement is taken in the pouring cup (Lillieqvist[15]).

too great an improvement over that employed in the Bureau of Standards tests.

A much higher precision of temperature measurement was developed by Mott, Schaefer and Cook. Their temperature measurement technique and thermocouple assembly was described by Clark.[1] They placed the hot junction of a Pt/Pt,Rh thermocouple in the pouring basin of the fluidity mold. This couple was pulled through a single bore, fused-silica sheath with thin walls so that the thermal lag was not more than 4-5 sec.

Another high precision method for measuring temperature was advanced by Lillieqvist.[15] When shanking the steel, a direct temperature reading was taken in the pouring cup of the fluidity mold (Figs. 12 and 13). A fused quartz tube, about 6 in. long, sealed on one end, was fitted at an angle of 45 degrees into the pouring cup. A 32 B & S gauge Pt/Pt,Rh (10%) thermocouple was inserted into a quartz tube which had a 4 mm O.D. and ½ mm wall thickness. A single hole insulator prevented the two wires from shorting in the quartz tube. Pouring temperaure readings using this method could be obtained in

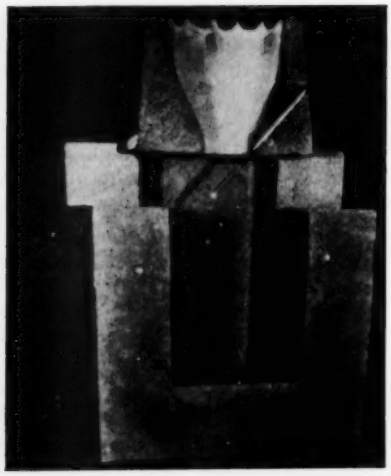


Fig. 13—Pouring cup and thermocouple (Lillieqvist[15]).

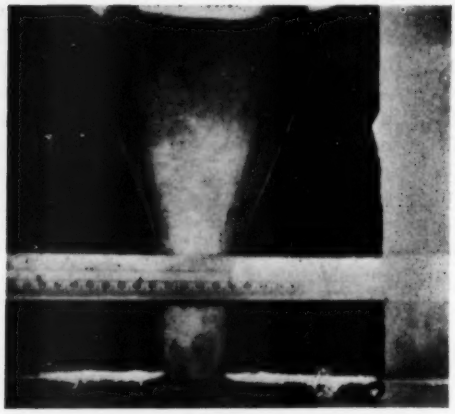


Fig. 14—Fuzed quartz tube placed across the pouring cup near the bottom (Lillieqvist[15]).

about 7 to 12 sec. It was necessary to have a solid stream of the metal in constant contact with the quartz tube for at least 10 sec.

When a bottom poured ladle was employed the metal entered the mold with a faster rate and in this case the temperature readings were taken in the cup of a fairly large mold poured just prior to the fluidity test mold. In addition, because of the higher static pressure developed in bottom pouring it was necessary to modify the thermocouple arrangement in the pouring cup so as to eliminate bending and breaking.

Accurate results were obtained by placing a fused quartz tube across the pouring cup opening near the bottom as illustrated in Fig. 14. With this arrangement it was possible to use 4 mm O.D. quartz tubes with 0.75 mm wall thickness. Because no insulators near the thermocouple bead were required, as one wire extended in from one side and the other from the other side, the couple responded to the steel temperature faster. The pouring cup had to be continuously filled with liquid steel so that the metal was always in contact with the quartz protection tube.

A number of other precautions had to be followed to obtain reliable temperature readings. An interesting remark was made by Lillieqvist with regard to the temperature measured at different times of the pouring operation. It was noticed that at the beginning and at the end of the heat the fluidity was the highest, while at the middle of the heat the fluidity was the lowest. In this connection the author makes reference to the paper presented in April 1950 by F. Von Gruenigen and D. Lawter on the subject of "Temperature in the Hearth and Ladle" before the National Hearth Conference. They inserted thermocouples into the ladle approximately 1½ in. from the ladle lining and 9 in. from the top of the nozzle. By this means they could determine the temperature of the liquid steel during the entire pouring operation. At the beginning of the pouring operation the temperature was fairly high, then in the middle of the heat the temperature was the lowest. The temperature starts to increase again and at the end of the heat the temperature was approximately the

same as in the beginning of the heat.

Kerr in discussing the Lillieqvist paper said that it might be possible that the phenomena just noted was due to the placement of the thermocouple too near the ladle lining. Whatever the explanation of this observation, the writer believes that this information is of interest. It may be noted that different authors in refering to pouring temperature use term "superheating temperature." In this connection the writer wants to remind that the term "superheat" has a specific meaning, i.e., the thermal treatment of the melt. It is suggested therefore that the expression "superheating temperature" be avoided when speaking about pouring temperature. It is more correct to report the temperature as so many degrees above the liquidus.

Summary

In summarizing the above discussion, the following concluding remarks can be made: The term "fluidity" should be established as purely a foundry term. For the present time at least, the only convenient method of evaluating fluidity is an empirical one. The nature of the mold and the technique of casting can be neglected if a sufficient degree of standardization is adopted. On the other hand, such important properties as surface tension, solidification temperature, viscosity, specific heat, solidification range and crystallization form depend upon composition and temperature. Therefore the only critical factors in the study of fluidity are composition and the temperature of the molten metal entering the fluidity channel.

There are a number of methods proposed by various authors for fluidity measurement. Some of these methods may be considered as purely laboratory methods, there are also methods which may satisfy the local needs but cannot be adequate for the study of the fluidity—temperature relation. The only practical methods, suitable for this purpose, appear to be the spiral and straight rod fluidity methods. The principal disadvantage of the straight rod test piece is that the cross-sectional area of the flow channel has necessarily been restricted in order to keep the mold length within convenient dimensions. As far as can be judged from publications available, preference is given by the different investigators to the spiral type of the test piece over the straight rod. A majority of the authors seem to prefer the use of spirals having cross-sections of a trapezoidal shape, the cross-sectional area being $\frac{1}{12}$ to $\frac{1}{7}$ sq in.

General indications are that the pouring of the metal directly into the sprue of the test mold is a poor practice and that a pouring basin and overflow reservoir are to be provided. In order to control casting head pressure, the rod plug device, such as described by Ash, may be suggested, provided that there is no leak of the molten metal into the mold until the plug is removed. The writer believes that Evans' remark that the raised reference marks of the spiral might create or aggravate undue turbulence in the stream of metal merits a serious consideration. They can easily be omitted.

In designing gating system consideration should also be given to the fluid flow studies at the Naval Research Laboratory and Battelle Memorial Institute where jet, turbulence and vortex effects of gating system were demonstrated. In measuring the length of the spiral the end of a spiral should be taken at the last point where the cross-section of the channel is completely filled with metal and a note to be made of the total length taken to the tip of the casting. It is doubtful, whether the volume or weight of the casting is a correct indication of fluidity and can be recommended for the fluidity measurement. The writer believes that some errors of observations may be introduced by such items as the imperfections of casting (blow holes, shrinkage cavities), burned on sand and difference in the methods of weighing employed by various investigators.

The temperature of the molten metal entering the flow channel is the most critical factor in fluidity determination. Therefore all possible precautions should be taken to insure the correct temperature measurement techniques. The use of an optical pyrometer for this purpose should be avoided. The method described by Lillieqvist is strongly recommended. Once reliable fluidity data for a given metal are established there is no need for repeated temperature measurements of the metal flowing into a test mold, since length of spiral indicates this temperature. A periodic check is sufficient.

In this summary no mention has been made about mold material and well at base of sprue. The reason being that the writer doubts whether the mold material can be standardized for fluidity testing and he hesitates to make any definite statement on this subject. As to the well, there was a difference in opinion concerning its usefulness. Some authors report that addition of the well at the base of sprue was beneficial, another (Ruff) found, on the contrary that use of the well caused an increase of discrepancies.

In conclusion, the writer hopes that one standard method and procedure will be adopted by the A.F.S. and will be strictly followed in the fluidity testing of foundry alloys. At the present state of affairs, when practically every author introduces his own alterations, no comparable data are available. Moreover, it is felt that many of the reported modifications would not be necessary if the correct temperature measurement technique were used. However, the writer does not mean to infer that research into new technique for fluidity testing should cease because there is a definite need for more fundamental information to be established on such a controversial subject.

Acknowledgment

The writer is indebted to Mr. Jack H. Schaum for his technical assistance and editorial reading of this review.

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DISCUSSION

Chairman: C. B. JENNI, General Steel Castings Co., Eddystone, Pa.

Co-Chairman: J. R. GOLDSMITH, Crane Co., Chicago.

Recorder: W. R. PUNKO, Wehr Steel Co., Milwaukee.

W. G. WILSON:[1] What is the reproducibility of results of two test molds poured simultaneously from the same ladle?

J. H. SCHAUM:[2] I am not in a position to answer in terms of steel foundry practice, but believe a 2 per cent deviation would be very good in steel. In aluminum it is 25 per cent.

J. A. RASSENFOSS:[3] Two spirals in a mold will check within ½ in. Even 1 in. would be satisfactory.

CHAIRMAN JENNI: Mr. Rassenfoss, did you have buttons on your spiral?

MR. RASSENFOSS: Yes.

CHAIRMAN JENNI: What effect would turbulence have on it?

MR. RASSENFOSS: Frankly, I do not know.

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[1] Metallurgist, Molybdenum Corporation of America, Chicago.
[2] Metallurgist, National Bureau of Standards, Washington, D. C.
[3] Assistant Resarch Director, American Steel Foundries, East Chicago, Ind.

MECHANICAL PROPERTIES OF CAST TITANIUM—CARBON ALLOYS

By

O. W. Simmons and R. E. Edelman*

ABSTRACT

Some of the mechanical properties of as-cast titanium-carbon alloys were determined over the range from the 0.04 per cent carbon present in duPont "Process A" titanium sponge to 0.90 per cent carbon. The ultimate tensile strength gradually increased from 70,000 to 90,000 psi and the elongation rapidly decreased from 29 per cent to 1.6 per cent (gage length of 1.4 in.) over this increase in the carbon content. The melting temperature of the alloy increased as the carbon increased but no particular difficulty was encountered in casting these binary alloys and the as-cast surfaces were free from cold shuts and seams.

These data are significant in considering the possibility of producing ductile material by induction melting. At present, graphite is the only practical crucible material for induction melting titanium. The carbon pick-up from a graphite crucible is sufficient to prevent the production of ductile castings by the induction method of melting.

Introduction

Many of the useful titanium alloys are very difficult to machine. Thus considerable savings would be made if these alloys could be cast to the desired shape. Therefore, as part of the general Army program to develop titanium for military purposes, Frankford Arsenal has been directed to study foundry techniques for titanium. The specific objective was to devise practical methods for casting titanium and its alloys into shapes weighing up to 10 lb. These castings were to have good surface finish and dimensional accuracy in the as-cast condition.

The simplicity and convenience of induction melting has been widely appreciated.[1,2,3,4] However, the extreme reactivity of molten titanium has successfully defied every attempt to apply this method to the preparation of ductile titanium castings. Even after extensive investigation, graphite is the only material now known which will withstand attack by molten titanium and which is also suitable for use as a crucible in induction melting. All other refractory crucibles contaminate the melts excessively and make the titanium hard and brittle.[2,3,4] Unfortunately, even graphite is not completely inert since titanium alloys melted in a graphite crucible contain from 0.30 to 1.0 per cent of carbon dissolved from the crucible wall.

Considerable information on wrought carbon containing alloys appears in the recent literature[5,6,7] but very little information[1,2,3] is available on cast alloys containing carbon. Therefore, in this investigation, a survey was made of the as-cast titanium alloys containing up to 0.90 per cent carbon in order to make a sound evaluation of induction melting as a method for producing ductile titanium castings. The test bar castings (Fig. 1) for this survey were prepared in a special arc furnace[8] which had been developed for making contamination-free castings of any desired composition. This device provided test bar castings of the desired carbon content which were essentially free of other contaminating elements.

Method of Preparation

The specimens were prepared from duPont "Process A" titanium sponge and 40 to 60 mesh carbon powder. A commercially available stabilized carbon

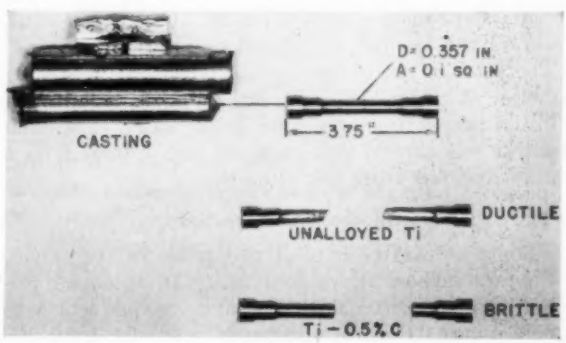


Fig. 1—Special "keel block" cast in a mold of solid graphite. A modified "half-size" tensile-test specimen with 1.4-in. gage length was made from lower section of keel block. The addition of 0.5 pct carbon markedly reduced ductility from 29 pct to 4 pct elongation.

* The authors are Metallurgists, Pitman-Dunn Laboratories, Frankford Arsenal, Philadelphia.

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TABLE 1.—MECHANICAL PROPERTIES OF AS-CAST TITANIUM-CARBON ALLOYS

Specimen No.	% Carbon Balance Ti Intended	Actual	Ultimate, Tensile Strength, psi	Yield Strength (0.2% offset), psi	Proportional Limit, psi	Elongation Per cent in 1.4 in.	Reduction in Area, %	Hardness, Brinell (3000 kg load)	Charpy Impact, ft-lb —105°F	84°F	212°F
22	0.00*	0.04	69.5	53.0	42.5	29.3	41.6	156	9.25	12.8	26.3
24	0.00*	0.04	72.6	58.6	44.5	27.5	40.3	161	8.75	12.0	23.3
28	0.25	0.31	89.4	63.0	50.0	8.5	10.0	181	—	—	—
29	0.25	0.29	90.7	68.7	50.0	9.3	11.2	182	3.5	11.0	—
30	0.50	0.51	87.5	68.7	51.0	4.0	3.9	188	3.0	3.0	—
32	0.50	0.47	84.8	68.5	60.0	4.2	3.1	197	4.5	—	22.0
33	0.50	0.53	86.3	70.2	51.0	3.9	1.7	189	—	3.0	3.0
34	0.70		90.3	78.7	55.0	2.5	1.7	215			
37	0.70	0.65	94.5	84.6	60.0	2.0	1.1	217		2.5	
38	0.70		89.8	80.0	70.0	3.0	2.0	217			
43	0.90	0.85	96.6	80.9	67.0	1.1	1.1	217			
44	0.90		90.4	80.5	65.0	2.1	1.4	241			

*"Commercially Pure" Titanium Process "A" Sponge used as base alloy material

powder, or "CS" grade graphite filings were used for this purpose. Castings were prepared in a skull type arc melting furnace developed at Frankford Arsenal specifically for the purpose of making titanium castings of any desired composition free of contamination.[8]

Testing Procedure

Brinell (3,000 kilogram load) hardness readings were made on flats machined on the castings. At least two readings were taken on each bar.

Tensile tests were made on a Baldwin-Southwark hydraulic machine having a load range of 120,000 lb. The standard 0.357-in. diameter tensile bar (Federal Specification QQ-M-151a, Type 4 specimen) was modified slightly to provide shoulders. See Figs. 1 and 2. This modification avoided the difficult operation of machining threads. However, none of the basic test bar dimensions were altered. Thus the results obtained with the modified bar should be directly comparable with those obtained with the standard threaded test bar. An SR-4, PA-3 (Post Yield) electric resistance gage was mounted on each specimen. The cross head speed was 0.005 in. per min. At least two specimens of each composition were tested to determine the average value.

A water-cooled alundum cut-off wheel was used to section the castings for metallographic examination. The sections were then polished on the following grades of paper: 1G, 400, 600, and 00. All of the initial preparation was done under water. The final polish was carried out on a silk lap followed by a miracloth lap using fine alumina.

Mechanical Properties

Some of the mechanical properties of the binary titanium-carbon alloys containing an intended carbon content of 0.25, 0.50, 0.70, and 0.90 per cent were determined. Control specimens prepared from unalloyed "Process A" titanium sponge,* which contains 0.04 per cent carbon, provided a "commercially-pure" alloy as a basis for comparison. The test specimens were machined from a special "keel-block" type casting designed to produce sound test bars in a graphite mold from a minimum quantity of molten metal (Fig. 1).

The average mechanical properties of the different as-cast alloys are shown in Table 1. The gain in strength with increasing carbon content was accompanied by a marked decrease in the ductility. The Brinell hardness was extremely sensitive to additions of carbon, a slight increase in carbon bringing about a rapid rise in hardness, Fig. 3.

Machined graphite molds[8] were used to make the castings but no appreciable amount of carbon was picked up from the mold. This was demonstrated by practically identical superficial hardness values determined on the surface of commercial purity metal cast into both graphite and copper molds.

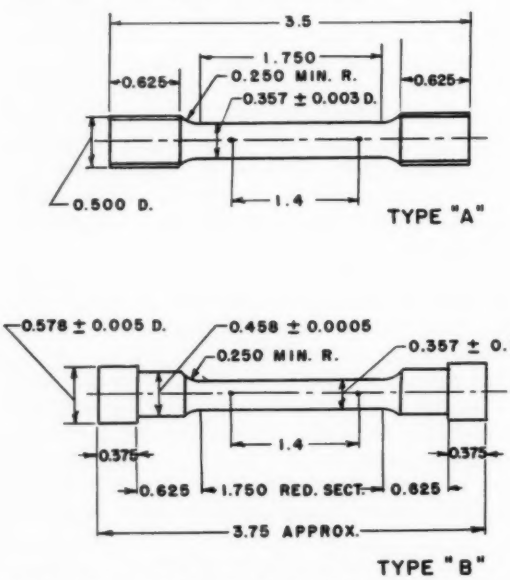


Fig. 2—Type "A" is standard half-size tensile-test specimen with threaded ends. Cross-sectional area is 0.1 sq in. (Federal Spec. QQ-M-151a, Type 4 specimen). Type "B" is essentially same specimen modified to have shoulders to avoid threading operation which is difficult with some titanium alloys.

*Typical analysis of duPont "Process A" titanium sponge.[2]

Ti	= >99.5 per cent	H	= 0.006 to 0.009 per cent
C	= 0.01 to 0.05 per cent	Cl	= 0.05 to 0.10 per cent
O	= 0.06 to 0.10 per cent	Mg	= 0.01 to 0.05 per cent
N	= 0.01 to 0.02 per cent	Fe	= 0.05 to 0.25 per cent

Vickers Hardness Number <180

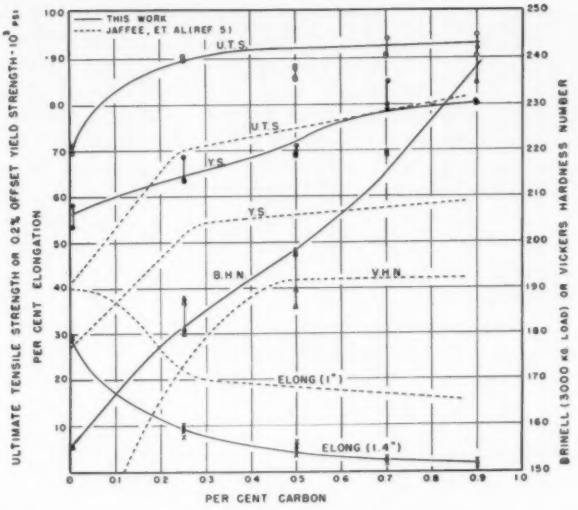


Fig. 3—Mechanical properties of as-cast titanium-carbon alloys. Broken line curves represent data for wrought, high-purity, titanium-carbon alloys and demonstrate that trends in properties are caused by carbon content rather than the treatment.

The moduli of elasticity (Table 2) were determined by using SR-4 strain gages. There was considerable variation in these data, but the values fall within the range reported by other investigators[5,7,9] for wrought alloys. To date, there have been no other reports on this property for as-cast titanium alloys.

Microstructure

The microstructure of these alloys exhibited more carbides than would be anticipated from a study of the binary titanium-carbon equilibrium diagram.[5] This excess may be attributed to the non-equilibrium conditions present in the castings as a result of the rapid cooling which tends to retain the low carbon solubility, characteristic of the beta region at elevated temperature. No specimens with less than 0.25 per cent carbon were cast since this is the minimum carbon content which can be anticipated in induction melted titanium castings.

It is readily apparent in the microstructures of the as-polished specimens showns in Fig. 4 that the amount of the carbide phase increases with increasing carbon content. When these specimens were etched as shown in Figs. 5 and 6 the unalloyed titanium had a typical basket-weave Widmanstatten pattern of transformed beta. With increasing carbon content two-phases become apparent, namely, titanium carbide in an alpha matrix.

Discussion

At the present time either induction or arc melting methods can be used for titanium. For casting purposes, a melting method must provide a sufficient quantity of molten metal having uniform composition and adequate superheat to fill the molds in one continuous pour of short duration. These requirements are further complicated when applied to titanium because it is necessary to conduct all operations without contamination by the atmosphere or other surroundings.

TABLE 2.—MODULI OF ELASTICITY OF CAST TITANIUM-CARBON ALLOYS

Material	Modulus of Elasticity, psi
Cast Titanium, Commercially pure	15,900,000
" " 0.25 per cent carbon (intended)	15,400,000
" " 0.50 " " " "	17,200,000
" " 0.70 " " " "	16,100,000
" " 0.90 " " " "	17,000,000

The foregoing requirements are fulfilled to a great extent by the induction method of melting. Unfortunately however, because all other refractories contaminate the titanium melt and make the metal hard and brittle, graphite is the only refractory now feasible for induction melting. Even so, from 0.3 to 1.0 per cent carbon is always dissolved in the bath during the process. Furthermore, this carbon pickup is unpredictable from heat to heat.

The work described in this paper was undertaken to ascertain how the mechanical properties of cast titanium are affected by the presence of carbon in the range encountered in induction melting practice.

The data in Table 1 shows that only a moderate increase in strength accompanied by a severe loss in ductility results from a small increase in the carbon content. Thus, it is unlikely that cast titanium-carbon binary alloys can be used in parts where toughness is a consideration. It follows that induction melting in a graphite crucible to produce titanium castings would likewise have to be ruled out for many applications because of the low ductility and poor reproducibility of properties.

Improvement of the ductility and other properties of cast titanium-carbon alloys by heat treatment alone is an unlikely prospect. Spheroidization of the carbides in carbon-bearing alloys might increase the ductility provided the matrix is ductile. However, data[5] on wrought titanium-carbon alloys indicate that the grain size cannot be refined and the carbides cannot be diffused at any practical rate by heat treatment alone. Mechanical working must be used in conjunction with heat treatment in order to break up the established grain size and carbide distribution and to bring about spheroidization. Thus, the only likely benefit to be expected from heat treating titanium carbon alloy castings is essentially that of stress relief.

Furthermore, the data on mechanical properties reported for high purity iodide titanium-carbon alloys,[5] which were hot forged, cold rolled, and subsequently annealed for 3½ hr at 1560 F (850 C), are almost identical in trend to those obtained in the present work on castings. See Fig. 3. This similarity in trend indicates that the carbon content of the alloy brings about the marked decrease in the ductility and that even mechanical working and heat treatment cannot alleviate this detrimental effect. The considerable difference in level of the various comparable curves of Fig. 3 may be attributed mainly to the difference

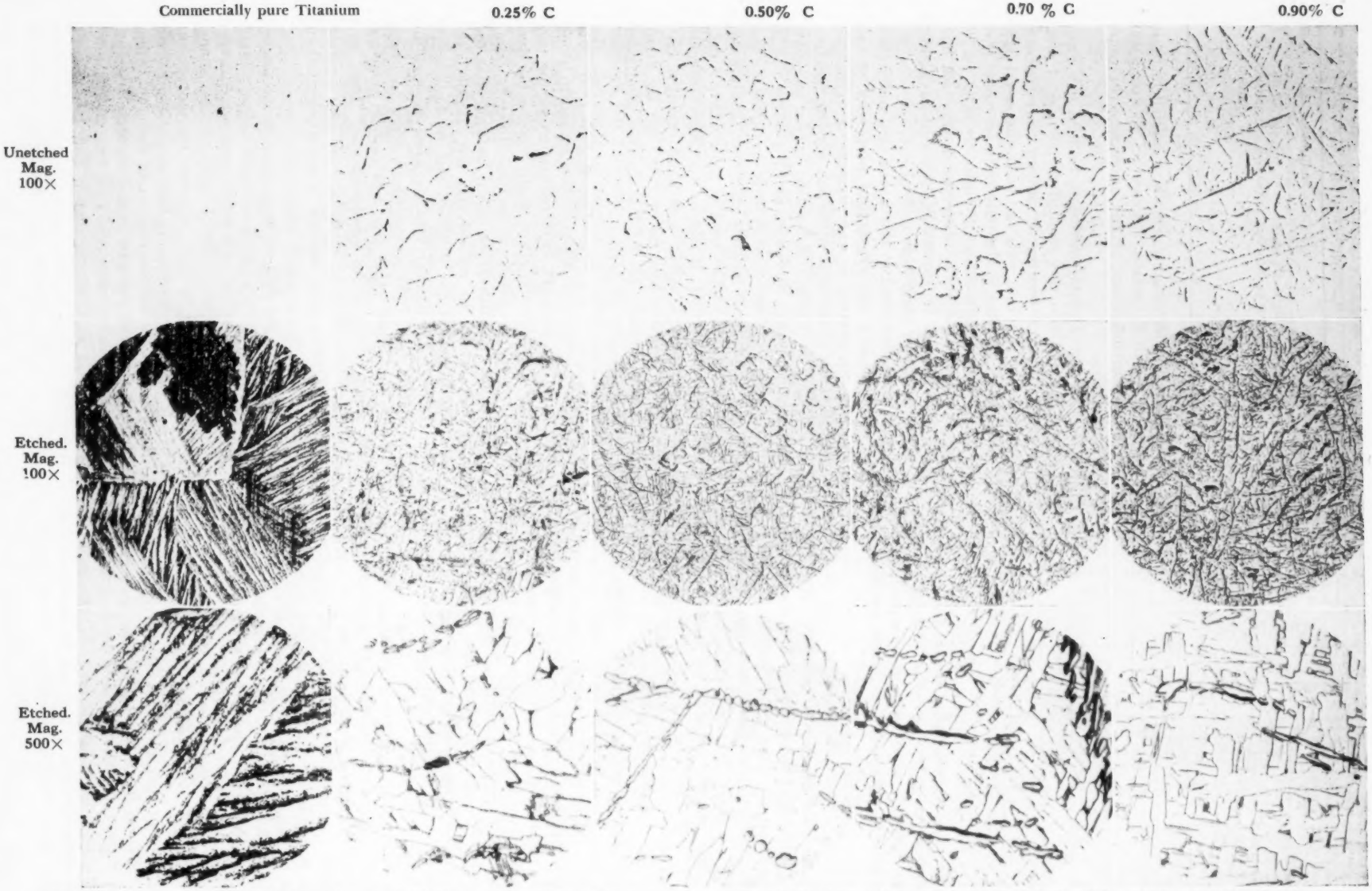


Fig. 4 (Top Row)—As-polished specimens of titanium-carbon alloys show quantity of carbide increases proportionately with carbon content. Fig. 5 (Center Row)—Etched specimens show basket-weave structure of transformed beta phase in unalloyed titanium and carbide distribution in alpha matrix of carbon-bearing alloys. Etchant—60 ml ethylene glycol, 25 ml HNO_3, 1 ml HF. (Bottom Row)—Specimens same as Fig. 5.

in purity of the raw materials used by the various investigators.

The quality of the metal produced for this report was not impaired by the casting operation. This is demonstrated by the Brinell hardness value of 156 on the unalloyed casting which is essentially that of a laboratory test sample melted from the same sponge. The Brinell hardness value is an excellent indication of the degree of contamination present in the specimen, because even a minute quantity of contamination has a very potent effect on the hardness of the metal. When the hardness value is not increased appreciably by the melting operation, it is considered an acceptable indication that no contamination occurred during melting.

Conclusions

1. Cast titanium-carbon alloys show a gradual increase in strength accompanied by a severe decrease in ductility as the carbon is increased up to 0.9 per cent. This will undoubtedly reduce the amount of many other alloying elements that might be profitably added to titanium castings containing carbon.

2. The great decrease in ductility with increasing carbon content is caused by the increasing amount of carbide formed.

3. The induction melting technique is not satisfactory for producing ductile titanium castings. This is true because from 0.3 to 1.0 per cent carbon is always dissolved in the titanium bath during induction melting in a graphite crucible so that a brittle product is obtained. Furthermore, the amount of carbon is not predictable from heat to heat. No other refractory crucible material is known which will produce less seriously embrittled metal.

4. The only immediate benefit to be expected from heat treating titanium-carbon alloy casting is essentially that of stress relief.

Acknowledgments

The authors wish to express their sincere appreciation to Mr. Harold Markus, Director, Metallurgical Laboratory, and to Lt. Col. Daniel J. Murphy, former Chief, Pitman-Dunn Laboratories for providing the opportunity and assistance required in carrying out this study; to Edward C. Olden for the preparation of the photomicrographs; to Lewis W. Berger and Hugh R. McCurdy for preparation of the alloy castings; and to Arthur M. Halstead for conducting the mechanical tests.

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GATING TO CONTROL POURING RATE AND ITS EFFECT ON THE CASTING

By

F. J. McDonald*

As a part of the quality control program, the management of the plant with which the author is associated felt that control of the pouring rate had a large influence on the quality of the castings produced. Investigation showed that many reports had been published on other factors affecting scrap, while very little data were available on the control of the pouring rate. A study consisting of over 1200 hr in the foundry department was made to further investigate and to determine what actually does control the pouring rate.[1]

Although pouring rate will be affected by molding sand conditions, iron temperature and analysis, casting design, gating, type of molding equipment, molding technique, etc., this study deals only with iron pressure and the area through which the iron flows. Thus we take into consideration sprue cups, sprue posts, ladle height, cross-sectional runner area and skim cores.

Many times throughout the article strains and swells are mentioned as being the result of excessive pouring rate. It is recognized that these defects can be caused by other conditions—mainly out-of-range sand, improper amount of jolting, or improper squeezing. Therefore, it is well to mention that the sand used for all test molds was held within the following limits:

Moisture	3.3 pct
Green Compressive Strength	10.0-10.5 psi
Permeability	80-85

The molds were made on automatic jolt squeeze machines which control the amount of jolts and the time and pressure of the squeeze.

The plant is highly mechanized, and the process of making and pouring molds is duplicated many times an hour on production conveyor lines. In order to simulate production technique, all test molds were made and poured in sets of five on a regular production conveyor line. The molding technique was duplicated for each set of molds, and each set was poured by the same iron pourer.

The molds were shaken out manually to keep the gating systems and the castings intact. Gates were allowed to cool, and were then cleaned, weighed and inspected for defects. Thus, for each test mold poured, the following data were recorded: date, time, job number, weight poured, pouring time, total weight of iron poured and any casting defects. Pouring time as expressed in the report was the elapsed time from when the iron started from the ladle spout until it stopped.

The iron flow in these molds was direct from the ladle spout downward into the sprue cup, through the sprue hole and horizontally through the runner into the shrink bobs and into the casting.

Pouring Rate as Affected by Sprue Cups

In the investigation of the effect of sprue cups on pouring rate and casting defects, three different type cups were used, as shown in Fig. 1. All other factors such as sprue hole size, cross-sectional runner area, etc., were kept constant.

The standard cup is used on the majority of production jobs. The ribbed sprue cup is used where it is necessary to place a skim core in the cope mold. The special cup is used rarely but was tested for data purposes.

Two tests were run with each sprue cup—one test with no skim core in the gating system, and one test with a skim core placed in the drag at the base of the sprue hole. The data from the first test is shown at the top of Table 1. The castings of the test were compared, and the only noticeable difference was that those poured with the standard cup showed a few parting fins or a tendency toward strain.

The data of the next test with the skim core in the drag are shown at the bottom of Table 1. The double test series with the special sprue cup was used to see if the data could be duplicated or reproduced. Comparing castings from this test, it was noted that those poured with the special sprue cup showed a smooth and shiny surface. This is an indication that it was close to a misrun condition and that the pouring rate was at its lowest point possible for these castings.

With the sprue cups the only variable in these

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[1] This study was made by Steve Spess in fulfillment of the thesis requirements for a degree.

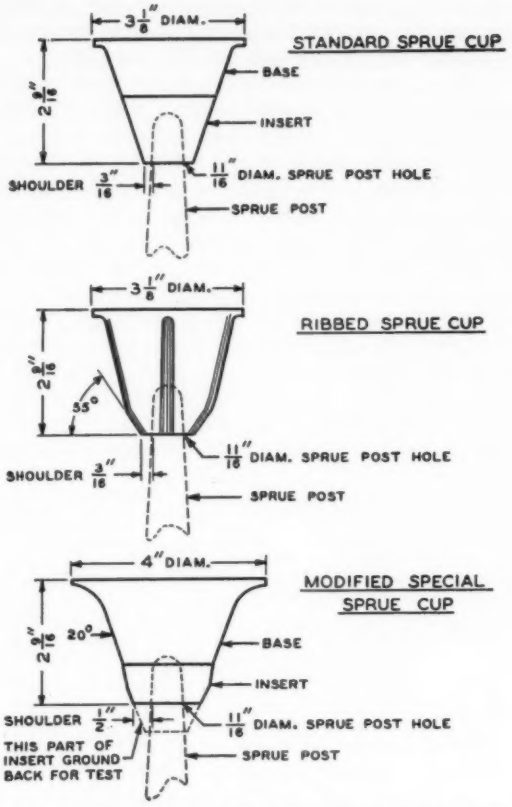


Fig. 1—Sketches of the three different types of sprue cups used in the investigation.

tests, it was concluded that the design of the sprue cup has a definite effect on the pouring rate.

Free falling iron from the ladle spout will gain velocity, but the velocity of the iron hitting a shoulder in the sprue hole will be reduced drastically at that point. Reducing the width of the shoulder or decreasing the angle from the vertical of the side walls of the sprue cup hole will increase the pouring rate if other factors are constant.

The test run with the skim core in the drag showed that the effects of the shoulder and side-wall angle were still present. More information about the skim in the drag appears later in this article.

Pouring Rate as Affected by Sprue Post Diameter

The next tests were to determine how the sprue post diameter or sprue hole cross-sectional area affects the pouring rate. A round sprue post with one to three degrees draft was mounted on all cope patterns. The smallest diameter of the sprue hole in the mold was the diameter of the sprue post at the tip of the sprue cup impression in the mold.

Five sprue posts—each with two degrees draft—4 in. total length, and with base diameters of 3/8, 1/2, 5/8, 3/4 and 7/8 in. were used for this test. Figure 2 shows the 3/4-in. base diameter sprue post and also the cavity in the mold made by the sprue post and sprue cup. Seventy molds were made and poured in these tests.

Table 2 shows the results of the three tests run. It is noted that the sprue post diameter at the tip of the sprue cup is about 1/16-in. smaller than the base diameter because of draft. Twenty-five molds were made without skim cores in the gating system and with total cross-sectional runner area of 0.472 sq. in. Sixty-five pct of the castings with the 5/16-in. minimum diameter holes were misrun. Seven pct of the castings from the 7/16-in. diameter holes were misrun, and the rest had a smooth and shiny surface. No visual defects were noted from the 9/16 and 11/16-in. diameter holes, but a tendency toward strain and fins was noted on the castings from the 13/16-in. diameter sprue hole.

Twenty molds were then made with a skim core in the drag of the gating system. Eight pct of the castings from the 7/16-in. diameter hole were misrun, and the others had a shiny surface. On the castings from the 9/16, 11/16 and 13/16-in. diameter holes there were no visual defects.

Twenty-five molds were then made with the gating system having a cross-sectional runner area of 0.750 sq. in. and no skim cores. Fifty-seven pct of the castings with the 5/16-in. diameter hole were misrun. Two pct of the castings with the 7/16-in. diameter holes were misrun, and others showed a misrun tendency. The 9/16, 11/16 and 13/16-in. diameter holes produced castings with no defects, but as the sprue post hole increased, the castings became rougher and showed a tendency toward strain.

Table 1—Sprue Cup Test Averages

	Pouring Time, min	Poured Weight, lb	Pouring Rate, lb/min
Test No. 1—Without Skim Core			
Standard Sprue Cup	0.096	17.55	182.8
Ribbed Sprue Cup	0.109	18.25	167.4
Special Sprue Cup (modified)	0.116	16.35	140.9
Test No. 2—Skim Core in Drag			
Standard Sprue Cup	.115	15.4	134.0
Ribbed Sprue Cup	.117	15.35	131.2
Special Sprue Cop (modified)	.132	14.75	111.8
Special Sprue Cup (modified)	.134	14.90	111.3

Plotting the sprue post cross-sectional area against the pouring rate, we find the pouring rate will increase until the sprue-post area approaches the smallest area in the gating system. A sprue-post area larger than the runner area would be of no value in increasing the pouring rate. Conversely, where skim cores are used only as a choke source, the same effect can be obtained by eliminating the skim core and using a smaller sprue post.

Pouring Rate as Affected by Sprue Post Length

The next test was to determine what effect sprue-post length had on the pouring rate. The test molds were made off the same pattern on the experimental floor. All drag molds were the same while the cope molds were made 4, 6 and 8 in. high. The sprue holes were cut with the same hand sprue cutter, thus giving all molds the same sprue hole cross-sectional area at the smallest point.

To eliminate the pouring height variable, the pouring spout was kept at a constant height of 14 in. from the parting of the test molds, and the iron was poured directly into the sprue cup. Table 3 shows the data accumulated from these tests. There were no casting defects nor difference in the appearance of the castings. There was a considerable amount of slag in the runners, but none in the castings. Varying only the cope height does not vary the pouring rate. The pressure head is determined by the height of the ladle from the parting line and not by the height of the cope mold. The effect of side-wall resistance of the longer sprue holes is nil. In this test, the effective sprue cross-sectional area was 0.248 sq. in. while the runner cross-sectional area was 1.03 sq. in. This condition gave us a low iron velocity through the runner and allowed the slag to come to the top and be trapped in the runner.

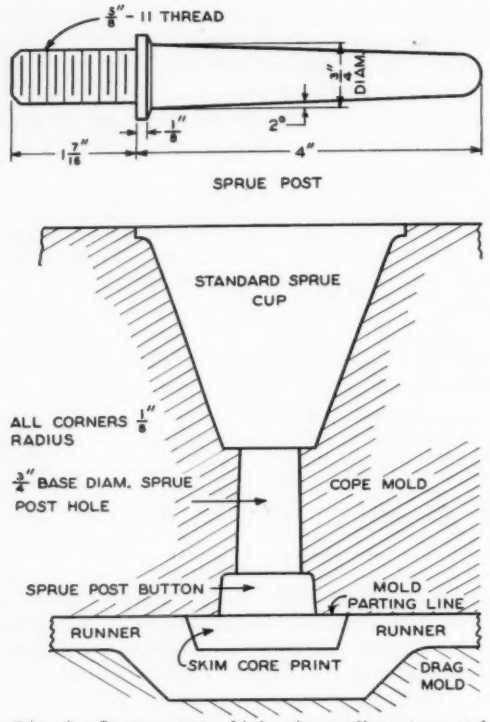


Fig. 2—Sprue post—¾-in. base diameter, and mold cavity.

Pouring Rate as Affected by Runner Area

As the iron leaves the base of the sprue post, it changes direction from vertical movement to horizontal movement and enters the runner. The effect of runner cross-sectional area on the pouring rate was studied next.

It is fundamental to say the runner cross-sectional area should be large enough to get the molten iron into the mold cavity with the least amount of restriction. However, in some cases it is necessary to choke the runner to eliminate strains, swells, run-out or core wash. At this plant all runners have ten degrees draft on each side, and similar jobs have similar runner area. Two separate tests were made, varying only the runner cross-sectional area, and keeping the sprue hole, runner length and ladle height the same.

Twelve test molds were made in this phase of the experiment—each two molds with a different sized runner. The results are shown in Table 4. Of the 12 test castings, only one from the 0.562 sq. in. runner was a good casting and free of slag. On the second test, one mold with a smaller runner and one with a larger runner were poured consecutively out of the same ladle. The results are shown at the bottom of Table 4. The castings with the small runner area were scrap due to slag, and those from the large runner area showed slag in the first few inches of runner but none in the casting.

These tests show that increasing the runner cross-sectional area increases the pouring rate until the runner cross-sectional area equals the sprue post cross-

TABLE 2—SPRUE POST DIAMETER TEST AVERAGES

Sprue Post Dia. at Sprue Cup, in.	Sprue Post Area at Sprue Cup, sq in.	Pouring Time, min	Poured Weight, lb	Pouring Rate, lb/min
Test No. 1—Without Skim Core—0.472 sq in. cross-sectional runner area				
5/16	0.0767	0.208	18.40	87.7
7/16	0.1503	0.200	20.10	100.5
9/16	0.2485	0.130	19.83	152.5
11/16	0.3712	0.125	20.44	163.5
13/16	0.5185	0.114	20.48	179.6
Test No. 2—Skim Core in Drag—0.472 sq in. sectional runner area				
7/16	0.1503	0.183	17.85	97.5
9/16	0.2485	0.140	18.08	129.1
11/16	0.3712	0.138	18.05	130.8
13/16	0.5185	0.130	18.48	142.1
Test No. 3—Without Skim Core—0.750 sq in. cross-sectional runner area				
11/32	0.0928	0.260	16.47	63.3
15/32	0.1726	0.128	17.58	137.3
19/32	0.2769	0.110	17.50	159.1
23/32	0.4057	0.094	17.25	183.5
27/32	0.5591	0.076	18.08	237.9

TABLE 3—SPRUE POST LENGTH TEST AVERAGES

Cope Size, in.	Length of Sprue Post, in.	Pouring Time, min	Poured Weight, lb	Pouring Rate, lb/min
4	1 5/8	0.120	14.38	119.8
6	3 5/8	0.121	14.50	119.8
8	5 5/8	0.120	14.50	120.8

TABLE 4—RUNNER AREA TEST AVERAGES

Runner Area, sq in.	Pouring Time, min	Poured Weight, lb	Pouring Rate, lb/min	Casting Defects by Pieces
		Test No. 1		
0.098	0.225	13.00	57.8	2 pcs. slag
0.162	0.165	13.38	81.1	2 pcs. slag
0.250	0.135	13.50	100.0	2 pcs. slag
0.310	0.130	13.68	105.2	2 pcs. slag
0.430	0.130	13.88	106.8	2 pcs. slag
0.562	0.130	14.12	108.6	1 pc. slag, 1 pc. OK
		Test No. 2		
0.250	0.160	16.13	100.9	2 pcs. slag
1.031	0.160	16.13	100.9	none

sectional area. From there on, increasing the runner area does not appreciably affect the pouring rate. Making the runner area four times as big as the sprue-post area has no effect on the pouring rate.

From the continuity equation (quantity equals area times velocity), it is determined that if the pouring rate is constant and the runner area is increased, the iron velocity must decrease, and that is what happened in the second test. Using the equation and the data accumulated, it is determined that an iron velocity of 0.750 ft/sec in 5 in. of runner length will eliminate slag defects without benefit of skim and slag cores in the gating system. The runner must be full of iron to obtain trapping action.

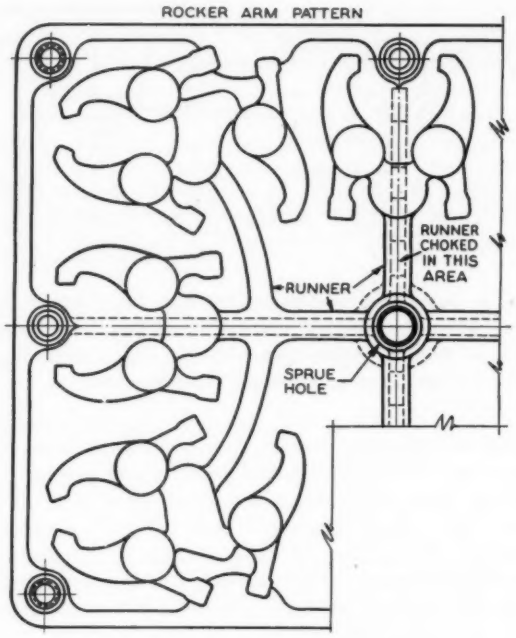


Fig. 3—Rocker arm pattern.

Pouring Rate as Affected by Runner Chokes

The next test was made to determine the effect of runner chokes on the pouring rate and casting defects. Runners are choked to maintain constant flow of iron to all mold cavities and to minimize or eliminate casting defects such as strain, swells, run-out and core wash. A runner choke is simply some point on the runner that has been cut down to give less runner cross-sectional area at that point. Two tests of ten molds each were run on the rocker arm pattern shown in Fig. 3.

In the first test the molds had a skim core in the drag, and in the second test the skim core was not used. In each test five molds were made with chokes in the runners and five with no chokes. The results are shown at the top of Table 5. There were no scrap castings from any of the test molds, but those made without the skim core and not choked had more parting fin and a tendency toward strain. They also had the highest pouring rate.

The total sprue post cross-sectional area is 0.371 sq. in., and the total runner cross-sectional area is 0.750 sq. in. The two runners feeding only two castings (Fig. 3) were choked down to 0.250 sq. in. The two runners feeding the eight castings were choked down to 0.222 sq. in. The total runner cross-sectional area was 0.472 sq. in., and even though this total area was larger than the sprue-post area, the pouring rate increased when the chokes were eliminated because 80 pct of the iron passed through the runner cross-sectional area of 0.222 sq. in.

Fifteen test molds were also made from a larger equipment with four differential case patterns mounted on it. The average results are shown at the bottom of Table 5. There were no casting defects.

TABLE 5—RUNNER CHOKE TEST AVERAGES

Minimum Total Runner Area at Choke Location, sq in.	Pouring Time, min	Poured Weight, lb	Pouring Rate, lb/min
Test No. 1			
0.472 skim core drag	0.132	18.20	138.0
0.750 skim core drag	0.116	18.20	157.0
0.472 no skim core	0.122	18.70	152.2
0.750 no skim core	0.106	18.98	179.0
Test No. 2			
Runners choked	0.658	106	161
Runners not choked	0.644	106	165

Sprue hole cross-sectional area was 0.371 sq. in. The total runner cross-sectional area leaving the base of the sprue post was 1.12 sq. in. This was choked down to a total runner cross sectional area of 0.420 sq. in.

It is apparent from the results that removing the chokes had little effect on the pouring rate because the sprue hole area was already smaller than the runner area at the chokes. The results show that runner chokes control the pouring rate if they are the least area in the gating system. Casting defects such as strain, swell, run-out and core wash can be minimized by choking runners and reducing the pouring rate, and misruns can be reduced by eliminating chokes and increasing the pouring rate.

Pouring Rate as Affected by Ladle Height

The factors affecting pouring rate that have been discussed thus far are parts of the permanent gating system. In the actual pouring of the mold a variable to be considered is the height of the pouring spout above the parting line. This is controlled by the iron pourer, and this section is a study of the effect of ladle height above the parting line on the pouring rate.

The ladles used at the plant are teapot type, holding between 600 and 700 lb of iron. The ladles hang from the single cable of an electric hoist which is on an overhead monorail (Fig. 4). Horizontal movement is obtained by the iron pourer holding the ladle in front of him as he rides on an escalator. Vertical movement is obtained by pressing the up or down button on the electric hoist. The ladle is tilted to pour iron out, and as the iron level decreases, the ladle is tilted more. This causes the distance between the spout and the parting line of the mold to decrease. The electric hoist makes it very easy to adjust

for this and maintain a uniform pouring height by raising the ladle.

Three different tests were run in this investigation using ladle heights—spout to cope mold—of zero, 6, 9, 12 and 18 in. The cope mold heights were kept constant. Zero height was obtained by pouring the iron into a pouring basin in the cope mold along side of and connected to the sprue cup hole. In this test 55 molds of a rocker arm composite were poured. Twenty molds had runners choked and no skim core; 15 had runners choked and a skim core in the drag. Twenty molds had no runners choked and no skim cores. The results of this series of tests are shown in Table 6.

In the test with the runners choked and without skim cores, 8 pct of the castings poured from the zero height were misruns. No other castings showed defects, but those poured from the 12-in. height showed some strain and some parting fin. At 18-in. height the iron pourer had difficulty pouring the iron directly into the sprue cup.

The results of the test molds with runners choked and a skim core in the drag are shown next in Table 6. (There were no molds poured from zero inches in this test since 8 pct of those with the skim core in the drag had misruns.) There were no casting defects detected in these 15 molds. Next in Table 6 are shown the results of test molds with no skim cores and no runner chokes. Six pct of the castings poured from zero inches were misrun, and castings poured from 6, 9 and 12 in. were strained.

Fifteen test molds from another part using the skim core in the drag were poured. The results of these molds are shown at the bottom of Table 6. No visual defects were noted, although more fins or strain tendency were noticeable as pouring rate increased.

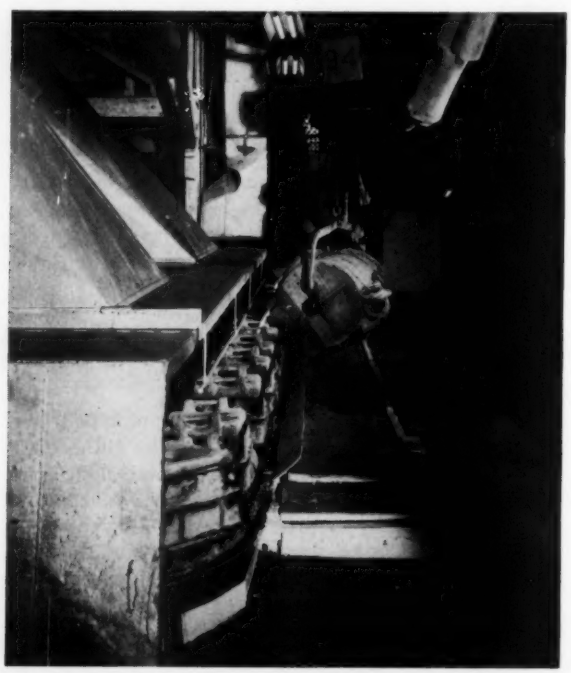


Fig. 4—Pouring from teapot type ladle suspended from electric hoist on overhead monorail.

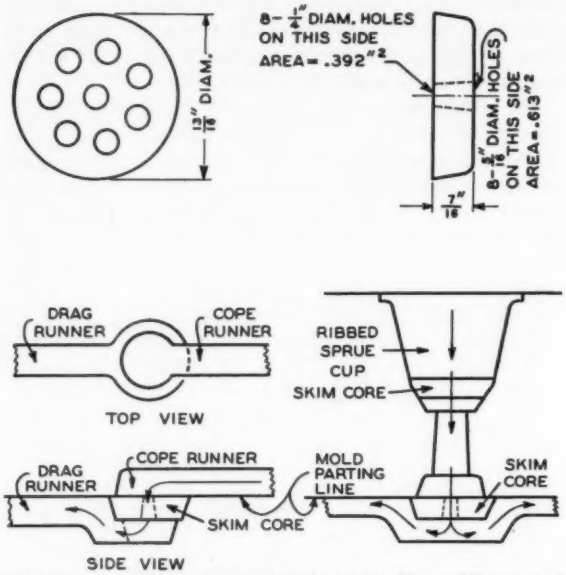


Fig. 5—Skim core and where located. Top—Views and dimensions of skim core. Bottom, left—Skim core in the runner. Bottom, right—Skim core in cope (sprue cup) and in drag (at base of sprue post).

Velocity equals gravity times time, and pressure equals height times density. When the iron enters the sprue-cup hole, it has gained velocity and pressure in falling from the ladle spout to the sprue hole. The iron also gains pressure and velocity dropping through the sprue-post hole. When it reaches the parting line and changes direction, the pressure and velocity are used to force the iron into the runners and into the mold cavities.

All of the results show that increasing the pouring height from zero to 12 in. increases the pouring rate. Increasing the pouring height over 12 in. reduced the pouring rate slightly. The effect of increasing the pouring height is dependent upon the permanent gating system, except where there is a skim core used in the cope. Increasing the pouring height on parts

TABLE 6—LADLE HEIGHT TEST AVERAGES

Ladle Height, in.	Pouring Time, min	Poured Weight, lb	Pouring Rate, lb/min
Test No. 1—Runners Choked—No Skim Core			
0	0.205	20.43	99.3
6	0.136	18.63	137.0
12	0.120	20.05	167.1
18	0.140	20.18	144.1
Test No. 2—Runner Choked—Skim Core Drag			
6	0.152	18.05	118.8
9	0.138	18.85	136.6
12	0.140	19.08	136.3
Test No. 3—No Runners Choked—No Skim Core Drag			
0	0.170	19.29	113.5
6	0.095	19.28	202.9
9	0.092	19.83	215.5
12	0.094	20.18	214.7
Test No. 4—Molds of Different Part Using Skim Core in Drag Mold			
6	0.118	18.10	153.4
9	0.100	18.03	180.3
12	0.104	18.33	176.3

running with skim cores in the cope only slightly increases the pouring rate. Casting defects such as misruns and strains can be eliminated or minimized by increasing or decreasing the ladle height.

Pouring Rate as Affected by Skim Cores

The next series of tests had to do with the effect of skim cores on the pouring rate. Figure 5 shows the skim core referred to and the three methods in which it can be used in the gating system. It is a baked sand core with eight 1/4-in. diameter, tapered holes. It is used in the sprue-cup hole in the cope, at the base of the sprue-cup hole in the drag, or in the runner. It is used to control pouring rate and minimize slag defects.

Since there were many tests and the data were quite lengthy, it is not published in this report. However, it might be interesting to discuss what the results did show. It is noticed that as the pouring rate increased, the severity of slag increased on parts prone to slag. Slag defects were found regardless of whether or not, skim cores were used.

From the tests conducted on runner area, it was found that as the iron velocity in the runner decreases, slag defects are minimized. Therefore, skim cores do not remove slag directly, but by reducing the pouring rate and the iron velocity in a constant runner area, it allows more slag to adhere to the top of the runner. The skim core in the cope is the most effective in reducing the pouring rate, while the skim core in the runner is practically ineffective in reducing the pouring rate.

Skim cores in the gating system can be eliminated if other means such as smaller sprue posts or choking the runner are used to control pouring rate.

Since the tests described in this article were made using production equipment and under production conditions, the conclusions are applicable.

Many skim cores that tests showed were being used solely for the control of the pouring rate have been eliminated in this plant. This has resulted in considerable savings in materials and labor. Production was increased when the molder was relieved of setting the skim cores. Standard sprue-post diameters or runner chokes are used more extensively as controls of pouring rate.

Pouring guide bars as shown in Fig. 4 have been installed on all conveyor lines. This has resulted in a constant pouring height since the iron pourer adjusts the spout to the guide. Casting thickness variation caused by uncontrolled ladle height has been virtually eliminated.

HARDENING CHARACTERISTICS OF INDUCTION HEATED DUCTILE IRON

By

Edward P. Rowady, William J. Murphy and Joseph F. Libsch*

ABSTRACT

The hardening characteristics of rapidly heated ductile (spheroidal or nodular graphite) iron have been related to its isothermally transformed microstructures and mechanical properties. To facilitate the selection of various prior structures, isothermal transformation diagrams of two heats of ductile iron varying only in silicon content were determined. The I-T diagrams include lines representing complete ferritization.

Response to induction hardening improves as the amount of pearlite in the prior structure increases, as the austenitizing temperature and time increase, and as the spheroidal graphite count increases. Completely ferritized ductile iron responded to hardening much more rapidly than a lot of ferritic malleable iron. Instantaneous response to induction hardening appears possible only with pearlitic-ferritic structures which show intermediate ductility, i.e., 8-10 per cent reduction of area. Retained austenite occurs in the hardened structure if the austenitizing temperature or time are excessive.

Silicon increases the lower critical temperature, tends to reduce the time required for complete ferritization, and appears to retard somewhat the rate at which carbon diffuses to provide homogeneous and relatively high carbon austenite.

In general, the results indicate that surface hardening ductile iron by induction or flame heating should provide iron castings with high surface hardness (R_c58) and good core properties.

Introduction

Recent publications[1,2] on ductile (spheroidal or nodular graphite) iron have reported mechanical properties developed by suitable heat treatment, which offer many interesting engineering possibilities. Among these possibilities is that of surface hardening ductile iron to provide a hard surface supported by a ductile core of good strength. Thus induction or flame surface-hardened ductile iron may provide a unique combination of properties for the engineering application of iron castings.

Of particular significance for surface hardening is the ability of ductile iron to develop a high degree of ductility combined with good strength. This ability is primarily the result of special alloy additions, particularly magnesium,[3,4] which cause the graphite to form in the shape of spheroids, rather than graphite flakes, when the molten iron is cooled. The methods of adding the special agents and their influence have been described by a number of recent investigators.[5,2,6,3]

The actual ductility and strength of ductile iron vary with its structure as developed by heat treatment.[4] Maximum ductility is secured in the annealed or "ferritized" condition which can be obtained by heating the casting to 1600-1700 F (870-925 C), followed by isothermal treatment at 1300 F (705 C) for one or more hours. This treatment results in a ferritic material containing spheroidal graphite. The properties resulting from such a heat treatment are: tensile strength, 65,000-75,000 psi; yield strength, 50,000-60,000 psi; elongation, 17-23 per cent; hardness, 140-180 Bhn. Incompletely ferritized castings show lower ductility, but better strength, since they contain varying amounts of pearlite in the matrix. The range of properties exhibited by ductile iron is thus typical of core properties considered desirable in a surface hardened section.

Surface hardening, however, requires selective heating of the surface. This can be accomplished only by such rapid heating techniques as induction and flame heating, which minimize penetration of heat to the interior of the piece. Unfortunately, rapid heating provides a minimum of time for diffusion of carbon to form reasonably homogeneous austenite prior to quenching. Whether ductile iron structures showing suitable core properties can be successfully hardened by rapid heating is of considerable interest.

Several studies have been made to determine the response of gray cast iron and pearlitic malleable iron to induction heating. Walton and Osborn[7] studied the response of two quality controlled gray irons to short heating cycles, and concluded that matrices devoid of free ferrite and consisting of either fine pearlite or sorbite and pearlite respond almost instantaneously. Sloan and Hays[8] studied the application of induction heating to alloy cast iron. The surface hardening of pearlitic malleable irons was investigated by Bush, Rote and Wood[9] and satisfactory results were obtained with a variety of compositions and prior structures.

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It is the purpose of the present paper to report the initial results of a project designed to study the hardening characteristics of rapidly heated ductile iron. Since the prior structure markedly influences both the mechanical properties and response to rapid heating, the isothermal transformation characteristics of ductile iron cooled from the austenite range have also been studied. Although the present experiments were limited to heating by induction, the data are believed useful in flame heating and welding processes which also involve rapid heating.

Material

The chemical compositions for four heats of magnesium treated ductile iron and a heat of malleable iron, received for use in this study, are in Table 1.

TABLE 1—CHEMICAL COMPOSITION OF DUCTILE IRON HEATS AND MALLEABLE IRON USED

Heat	T.C.	Si	Mn	Ni	P	S
K	3.42	1.87	0.32	0.75	0.034	0.01
N	3.49	2.98	0.34	0.82	0.034	0.01
D	3.60	2.50	0.60	1.50	0.080	—
P	3.63	1.88	0.22	1.25	—	—
Malleable	1.68	1.25	0.25	0.050	0.050	—

Heats K and N, received in the form of 1-in. keel blocks, differ primarily in silicon content and these two heats were used throughout most of the investigation. Heat P was used only to determine the influence of spheroidal graphite count upon hardening response, and was received as castings of varying section size. Heat D was used to study the mode of carbon solution by microscopic examination and to compare the hardening characteristics of ductile iron and malleable iron.

Development of Prior Structure—Isothermal Transformation

Both the response to rapid heating and the properties of a surface hardened part depend fundamentally upon the prior structure or the distribution of the ferrite, carbide and graphite phases. It appeared desirable, therefore, to study the development of various prior structures in ductile iron by suitable heat treatment. A wide variation of prior structures is readily obtained by isothermal transformation at subcritical temperatures after heating to form austenite. Furthermore, since ductile iron is often heat-treated commercially in this manner to develop optimum ductility, it was believed that isothermal transformation diagrams indicating the progress of ferritization, as well as the transformation of austenite would be useful. Accordingly, isothermal transformation diagrams were developed for Heat K, representing low silicon content (1.87 per cent) and Heat N, representing high silicon content (2.98 per cent).

Procedure: The interrupted quench method of Davenport and Bain[10] was employed. The specimens, approximately $\frac{1}{16}$ in. thick, were cut from milled (Heat K) or 30 min (Heat N), and the specimens legs of the 1-in. keel blocks. Austenitization was performed in a lead bath at 1600 F (870 C) for 15 min then rapidly transferred to lead baths maintained at temperatures ranging from 800 to 1350 F (425-735 C) for isothermal transformation, followed by a brine quench after the desired time at the isothermal temperature. Rockwell A hardness readings and microexamination were used to follow the course of isothermal transformation, the combined observations being correlated to establish the I-T diagrams.

Isothermal Transformation Diagrams: The upper portion of the isothermal transformation diagrams determined for the two heats of ductile iron are given in Fig. 1. The diagrams indicate not only the time required for the beginning and ending of austenite transformation, but the approximate time required for complete ferritization.

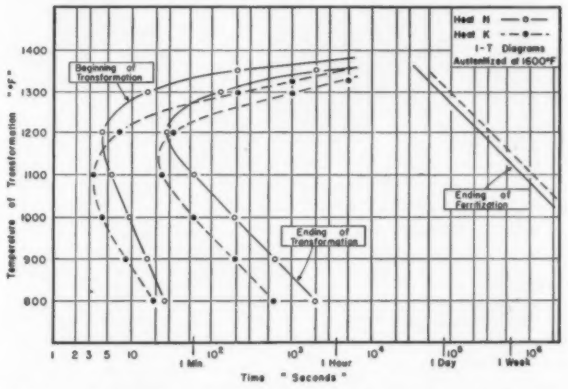


Fig. 1—Isothermal transformation diagrams for ductile iron; Heats K and N.

The inclusion of lines representing the approximate completion of ferritization is necessary in ductile iron because the initial products of austenite decomposition are not stable but decompose to a stable structure, consisting of a ferrite matrix and spheroidal graphite. This process is generally termed ferritization, and its progress is of particular interest in the present investigation because it markedly influences both the properties and hardening response of ductile iron to rapid heating.

In general, the shape of the curves representing the progress of austenite decomposition are similar to those for a medium or high carbon steel containing an appreciable amount of silicon, i.e., 9260 steel;[11] although the nickel content of the ductile iron heats (.75/.83 per cent) appears to have shifted the curves slightly to the right. Complete transformation occurs in a relatively short period of time, regardless of the isothermal transformation temperatures studied.

The time required for complete ferritization, on the other hand, may be quite long, and increases rapidly as the isothermal transformation temperature is decreased. While complete ferritization occurred in approximately 25 hr at 1300 F (705 C) for Heat K, it required 195 hr to completely ferritize this material at 1140 F (615 C). This is due, of course, to the much slower rate of diffusion for carbon at the lower temperatures.

It is interesting to note from Fig. 1 that the higher silicon content of Heat N has raised the lower crit-

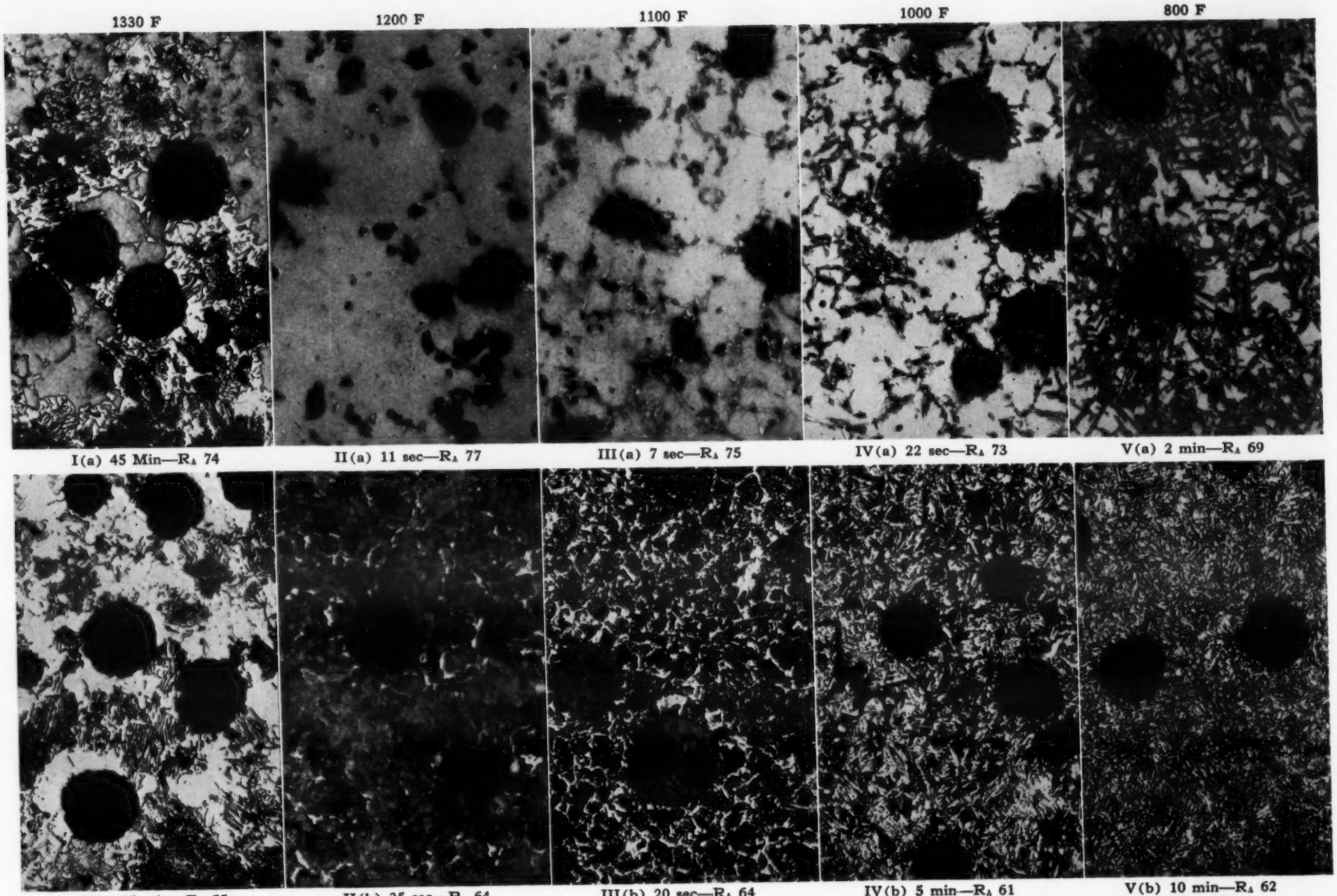
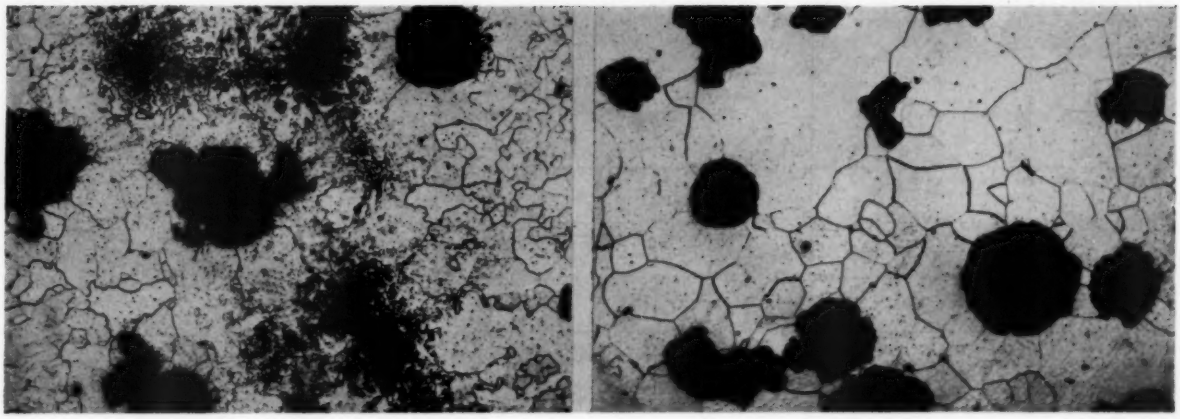


Fig. 2—Microstructure and hardness of (a) partially trans-formed and (b) completely transformed structures of Heat K. Picral-nital etch. Mag - 250x.



A—1200 F, 20.5 hr, R_A 52 B—1350 F, 16.5 hr, R_A 50

Fig. 3—Typical microstructure and hardness of (a) partly ferritized and (b) completely ferritized structures; Heat N. Nital etch. Mag. - 250x.

ical temperature. This is in accord with results found on gray cast iron by other investigators.[12] Although sufficient data are lacking for accurate comparison, it appears that the increased silicon content has also reduced the time for complete ferritization at constant temperature.

The actual microstructure and hardness of some partially transformed and completely transformed samples of Heat K are given in Fig. 2. The products shown are typical of those found in isothermal studies of gray cast iron and pearlitic malleable cast iron.[12,13] The products of transformation at 1330 F (720 C) are composed of coarse pearlite, ferrite and spheroidal graphite. As the temperature of isothermal transformation decreases, the pearlite spacing becomes progressively smaller and the hardness increases. Products transformed just below the nose of the isothermal transformation diagram, i.e., 1000 F (540 C) are predominantly acicular in nature and have somewhat lower hardness. Flinn, Cohen and Chipman[14] concluded from their investigation of acicular structures in nickel-molybdenum cast irons that the acicular structures formed below the nose of the isothermal transformation curves were typical of the upper portion of a range of transformation products nucleated by ferrite. The structures shown in Fig. 2 for Heat K are also characteristic of those found in Heat N.

Typical microstructures and hardness values of partly ferritized and completely ferritized samples are given in Fig. 3. These structures are of Heat N; Figure 3 (a), representing the structure of a specimen held at 1200 F (650 C) for 20.5 hr, and Figure 3 (b), a specimen held at 1350 F (735 C) for 16.5 hr. It is apparent that during ferritization the pearlite gradually spheroidizes and finally is eliminated by diffusion and deposition of carbon on the spheroidal graphite. The hardness, meanwhile, decreases very gradually, while the ductility increases to a maximum in the completely ferritized structure. The mechanical properties of the completely ferritized structure of Heat N, for example, are T.S. = 71,000 psi, per cent elongation in ¾ in. = 22.2 per cent, per cent Red. of Area = 24.2 per cent, and impact energy absorbed for unnotched Charpy specimen = 102 ft-lb.

Hardening Characteristics—Response to Induction Heating

Such processes as induction surface hardening, flame hardening and welding, involve heating to the austenite range and are characterized by very rapid heating rates and very short holding times at elevated temperatures. As a result, the austenite formed prior to cooling is not generally homogeneous, but consists of carbon and alloy concentration gradients which may markedly influence the transformation products and, therefore, the mechanical properties upon subsequent cooling or quenching to room temperature.[15,16] The extent of the gradients developed depends upon a number of factors.

Of primary importance is the prior structure, which determines the distance through which carbon must diffuse. Austenitizing temperatures are of great consequence in that higher temperatures provide more rapid rates of carbon solution. The effect of longer austenitizing times to allow greater carbon solution, needs no comment. Alloying elements may determine to a great extent the prior structure, and thus indirectly influence response to hardening, but they may also affect the rates of carbon solution and diffusion at the austenitizing temperature.

Ductile iron is particularly interesting to study in this connection because it contains not only the carbide and ferrite phases found in steel, but spheroidal graphite as well. In partially and completely ferritized structures which exhibit optimum ductility, the graphitic spheroids may supply sufficient carbon to the austenite for successful hardening upon quenching, provided enough diffusion occurs in the period of time allowed. Naturally, the time required will be a maximum for fully ferritized structures. In a preliminary study with fully malleabilized cast iron, Kerr[17] found that the time required for reasonable homogenization of austenite upon induction heating and holding at the austenitizing temperature was in the order of several minutes. Two questions, there-

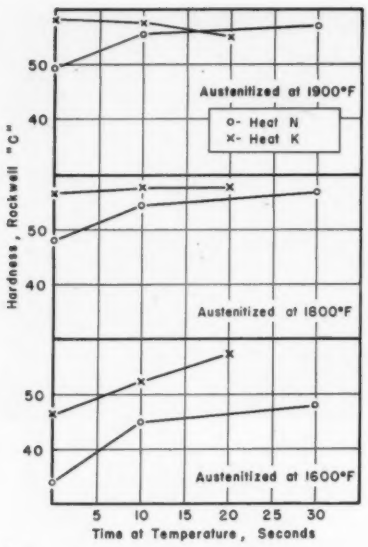


Fig. 4—Influence of austenitizing temperature and time upon hardness of induction-treated specimens. Prior structure isothermally transformed at 1300 F for 1 hr. Heated to 1600 F, 1800 F and 1900 F and brine quenched.

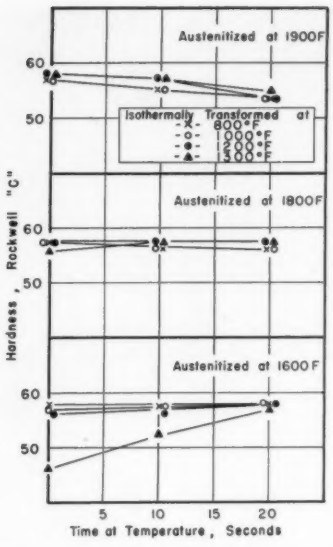


Fig. 5—Influence of prior structure on hardness of induction-heated specimens; Heat K. Completely transformed isothermally without appreciable ferritization at temperatures indicated. Heated to 1600 F, 1800 F and 1900 F and brine quenched.

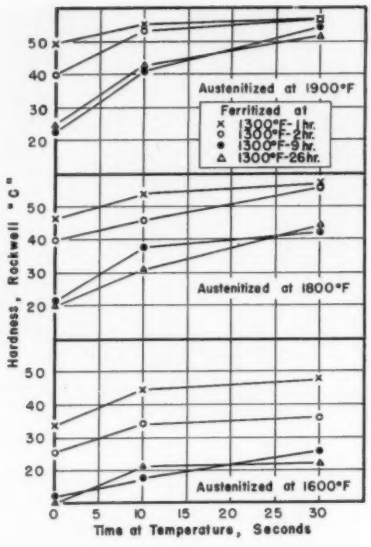


Fig. 6—Influence of ferritization on hardness of induction-heated specimens; Heat N. Isothermally transformed and ferritized at 1300 F for 1, 2, 9 and 26 hr. Heated to 1600 F, 1800 F and 1900 F and brine quenched.

fore, develop: (1) can fully ferritized ductile iron be successfully hardened by rapid heating?, and (2), if not, what degree of ferritization will respond to rapid heating to form austenite with sufficient homogeneity and carbon content so that hardening will be successful?

Procedure: Induction heating was chosen as the method for rapidly heating specimens of ductile iron to study hardening response. This method of heating allows control of both temperature and time of heating within reasonable limits, by use of a chromel-alumel thermocouple whose elements are individually welded to the surface of the specimen, and a modified high-speed recording instrument.[16]

The specimens used were machined from square rods obtained from the legs of 1-in. keel blocks after heat treatment to establish various prior structures in accordance with the isothermal transformation diagrams of Fig. 1. The final size of the specimens was ⅜-in. in diameter by 1-in. long.

A 30 kw high frequency converter, operating at a frequency of approximately 300,000 cps was employed for induction heating. The heating coil was a simple solenoid, with an internal diameter of ⅝-in. With this arrangement, specimens were heated to temperatures of 1600-1900 F (870-1035 C) in 4.0 to 5.5 sec. When required, specimens were maintained at temperature for 10, 20 or 30 sec before quenching in brine.

Hardness and microscopic examination of the brine-quenched samples were used to study the progress of carbon solution and diffusion, and austenite homogenization.

It should be noted that the specimens heated by induction in the present study were small, to allow substantially uniform heating throughout the cross-section. No attempt was made to obtain a case hardened specimen in the experiments reported.

Influence of Temperature and Time: Figure 4 shows the influence of austenitizing temperature and time upon the hardening response of rapidly heated ductile iron samples from both Heat K and Heat N. The results shown are for samples isothermally transformed at 1300 F (705 C) for 1 hr prior to induction heating, and, therefore, represent structures formed by complete transformation of austenite to ferrite and pearlite (and spheroidal graphite) with some ferritization (Fig. 2-Ib).

It is evident that both higher austenitizing temperatures and longer austenitizing times help to promote maximum hardness upon quenching. This maximum hardness is approximately R_c58 and is attained readily in Heat K in the short time (4-5.5 sec) required for heating to 1800 F (980 C) and 1900 F (1035 C). However, maximum hardness is not attained for this prior structure in Heat N at any temperature unless sufficient time at temperature is provided.

Influence of Prior Structure — Not Appreciably Ferritized: The influence of various prior structures upon the quenched hardness of rapidly heated specimens of Heat K is shown in Fig. 5. The prior structures represented are for specimens isothermally transformed at 800 F (425 C) for 30 min, 1000 F (540 C) for 20 min, 1200 F (650 C) for 15 min and 1300 F (705 C) for 1 hr. These structures are characteristic of specimens in which the austenite has transformed completely but in which little ferritization has oc-

curred. They are similar to those shown in Fig. 2, Vb, IVb, IIb and Ib, respectively.

It is apparent that all of these structures respond almost instantaneously to induction heating at all austenitizing temperatures to provide maximum hardness upon quenching. The only exception is the structure isothermally transformed at 1300 F (705 C), which provides low hardness after rapid heating to 1600 F (870 C). The decrease in hardness with austenitizing time at 1900 F (1035 C) is caused by retained austenite, as will be demonstrated subsequently.

The mechanical properties of the prior structures represented in Fig. 5 are given in Table 2. Although determined on duplicate tensile and unnotched Charpy impact specimens, these results should be considered of an approximate nature because some evidence of flake graphite was found in microscopic examination of Heat K, and limited material forced a choice of undersize tensile bars, e.g., specimen diameter $\frac{3}{16}$ in., gage length $\frac{3}{4}$ in. The presence of flake graphite would decrease the per cent reduction of area and the per cent elongation,[18] while a short gage length would increase the per cent elongation in specimens of high ductility.[19] For comparison, the properties of a fully ferritized structure of Heat K are also given in Table 2.

Table 2—Mechanical Properties of Prior Structures Completely Transformed Isothermally Without Appreciable Ferritization

Heat K

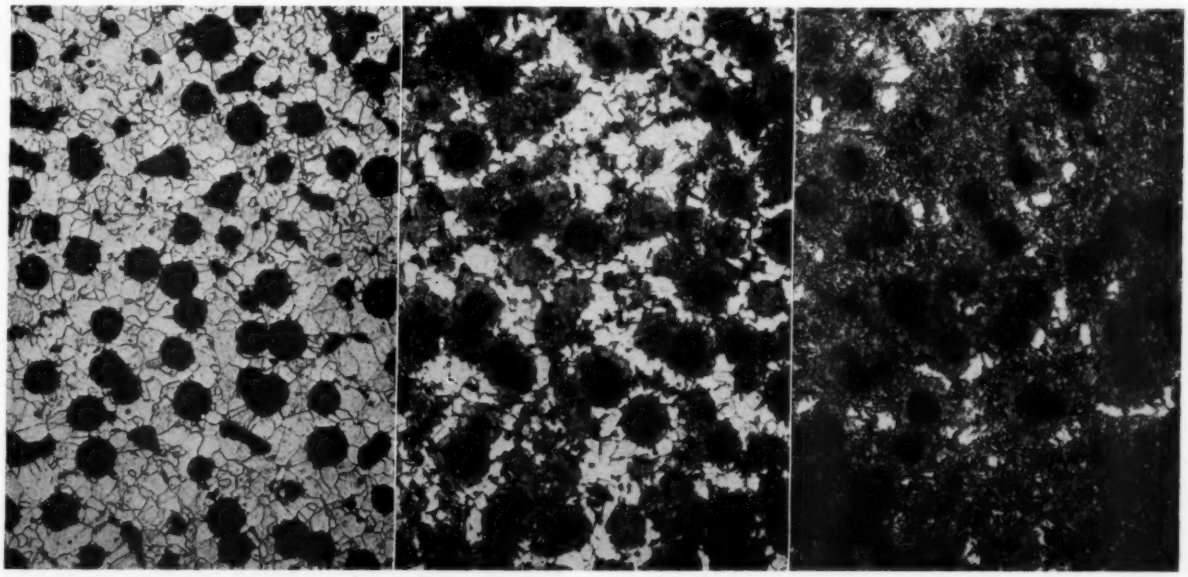
Isothermal Transformation Temp., F.	Time at Temp.	Tensile Strength, psi	% Elongation in ¾ in.	% Reduction of Area	Charpy Unnotched, Impact Energy in ft-lb	R_A Hardness	Bhn Equivalent Hardness
1350	Fully Ferritized	52,600	20.6	21.1	70	40	117
1300*	1 hr	87,300	9.5	8.4	28	56	190
1200*	15 min	115,300	7.5	7.2	37	58	210
1000*	20 min	128,000	5.1	3.6	32	62.5	250
800*	30 min	134,000	4.0	3.6	26	61.0	235

* Represent products of isothermal transformation.

It will be noted from Table 2 that while the prior structures which respond almost instantaneously to induction heating do not exhibit the ductility of the fully ferritized structure, they nevertheless exhibit a substantial amount of ductility in comparison with ordinary gray cast iron.

Influence of Prior Structure — Various Degrees of Ferritization: The hardening characteristics of prior structures with varying degrees of ferritization are illustrated for Heat N in Fig. 6. The results shown are for prior structures formed by isothermal transformation at 1300 F (705 C) for 1, 2, 9 and 26 hr. These structures contain varying proportions of the carbide and ferrite phase (with graphite spheroids), the amount of ferrite increasing with isothermal holding time. The prior structures formed by holding at 1300 F (705 C) for 9 hr and 26 hr were similar to those shown in Figs. 3a and 3b, respectively. Since Heat N tended to ferritize rapidly, even the specimen held 1 hr at 1300 F (705 C) showed an appreciable amount of ferrite.

It is apparent from Fig. 6 that the fully ferritized (26 hr), and even the substantially ferritized structures do not respond readily to rapid heating at any austenitizing temperature. As ferritization progresses, response to hardening upon heating becomes poorer and poorer. Only the structure isothermally transformed at 1300 F (705 C) for 1 hr shows appreciable



A—R_C 19 B—R_C 34 C—R_C 42

Fig. 7—Mode of carbon solution as indicated by microscopic appearance of induction hardened ductile iron; Heat D. Ferritized at 1300 F, 20 hr. Heated to 1600 F by induction for (a) 0 sec (b) 10 sec (c) 30 sec and brine quenched. Picral etch. Mag. - 100x.

hardness after heating to 1800 F (980 C) and 1900 F (1035 C) in 4 to 5.5 sec, and brine quenching. The higher the austenitizing temperature, the better is the response to rapid heating.

Perhaps the outstanding observation from Fig. 6 is that the fully ferritized structure will develop a hardness of R_c50 only after a prolonged heating cycle at elevated austenitizing temperatures.

TABLE 3—MECHANICAL PROPERTIES OF PRIOR STRUCTURES FERRITIZED TO VARYING DEGREES

Heat N

Isothermal Transformation Temp., F.	Time at Temp., hr	Tensile Strength, psi	% Elongation in ¾ in.	% Reduction of Area	Charpy Unnotched, Impact Energy in ft-lb	R_A Hardness	Bhn Equivalent Hardness
1300	1	88,000	—	13.0	32	58.5	213
1300	2	85,300	—	15.6	80	54.0	176
1300	9	71,400	16.5	23.2	96	51	159
1300	26	71,000	22.2	24.2	102	50	153

The mechanical properties of the prior structures represented in Fig. 6 are given in Table 3. These properties were determined in the same manner as those for Heat K. All of the structures show a high per cent reduction of area, combined with good strength and impact resistance. Unfortunately, it appears that the high ductility exhibited by these structures cannot be utilized in very rapid heating cycles, but that a slightly ferritized structure with lower accompanying ductility is necessary for instantaneous response.

Mode of Austenite Formation

The progress of austenite formation is depicted in Fig. 7, which shows the microscopic appearance of specimens of ductile iron (Heat D) almost completely ferritized and heated by induction at 1600 F (870 C) for (a) 0 sec, (b) 10 sec, and (c) 30 sec, followed by quenching in brine. It appears that any combined carbon, as cementite, almost immediately is transformed to austenite upon reaching the lower critical temperature and that thereafter austenite formation occurs by diffusion of carbon from the spheroidal graphite into the matrix. The martensite areas in the photomicrographs show clearly the progressive diffusion of carbon outward from the graphite spheroids as time increases.

Investigation of the structures of Fig. 7 at high magnification (1000×) revealed that the martensite to ferrite transition zones were abrupt. The martensitic areas change suddenly to ferrite without the appearance of any intermediate transformation products. This is illustrated in Fig. 8. It is apparent from this photomicrograph that carbon diffusion has proceeded irregularly, being guided perhaps by grain boundaries and crystallographic planes. The evidence presented indicates that all of the austenite formed in the heating cycle subsequently transformed to martensite. Actually, it is important to note that at the austenitizing temperature employed both ferrite and austenite are present simultaneously. The effect of silicon on the carbon limitations of austenite is such that any austenite present must be relatively high carbon content (.3-.4 per cent).[20] It appears then that austenite formation proceeds by the progressive movement of an austenite-ferrite interface from the graphite spheroids into the ferrite. Since the carbon content of the austenite at this interface may be as much as .30 to .40 per cent and contains appreciable amounts of nickel and silicon, it is not surprising that the hardenability of the austenite formed is sufficient for it to transform to martensite upon quenching, thus accounting for the abrupt martensite to ferrite transition.

Retained Austenite: A review of Figs. 4 and 5 indicates that a decrease in maximum hardness may occur in ductile iron specimens which respond readily to rapid heating if the austenitizing temperature is high and the time at temperature extended. The magnitude of this decrease may be as much as 3 to 4 points on the R_c scale, as indicated in Fig. 9, for specimens isothermally transformed at 800 F (425 C) and 1000 F (540 C) and then austenitized at 1900 F (1035 C) for increasing periods of time prior to quenching.

The decrease in hardness appears to be definitely associated with the presence of retained austenite in the as-quenched structure. This is demonstrated by the effectiveness of a sub-zero cooling treatment in liquid nitrogen in restoring essentially maximum hardness (Fig. 9).

Austenite is readily retained in ductile iron because the spheroidal graphite offers an almost unlimited source of carbon for solution in the matrix. Therefore, at high austenitizing temperatures, the carbon content of the austenite will progressively increase until the solubility limit is reached. High carbon concentration areas will then result in the retention of austenite upon quenching.

Influence of Composition—Silicon Content: Figure 10 shows the effect of silicon content upon the hardening characteristics of induction-heated ductile iron. All specimens were fully ferritized prior to hardening. While the influence of silicon is not large, it is apparent that Heat K (1.87 per cent Si) responds



Fig. 8—Microscopic appearance of specimen in Fig. 7b, showing abrupt martensite-ferrite transition zones. Picral etch. Mag. - 1000x.

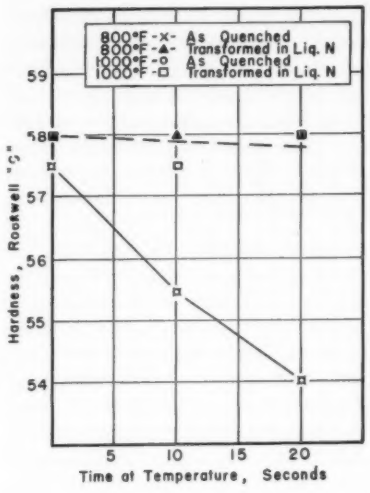


Fig. 9—Influence of retained austenite on maximum hardness; Heat K. Isothermally transformed at 800 F and 1000 F. Heated by induction to 1900 F and brine quenched. Subsequently cooled in liquid nitrogen.

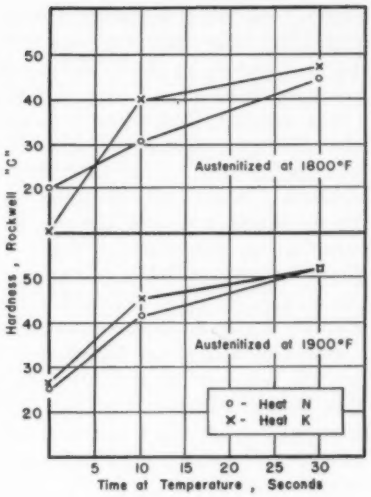


Fig. 10—Effect of silicon on hardening response of rapidly heated ductile iron; Heat K (1.87 pct Si) and Heat N (2.98 pct Si). Both heats fully ferritized prior to induction heating at 1800 F and 1900 F and brine quenched.

more rapidly than Heat N (2.98 per cent Si), particularly when austenitized at 1800 F (980 C). The lower hardness for Heat K after zero time at 1800 F (980 C) is due to the lower original hardness of Heat K as ferritized.

It appears then, that increased silicon content retards somewhat the rate at which carbon diffuses to provide a homogeneous and relatively high carbon austenite during rapid heating.

Comparison of Response—Ductile and Malleable Iron: Although the fully ferritized structures of ductile iron do not respond to rapid heating instantaneously (Fig. 6), their response is unusually rapid when compared to that reported by Kerr[17] for malleable iron. It appeared interesting, therefore, to actually compare the hardening characteristics of a lot of induction-heated ferritic malleable cast iron and completely ferritized ductile iron.

While the general structure of ferritic malleable iron and fully ferritized ductile iron is similar, the structure and configuration of the graphite present in each is actually quite different. The graphite in malleable iron exists as random relatively large masses with irregular and often sharp contours, whereas in ductile iron the graphite is in masses radially oriented around a common center, and exists as smooth spherical particles. The graphite in malleable iron has generally[21] been referred to as nodular graphite, while the term spheroidal graphite has been applied to the graphite in ductile iron.

Unfortunately, neither the number of nodules per unit area, nor the composition of the malleable iron received, compared favorably with the composition and spheroidal graphite count, i.e. number of graphite spheroids per unit area, of the ductile iron. Table 1 shows that the carbon, silicon and nickel contents of the malleable iron are appreciably lower than those in the ductile iron heat used for comparison (Heat D). The spheroidal graphite count of the ductile iron was found to be ten times the nodule count of the malleable iron.

Figure 11 compares the results of induction hardening this lot of malleable iron and ductile iron by heating to 1600 F (870 C) for increasing periods of time and quenching in brine. It is obvious that the ductile iron responds much more rapidly to heating

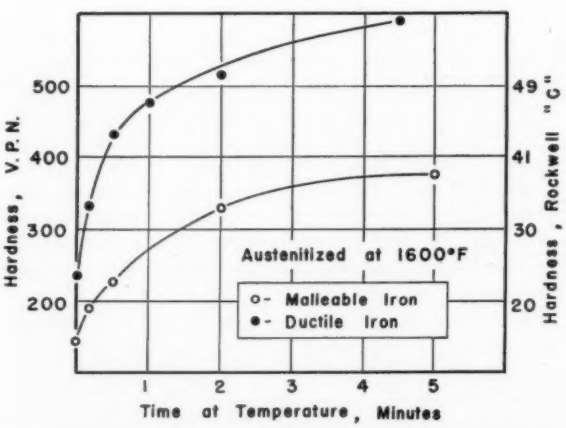


Fig. 11—Comparison of hardening response for induction-heated fully malleablilized iron and completely ferritized ductile iron. Austenitized at 1600 F and brine quenched.

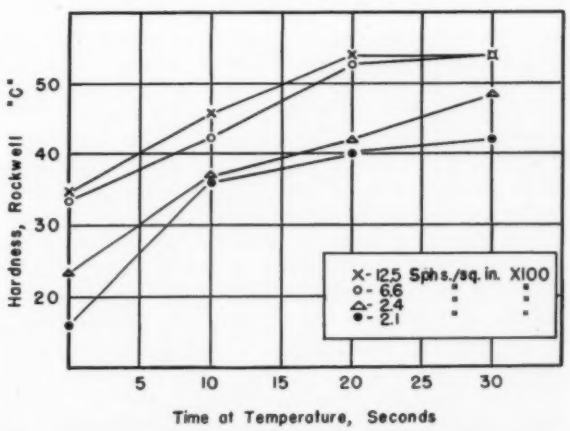


Fig. 12—Effect of spheroid count on hardening response of fully ferritized ductile iron. Austenitized at 1800 F by induction heating and brine quenched; Heat P.

since a hardness equivalent to R_c50 is obtained upon quenching after only 1-2 min at temperature, while the malleable iron does not exceed a hardness equivalent to R_c40 after 5 min of holding time.

The comparatively slow response of the malleable iron to rapid heating is associated primarily with the low nodule count determined for this lot of material. While variations in the composition of the malleable iron and the ductile iron as well as a variation in the external shape and nature of the graphite particles in the two materials may have some effect upon the rates of carbon solution, no inferences may be drawn from these preliminary data.

Effect of Spheroidal Graphite Count: Since an increased number of graphite spheroids unquestionably increases the hardening response of ductile iron to rapid heating by decreasing the distance through which carbon must diffuse, it appeared desirable to determine the exact influence of spheroidal graphite count. To isolate the effect of spheroid count, four samples from castings of different section size were heat treated to a completely ferritized condition. The composition of these samples is given in Table 1 as Heat P, while the spheroid count obtained with the aid of a grain size eyepiece at 100 magnifications is given in the legend of Fig. 12. The larger spheroid count is typical of castings with a small cross-section.

The influence of spheroidal graphite count upon the hardening response of specimens rapidly heated to 1800 F (980 C) is shown in Fig. 12. It is evident that increased spheroid count causes a significant improvement. Unfortunately, section size appears to be the only known factor which governs the spheroid count at the present time. Any development which could increase the number of graphite spheroids in a given section size would aid the response to rapid heating materially.

It should be noted that both Heats K and N, used extensively in this study, had a spheroidal graphite count of the order of 4 spheroids per square inch at 100 magnifications.

General Discussion—Application to Induction and Flame Surface Hardening

It is believed that the results presented are useful and necessary in predicting the possible success of various induction and flame heating cycles in producing a hardened case on ductile iron castings, while maintaining reasonable ductility and toughness in the core. Since these processes involve very rapid heating and depend upon rapid solution of combined and graphitic carbon for successful hardening, it appears certain that minimum case depths will be achieved with high austenitizing temperatures, partially ferritized prior structures and castings where the spheroidal graphite count is high. While suitable prior structures for minimum case depths may be obtained by reference to the isothermal transformation diagrams, they will not provide optimum core properties in the casting. It nevertheless appears that small case depths may be associated with core structures having a tensile strength of 88,000-100,000 psi, an elongation of 8-10 per cent, a reduction of area of 8-10 per cent and an unnotched Charpy impact resistance of 28 to 30 ft-lb.

Where heavier case depths are necessary or allowable, lower austenitizing temperatures and more completely ferritized prior structures are suitable. Core properties with such heavy cases should show substantially better ductility and toughness, though lower strength. However, the longer austenitizing times associated with heavy cases may promote the presence of retained austenite in the case, with consequent loss of hardness.

The ultimate properties of the surface hardened case and the transition zone have not yet been determined except that a maximum case hardness of R_c58 appears well established; likewise, the mechanical properties of case hardened sections with given core properties have not been determined. It is recognized that the presence of a case in service may have a pronounced influence upon the core properties reported. While a study of the composition for best balance between response to hardening and core properties has not been completed, silicon at least has been shown to influence this balance.

Summary and Conclusions

1. Isothermal transformation diagrams for two heats of ductile (spheroidal or nodular graphite) iron have been developed to show the progress of austenite decomposition and ferritization, and to aid in the selection of various prior structures. Isothermal transformation of austenite proceeds rapidly in a manner similar to that for a high silicon steel, resulting in lamellar structures above 1000 F (540 C) and acicular structures from 800-1000 F (425-540 C). Ferritization proceeds much more slowly and appears impractical below 1200 F (650 C).

2. The response of ductile iron to rapid heating, e.g. (the rate at which austenite with sufficient carbon content to harden uniformly upon quenching is formed) improves as the amount of pearlite in the prior structure increases, as the austenitizing temperature and time are increased and as the spheroidal graphite count is increased. Ductile iron responded much more rapidly than the lot ferritic malleable iron studied. If the austenitizing temperature or time are excessive retained austenite results with a corresponding decrease in maximum hardness.

3. Isothermally transformed prior structures which give almost instantaneous response to rapid heating have intermediate mechanical properties, i.e., tensile strength 88,000-100,000 psi, elongation 8-10 per cent reduction, reduction of area 8-10 per cent, unnotched Charpy impact resistance 28-30 ft-lb. Partially ferritized prior structures respond rapidly, though not instantaneously, and provide better ductility and toughness. Completely ferritized prior structures do not appear useful for case hardening processes unless very deep cases are required.

4. Silicon was found to modify the isothermal transformation diagrams by increasing the lower critical temperature, and tends to reduce the time required for ferritization. Silicon also appears to retard austenitization during rapid heating to some extent.

5. In general, the results indicate that surface hardening ductile iron by induction or flame heat-

ing should provide iron castings with high surface hardness, i.e., R_c58, and unusual core properties, though the influence of a hardened case on core properties has not yet been determined. Minimum case depths appear possible, provided suitable prior structures are used, and the austenitizing temperature is sufficiently high for rapid carbon diffusion.

Acknowledgment

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DISCUSSION

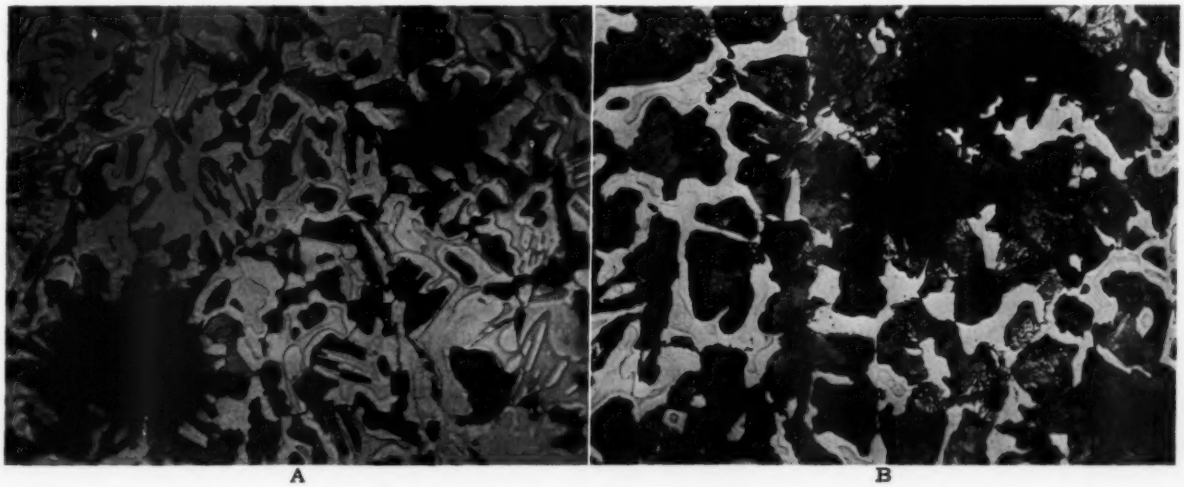
Chairman: D. E. Krause, Gray Iron Research Institute, Columbus, Ohio.

Co-Chairman: F. T. McGuire, Deere & Co., Moline, Ill.

Recorder: D. S. Eppelsheimer, Missouri School of Mines, Rolla, Mo.

Richard Schneidewind: [1] The results discussed by the authors seem to confirm some of the work we have done on malleable iron. Samples of fully-annealed malleable iron containing 2.3 carbon and 1.3 silicon were reheated at 1500 F for varying lengths of time after which they were removed and air quenched. According to the 2 per cent silicon section of the iron-carbon diagram presented by Greiner, Marsh, and Stoughton, the higher temperature of the three-phase area is around 1450 F. For a 1.3 per cent silicon iron it should be still lower. The temperature 1500 is well above the three-phase region as described in the literature. The samples were examined metallographically as the time of heating varied from 15 min to 100 hr. The amount of pearlite increased rapidly at first but slowed down after about 1 hr. Recarburization started along the grain boundaries. One photomicrograph (A) is of a specimen heated at 1500 for 65 min. It will be noted that the ferrite seems ringed with a reaction zone. Spectrographic tests showed that the rim and the center of the crystal both were alpha ferrite, but with somewhat different orientation. The other photomicrograph (B) is of a sample held at 1500 F for 16 hr. After 100 hr of heating, small amounts of ferrite were still visible. This slow recarburization seems to be similar to the observations made by the authors.

[1] Professor of Metallurgical Engineering, University of Michigan, Ann Arbor, Mich.



A B

A GENERAL LOOK AT NON-FERROUS MOLDING SANDS

By

C. A. Robeck* and C. A. Sanders**

Copper-Base and Aluminum Alloy Sands

Introduction—Naturally-bonded fine grained sands have been the popular choice in the non-ferrous foundry, as well as in certain light-weight gray iron foundries that cast enamel ware, hardware, architectural tracery, ornamental fittings, memorial tablets, name plates, and thin gray iron plate. Only recently has there been a trend toward the use of synthetic or semi-synthetic sands in the non-ferrous foundry.

Not all localities have suitable non-ferrous molding sands, so the more suitable ones are imported from other outside deposits. This additional freight cost makes them prohibitive in some areas, so substitute sands must be selected, or manufactured.

Teachings—Fundamentally, non-ferrous foundries have been taught the use of finer, more silty-in-feel sand that carries its own bond. This practice has become more hereditary than practical. Non-ferrous foundries as a rule are not fully mechanized, they have limited storage space and realize that unless a certain amount of mechanization is available, the foundry cannot obtain the full economy of synthetic sands. However, the progressive foundryman has learned the value of sand control and has found it worthwhile in money savings by incorporating sound, basic principles in the sand system.

Mr. Foundryman—To arrive at what constitutes a good non-ferrous molding sand is left to the opinion of Mr. Foundryman. He should first study the character of the castings that are to be produced and to select that base sand which furnishes the least expense in manufacturing satisfactory castings. Non-ferrous foundries as a group, cast over 2,000-3,000 alloys and different alloys require different base sands for the various groups in order to obtain best results.

For instance, with light work such as non-ferrous carriage parts, unique designed automotive parts, lockwork, bushings, or pins, require a fine sand, lightly bonded and yet open enough to allow detrimental gases to escape. Suitable sands for this class of work are perhaps more plentiful than for any other class of non-ferrous castings, but to apply this sand to a number of other non-ferrous alloys results in a quandary. However, it is good practice to have *one base sand*, so by blending other sands to the base sand, multiple properties are developed. This formulates the authors' advocation of proper *wide grain distribution* to satisfy foundry and metal variables.

Finish—Albany sands, Tennessee sands, Mississippi sands or several other similar sands are used widely in non-ferrous foundries, and in making light-weight gray iron castings. Their performance is above reproach in producing castings of "sales appeal." However, these fine-grained sands are considered weak sands and require substantial additions to the system to obtain standard physical properties requisite of a good mold. This is expensive, and progressive foundrymen have sought more economical methods of maintaining these physical properties. Blending synthetic mixtures into the system has been highly approved. However, naturally-bonded sands from Ohio to Kansas fit the purpose of many non-ferrous foundries with just claims for using them. Sands for brass or bronze demand higher dry strength, i. e., from 40-70 psi which is easily found in many of the naturally-bonded sands, yet they furnish good casting finish.

Castings to be polished or plated need no smooth finish, however many alloy foundries seeking finish may destroy the finish in the sand blast, shot blast or cleaning room. To seek finish requires low permeable sands with good flowability which at times is dangerous. Higher rammed permeabilities are better safety factors (within reason) and rough abrasion is not as closely noted.

Synthetic Sands (1941-1945)

During the war many bronze and aluminum foundries mechanized and changed to synthetic sands. Aluminum foundries asked for sands with permeabilities of over 100 and successfully used them.

The adoption of synthetic sand in lieu of naturally-bonded sands for non-ferrous use came about for many reliable reasons:

(1) Economy and success of using synthetic sands

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in the ferrous foundries led non-ferrous foundries to investigate the possibilities of using these sands.

(2) Naturally-bonded sands are usually non-uniform, as the clay content, moisture, sand grain distribution vary widely and control is difficult. The grain and shape of the average naturally-bonded sand is more complex and as a rule far less uniform from shipment to shipment.

(3) Chemical composition varies, causing fusion and encourages other common casting defects.

(4) Prohibitive transportation costs and difficulty in obtaining certain grades in the winter months, as well as constant fear of freezing of natural sands led investigators to seek closer and more available sand supply.

(5) Fine grained naturally-bonded sands usually have low durability and new sand additions are expensive.

(6) Weak sands are usually accompanied by high water carrying capacity resulting in excess steam gas porosity and balling. This is principally due to high silt and inert fines in the clay content.

(7) Synthetic sands offer controlled permeabilities, yet when properly distributed (sand grain) casting finish is equally as good. NOTE: Naturally-bonded sands with permeabilities of 10-15 may cause trouble, whereas synthetic sands with the same permeability are safer to use. Low permeabilities are not detrimental if gasses are not intentionally placed in the mold by additions of gas producing materials, or an air-suction gating system.

Advantages of Using Natural Sands

The above data points out the disadvantages of using naturally-bonded sand in the non-ferrous foundry, yet Mr. Osborn[12] explains the following good points of natural sands and the authors concur on many.

"Surveying molding sands for the non-ferrous industries, naturally-bonded sands continue to be the popular choice for both large and small foundries. Postwar casting production focuses greater attention upon casting finish and the economies necessary in obtaining finish under competitive conditions. Molding and core sands assume a greater responsibility to this end, with many foundries seeking to reduce the use of mold coatings, special facings and other procedures that may have been more profitable under wartime systems of operation. For operating efficiency and simplification, it is advisable to have the whole heap or the entire system equivalent to facing sands; and the fine texture and favorable workability of many natural sands makes this conveniently possible for most foundries."

Economy—Since all foundries have a certain amount of burnt core sand returning to the system, it is necessary to add a bonding material periodically to maintain satisfactory working strength. A sizeable amount of heap sand is lost each heat at the shake-out due to baked, lumpy sand, fusion of the bond, etc. This sand loss must be replaced with new sand additions or with clay additions to replenish the strength. The latter addition is more economical.

Actual non-ferrous operations have proven that many advantages appear by incorporating a small amount of mineral bond with the system sand. The addition of bentonite, or fireclay, (the mineral bond) to the heap sand, rebonds the entering core sand much more effectively and far more economically than the required, larger amounts of naturally-bonded sand. For operating efficiency and simplicity, it is advisable to have the entire system equivalent to the facing sand.

Effect of Heat—It is quite important for the foundryman to know the amount of clay content in the naturally-bonded sand. The percentage of clay that enters the system affects the behavior of other properties to a great extent. Permeability and green strength are but two of the factors thus affected. Different grades of sand represent wide ranges of clay content, i. e., from 8 pct to 35 pct. Some clays in natural sands are weaker than others. Some are affected by heat greater than others at 900 F, and authoritative sources have found many to lose from 20 pct to 60 pct of their original green bonding strength when heated to 900 F for a short time and then re-tempered.

Most of the bonding property is lost permanently when heated to above 1400 F, but fortunately only a thin skin of the mold (except on larger castings) reaches those completely dehydrating temperatures. But in that area of sand which is heated from 400 F-900 F, naturally-bonded sand and some fireclays are partially deteriorated. Many of the more colloidal clays, such as Western bentonite, rehydrate more readily.

Semi-Synthetic Sands—A few non-ferrous foundries are using semi-synthetic sands, combinations of part natural with "sharp" or "core sands" (unbonded). It has been found that bonding agents such as bentonite or fireclay blend with the individual sand grain readily because of their extremely fine grain size and their ability to "spread" in the system. However, mulling is advised. This is a controversial subject with many claims from both sides.

Molding sands that are improperly rebonded produce irregular volume changes. Numerous defects easily develop and scrap runs high. New bond additions should be added regularly in order to furnish enough plastic and void space to compensate for sand expansion.

Semi-synthetic sands were developed because of many complaints of buckles and rat-tails on flat casting surfaces. This was not as true in non-ferrous as it was in gray iron, but the trend spread rapidly. As mechanization increases, so does the trend toward semi-synthetic or synthetic sands. It appears that most non-ferrous foundries are reluctant to make additions of new bonded sand, thus these expansion defects develop more easily.

In return, naturally-bonded sand that is added to the synthetic mixture tends to furnish a smooth, velvety feel to the blend and helps to give an excellent finish to the casting. The vent wires, sponges, swabs, and other patch tools common to the non-ferrous jobbing foundry are dispensed with, as proper balance is restored to the system when a wider sand grain distribution is obtained by the blending.

Balling Up—On occasions it has been found that when adding heavy additions of new naturally-bonded sand with much fines that the system tends to "ball-up." The same is true if excess clay is added, or too much "rolling" of the sand in handling. However, only by adding a minute addition of mineral bond in

minor amounts to replace the naturally-bonded clay, which has been lost, can this occurrence be remedied. Moisture should be low and the mulling period studied carefully, as mulling can contribute.

How to Reduce Clay-balls

The question is usually asked, "How can clay-balls be best reduced?" The answer is not simple, as most foundries have experienced this same difficulty. **First and foremost the sand must be shipped on the dry side.** If naturally-bonded fine sand is dug, loaded and shipped while wet or damp, clay balls are bound to occur. Try to procure the sand in dry seasons, avoid wet climatic seasons and less balling-up will occur.

If however, this difficulty is experienced, try the following suggestions:

(1) Work the sand as dry as conditions permit;

(2) Keep clay content as low as possible;

(3) Avoid excess inert fines that easily build-up in the system;

(4) Constant control of ash in the sand if material such as seacoal is employed;

(5) Reduce riddling to a minimum;

(6) Avoid too much handling of the sand, except proper mulling time;

(7) Avoid large additions of new sand, try to add minimum amount of bond with proper mulling and add the least amount of new sand to maintain the system;

(8) Avoid long mulling and unnecessary movement of fine sand;

(9) All defects which result from clay-balls in the system can be entirely eliminated if a facing can be used.

Weak sand tends to use too much surplus moisture and usually stick to the pattern if the pattern is too hot or too cold, not matching the temperature of the molding sand. Proper blending and mulling of the naturally-bonded sand with the synthetic bonded sand eliminates this condition.

Tendency to sticking in the hoppers and bins is less with the semi-synthetic combination than with either synthetic or straight naturally-bonded sands.

Flowability is increased as synthetic bonded sands are added to naturally-bonded sands and the blend can be rammed harder. This is true as synthetic bonded sands have greater venting power and when the blend is rammed harder, the castings possess better finish. The molder is further assured that harmful gasses can escape by additional venting power.

The blend overcomes the tendency to dry out and the sand is workable for longer intervals. The naturally-bonded sand gives the blend a wider range of moisture and less fear arises of hot sands developing in a small system.

Regardless of all that has been offered, the authors realize that the non-ferrous industry has lagged in modern techniques. This is principally due to the man in the foundry who "knows all" and has lived with his little "black book." He has been slow in giving out information and has been reluctant in absorbing modern information. He still continues to dig his fist into the sand and come out with an answer. Even with the new and elaborate moisture telling devices, he continues to dig into the sand to verify the findings of the machine. Good information is available and much of it is practiced, and data usually can be obtained by request.

Brinson[13] used a synthetic sand mixture as facing from ½ in. to 1½ in. thick. The facing is subsequently reclaimed and reused for filler, as backing to the facing. He indicates the average life cycle as 30 molds, i. e., 30 cycles passing through the reclaiming system. When the Navy Yard first adopted the synthetic sand, it was thought that they could use a "one purpose sand" but this was found impractical due to varying conditions with different classes of castings.

Feasibility of "One Purpose Sand"

Perhaps it would have been feasible to use the "one purpose sand" in the days passed, but in order to obtain the maximum quality in the production of present castings, it was found advisable to use two regular and five special mixtures. Each mixture was specifically tailored to fit the chemistry and physical characteristics of the metals in the foundry.

Brinson indicates that the reason they diverted from a "one purpose sand" was to overcome the handicap of the synthetic sand drying out too rapidly. Additions of dextrine, kerosene, and ethylene glycol were used but Brinson states that the wet burlap over the sand is far superior to the special mixture. As stated previously, a semi-synthetic sand helps overcome this condition.

High Lead Bronzes and Nickel Alloys—These alloys require sand of low permeability and good flow. Sands or sand mixtures associated with higher clay content and fines are used, as this type sand or mixture has longer water retention and helps chill the difficult setting metal thus avoiding metal penetration. Sands or sand mixtures of high density should be used. Mold pressures and mold atmospheres must be studied in the casting of these difficult flowing and seeking alloys. Nickel-silver must be treated differently, as greater success has been obtained with synthetic sand mixtures.

Popular Non-Ferrous Sands—To follow nature's process in the manufacture of her many varieties of naturally-bonded sands, one must trace the geology of each through their various periods, usually beginning with post-glacial period.

Most all these sands underwent an aging process after being deposited by the huge glaciers. Nature's wind and water worked these sands to their final resting places and then began a combination of weathering and chemical reactions in converting the various minerals present to their present forms.

Albany Molding Sand—Perhaps the most widely known non-ferrous sand is the Albany Sand, however the N. J., Tenn. and Mississippi Sands have taken a toll of its tonnage. Albany is different from other sands in that it has a wide sand grain distribution accompanied by a clay content that has the unusual tendency to cling to the sand grain. The clay coating is difficult to wash off, as washing requires twice as long as with other sands, even when a strong alkali is present. This phenomenon is said to be partly responsible for the unusual working of Albany. It also pos-

sesses more organic material than most sands which furnishes good resilience.

Geology of Albany Sand

Nature began the production of Albany sand[14] ages ago. When the great Continental Ice Sheet covered all of Eastern New York State as far south as Long Island, the process was well under way. As the great mass of ice swept down from the North, it picked up quartz rock from the Adirondacks, ground it into sand, mixed it with shale from the Hudson Valley; and then deposited the basic materials for Albany Molding Sand. Another important process was required; and that took more than 20,000 years. It was the process of coating each individual grain with a clay-like substance that gives Albany sand its unusual bonding properties.

The deposits occur on the terraces that border both shores of the Hudson and lower Mohawk rivers. Only a small proportion of them yield molding sand of choice quality.

The area extends from Dutchess County in the south to the foothills of the Adirondacks. Below Albany, the deposits are confined to a comparatively narrow belt. North of the city, the district broadens out. As a rule, the molding sand deposits are sharply defined. They are comparatively small, most of them containing five acres or less. Large deposits of fifty acres or more are unusual.

The typical deposit is composed of 18 or 20 in. of molding sand covered by 8 to 12 in. of top soil. Occasionally molding sand is found, covered by a wind-blown sand dune. In such deposits, the molding sand lies below a covering of several feet of sharp sand and the original layer of top soil.

Although some Albany sands "clay substance" have a low fusion point, many of these natural sands produce good finish on fine bench work such as ornamental castings and stove plate. Both the sand and "clay substance" which compose the Albany sand grain seem to be completely fused at approximately 2625 F, but of course it depends upon the amount of clay and grain present.

As stated by Anselman,[15] "The Albany Sand gained its reputation in early days in the stove plate foundries where the iron was poured at approximately 2550 F to 2600 F and was fluid at this temperature due to the high phosphorus content in the metal." However, today in the modern stove shop where we are pouring at temperatures around 2800 F, this sand is not entirely satisfactory, first because there is a fusion of the sand at this temperature and secondly, because it has too much pan material to withstand the hard ramming under mechanical methods such as jolt squeeze or slinger work.

Albany Withstands Expansion Troubles

Today a more open sand is used with a high silica base grain to withstand the temperature and a clay that is soft, not too durable, but one that will give the desired properties for a hard rammed sand. This softening, or partial fusion, at a low temperature in the Albany sand is what gives it its ability to withstand expansion troubles, which reduces rat-tails, buckles or scabs.

America has been blessed with many deposits of moldings, bonded and unbonded.

Non-ferrous foundries are well acquainted with the famous Albany Deposits, but bonded sands of equal quality are found in New Jersey, Ohio, Tennessee, Mississippi, California and other states. So much data are available on these sands that it would be unfair to attempt to evaluate them in this article.

French Molding Sand

French Molding Sand—Many years ago foreign sands entered our ports as ballast on ships and were inexpensive to our New England foundry ancestors. Later, our own deposits were discovered and developed and most of these foreign sands were forgotten and dispensed with. However, French molding sand is still sold in the U. S. A. and used faithfully by certain craftsmen.

Imported French molding sand is recommended for the manufacture of statuary work, bronze tablets, plaques, jewelry and ornamental castings, especially where fine detail and texture are required. It is best suited for fine ornamental bronze and brass art work, and most silver was cast in it. Some special mixtures were prepared by grinding and blending the French sand with small amounts of glycerine. French molding sand is fine grained and contains approximately 17-19 pct clay. Its permeability is satisfactory to produce such castings. Molds may be rammed quite dense with this sand and it can also be oven dried for dry sand molding where necessary. The majority of foundries using French molding sand skin-dry the mold before casting. French molding sand is referred to as a fine sand, as it is similar to an Albany O or OO grade, but may be nearer a No. 2 Albany in the A.F.S. Classification. It possesses exceptional flowability and the grains knit so well that green strength is improved.

Some jewelry manufacturers may question the above data, as they purchase a much finer French molding sand. This is quite true, as the authors know one prominent importer that grinds the crude French sand in special mills which then shows a 35-50 pct A.F.S. clay content. This is not true clay, but analyzes as such. This ground sand gives excellent finish, especially when treated with a glycerine. The following is a screen analysis of French molding sand:

Retained on No. 100—	4.8
Retained on No. 140—	7.0
Retained on No. 200—	22.8
Retained on No. 270—	31.8
A.F.A. Clay	12.8
A.F.A. Fineness Number—	170.6
Permeability with moisture @ 5.8%	13.8
Green Shear	— 3.1 psi as received
Green Compression	—10.5 psi as received

Windsor Lock Molding Sand—A prominent non-ferrous sand that is used in the manufacture of fine texture or detailed castings is the Windsor Lock sand. This sand is mined at Windsor Locks, Hartford County, Connecticut. It contains an average of 9-10 pct clay (bond). This sand is very fine grain and contains a good percentage of loam. It is recommended for the

casting of small and medium bronze (fine art work) and aluminum castings. It works well with French sand and is even used in the casting of gold and silver.

A typical analysis of Windsor Lock sand is as follows:

6 Mesh	—	None
12 "	—	.08%
20 "	—	.08%
30 "	—	.06%
40 "	—	.10%
50 "	—	.10%
70 "	—	.16%
100 "	—	.84%
140 "	—	5.62%
200 "	—	18.08%
270 "	—	11.28%
Pan	—	53.88%
A.F.S. Clay	—	9.72%

A.F.S. Grain Fineness No. 239

Physical Properties

Green Compressive Strength, psi	—	3.5
Green Shear Strength, psi	—	1.0
Permeability	—	9.0
Moisture, %	—	5

Some of the raw materials used in the non-ferrous foundry are discussed below.

Use of Cement—Cement molding is used in non-ferrous foundries particularly in intricately designed pressure bronzes which may leak or sweat. Cement is used because of its low gas forming rate and the moisture can be held below 1 pct. Cement molds also chill the metal faster, the same as in gray iron or steel practice and thereby impart deeper and denser skin to the castings. The only difficulty with this class of molding seems to be the same for gray iron, it requires greater storage space and reclamation of the molding sand is more difficult. However, cement is sometimes selected as a binder because the cores can be stored for a longer length of time. (Refer to appended formulas.)

Cement is commonly used as a shake-on facing to impart higher dry strength on the surface of the mold.

Use of Pitch—Large non-ferrous castings such as, propellers, bells, return heads, etc., are cast in more open sand and some pitch is used to relieve certain strains when the heat radiates through the mold. Many of these larger castings require a mold wash and are dried in ovens preliminary to pouring. Approximately 2 pct pitch is used as an average. Too much gas can be ruinous to the casting if the molding sand is not open, or the mold not properly vented.

Charcoal—For extra fine brasswork suitable for ornamental purposes, fine ground charcoal is dusted on the mold surface with cement or another binder to furnish better finish. This unnecessary labor and material cost can be avoided if more care is given the correct density and grain distribution of the sand grains.

Silicon Carbide—High heat transfer mixes are sometimes used such as silicon carbide, where chilling is desired in sharp corners, fillets, around bosses, and similar areas. Brinson points out that some silicon carbide mixtures possess three-fourths of chilling power as solid graphite and three times that of green molding sand. Greater chilling is further accomplished with sand of higher density and shrinkage is usually decreased if properly gated.

Wood Flour or Cellulose Materials—Non-ferrous foundries have found wood flour or cellulose materials to be advantageous in overcoming volume changes in sand molds, thus eliminating buckles, rat-tails, and scabbing action. By incorporating approximately 1-2 pct wood flour in naturally-bonded sands or synthetic sands, the control to overcome rapid volume changes is gained. Too uniform sand promotes these defects as described in previous articles and a wider grain distribution with the use of a reducing agent seems to improve it.

Bentonite and Fireclay—Non-ferrous foundries using a portion of bank sand in the system may use as little as 3 pct bentonite (based on the total weight of the bank sand in the system) and obtain the same strength as the naturally-bonded base sand. A strong tendency toward the use of a slurry is noted. Several non-ferrous foundries are stirring a handful of fireclay or bentonite into each sprinkling can full of water and pouring the contents on the heap, mold, or the shake-out sand. The orifice openings in the sprinkling can nozzle are enlarged to allow easier flow of the slurry. This tempers and re-bonds at one operation. It is highly advantageous and should be investigated.

In conclusion, it is not the authors' intention to regale the reader with advice about non-ferrous foundry sands, but to point out certain trends during this period.

Much additional data on proper handling and mulling could be added, but this shall be covered later.

This paper, in no way, wishes to advocate the use of any particular system or sand. Synthetic sand is advantageous when used in mechanized operation, whereas naturally-bonded sands are best to use where the least amount of care is given. The authors have attempted to show this clearly in a previous article entitled "Foundry Sands Evaluated — Naturally Bonded vs. Synthetic Sands," AMERICAN FOUNDRYMAN, September 1948.

The following formulas and sands are listed as examples. However, other good sands of similar characteristics can be substituted with equal properties, as the choice usually depends upon the locality, availability and delivery cost at the foundry.

Formulas

1. *Semi-Synthetic*—Very light work, part natural
   - 25 lb—Bank Sand, A.F.S. Grain Fineness No. 250 Low clay content, Less than 12 pct.
   - 75 lb—Silica Sand, A.F.S. Grain Fineness, No. 90-120—Less than 1 pct clay content.
   - 2 lb—Southern Bentonite
   - 5-6 pints—Water

NOTE: Finer grade sands are powerful in their effect on physical properties. They close the mixture rapidly, but detail is excellent. The above mixture may have no more than a permeability of 10. The green compressive strength approximately 6-8 psi. The dry compressive strength will be under 20 psi. If a more open sand is desired, replace the finer bank sand with coarser sands. *Location of the foundry determines the*

choice of the sands selected. Use the nearest and most available source.

2. *Semi-Synthetic*—additions of wood flour
   - 40 lb—Albany Sand, Tenn., N. J. or Miss.—Grade depends upon choice of finish
   - 60 lb—Silica Sands or special washed sands (Several suppliers), A.F.S. Grain Fineness No. 115-150, clay (1-7 pct)
   - 2 lb—Southern Bentonite
   - ½ lb—Wood flour (at least an 80 mesh grade)
   - 5 pints—Water

NOTE: One-quarter pound of cereal may be added if edges are brittle. Some foundries add minute amounts of kerosene to prevent friable edges, about 1 pint per 1000 lb of sand. Wood flour helps eliminate cracks, buckles, scabs, rat-tails, difficult shake-out, over ramming, and improves finish. This is also a good formula for pressure castings.

3. *Mixture with Cement*
   - 90 lb—Silica Sand, A.F.S. Grain Fineness No. 60-70
   - 10 lb—Cement
   - 6-7 pints—Water

NOTE: This mixture is used chiefly for Pressure Castings, but certain collapsible mixtures work equally well. In fact, some hard baked molds may crack or hot tear certain pressure castings. This mixture is not widely used but where molds or cores are desired to be stored for indefinite periods this mixture performs well. Molds or cores made from the above formula will stand rough handling. Many large molds or cores handled by crane seem to be mechanically stronger than other types of bonded molds or cores.

4. *Part Core Oil or Kerosene to Synthetic Sands*
   - 75 lb—Silica Sand—A.F.S. Grain Fineness No. 60-70
   - 25 lb—Silica Sand—Ex. A.F.S. Grain Fineness No. 92-95
   - 2 lb—Western Bentonite
   - 2 quarts—Core Oil (Linseed, alkali-refined)
   - 6-7 pct—Moisture, by weight
     - Permeability—20
     - Green Compressive Strength—6 psi
     - Green Hardness—75

NOTE: Such mixtures are used for larger castings to help eliminate nails and sprigging. Copes are held well with less dirt. Kerosene with about 20 pct resin added to it has been used. Molasses is used at times to hold surfaces and is often substituted for core oil in the above mixture.

5. *Dry Sand Mixture for Heavy Non-Ferrous Castings*
   - 75 lb—Naturally-bonded sand, A.F.S. Grain Fineness No. 90-120, clay content approximately 16 pct.
   - 25 lb—Silica Sand—A.F.S. Grain Fineness No. 60-70
   - 1 lb—Western Bentonite
   - 4 lb—Pitch
   - Temper with 7-8 pct molasses, glutrin or dextrin water.
   - Bake at 450 F overnight.

NOTE: When pitch is used, temper on *heavy side,* or edges will be soft and dry strength low. If mixture is too open, finer bonded sand should be substituted for the more open naturally-bonded sand. If the mixture is too tight, open with additions of silica sand.

6. Two good non-ferrous mixtures worth repeating from S. W. Brinson's "Synthetic Sand in a Non-Ferrous Foundry," *The Foundry,* Jan., 1947.
   (a) *Anti-Buckle,* percentage by weight
   - 67 pct—Silica Sand—A.F.S. Grain Fineness No. 150.
   - 33 pct—Silica Sand—A.F.S. Grain Fineness No. 60.
   - 4 pct—Western Bentonite
   - 0.63 pct—Cereal Binders
   - 1.8 pct—Wood Flour
   - 3.9-4.1 pct—Moisture, permeability 25, green compressive strength 4 psi

NOTE: This mixture was created to eliminate scabbing, buckling, or rat-tailing of aluminum chocks and covers. This mixture overcomes rapid expansion or other mold volume changes.

   (b) *Silicon Carbide Mix,* percentage by weight—Note: Zircon Sands may replace Silicon Carbide
   - 100 pct—Silicon Carbide, A.F.S. Grain Fineness No. 60
   - 4 pct—Western Bentonite
   - 0.5 pct—Cereal
   - 1.3 pct—Truline Binder
   - 4.5 pct—Moisture, by wt.

NOTE: Used where chilling is desired. This is a very good mixture to use where sharp edges, fillets, bosses or hot areas are desired and where shrinkage is apt to occur. Areas that crack may be covered with the above mixture to help eliminate the problem. It has been found in the past, and used to a sizeable degree during the war, that metal shot and grit could be used in molding sand for good chilling results.

7. Taken from "Foundry Sands and Mold Materials," Report No. 1, A.F.S. No. Calif. Chapter.

Heavy Brass and Bronze Castings

Mission Red Heap Sand	—	35 gal
Mission Red New Sand	—	10 gal
Del Monte Sand (Fine)	—	15 gal
Southern Bentonite	—	1 gal

Physical Properties

Green Compressive Strength	—4-6 psi
Permeability	—35-55
Moisture, %	—7-9

NOTE: The molds are usually sprayed or washed with blacking and then dried. Drying time is reduced with the silica sand present.

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13. S. W. Brinson, "Synthetic Sand in a Non-Ferrous Foundry," *The Foundry*, Jan. 1947.

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DISCUSSION

Chairman: B. H. BOOTH, Carpenter Brothers, Inc., Milwaukee.

Co-Chairman: H. W. DIETERT, Harry W. Dietert Co., Detroit.

Recorder: F. P. GOETTMAN, Standard Sand Co., Grand Haven, Mich.

S. A. KUNDRAT:[1] Was there any change in flowability of the sand when you substituted 1 per cent graphite for the 1 per cent oil?

MR. ROBECK: No change was evident to the molders.

MR. KUNDRAT: How do you determine the residual amount of fuel oil in the system sand so one can control new additions?

MR. ROBECK: We have no test; we used fuel oil for 3 to 4 weeks during which time we relied on the judgment of the muller operator on how much to add. We then eliminated fuel oil except on one floor for one job where it is still used periodically.

CHAIRMAN BOOTH: Did you make an experiment on the effect of grain distribution?

MR. ROBECK: In arriving at present synthetic sand we selected two base sands to give a 4½ to 5 screen sand.

[1] Pattern Shop and Foundry Foreman, Homestead Valve Mfg. Co., Coraopolis, Pa.

VELOCITIES AND VOLUME RATES OF METAL FLOW IN GATING SYSTEMS

By

W. H. Johnson,* H. F. Bishop* and W. S. Pellini†

ABSTRACT

The effects of sprue runner and finger-gate variations on the velocity and volume rates of metal flow from gating systems were determined quantitatively by a combination of photographic and electronic measurement techniques. The studies related to conditions of first stage pouring, i.e., the initial period of pouring prior to the development of back pressure, due to the rise of metal in the mold. The results of the investigation indicate that the effect of sprue height is dependent on whether the gating system is choked or free-flowing. In general, turns and long runner distances act to choke the system and thus tend to produce effects similar to geometric choking. For systems which remain free-flowing the quantity of metal delivered is determined almost entirely by sprue cup conditions; flow velocities are determined primarily by the conditions existing in runners and finger-gates. For choked systems volume rates are determined by the degree of choking and velocities are strongly influenced by sprue height.

Introduction

Previous qualitative studies[1,2,3] of metal flow from gating systems have indicated a general conformance to hydrodynamic laws. The application of these laws to foundry practice requires interpretation with respect to the principal stages of pouring, Fig. 1A and 1B. The initial entry (first-stage pouring) of metal into the mold approximates the conditions of a free system in that the flow is unaffected by the liquid metal present in the mold. As the system fills (second-stage pouring) a ferrostatic back pressure is developed and the action approximates that of a pressurized system. The duration of first stage pouring depends on the vertical position of the ingate with respect to the mold cavity. The conditions of first-stage pouring place a predominant importance on momentum and related momentum-developed pressure effects, Fig. 1C.

The characteristics of the stream of molten metal issuing from a gating system during first stage pouring are determined by the overall design of the gating channels which may consist of several straight, vertical and horizontal members and angle junctions. Each channel offers some degree of frictional resistance to flow and affects the momentum-pressure conditions within the system which, in turn, affects the velocity and quantity of metal flow.

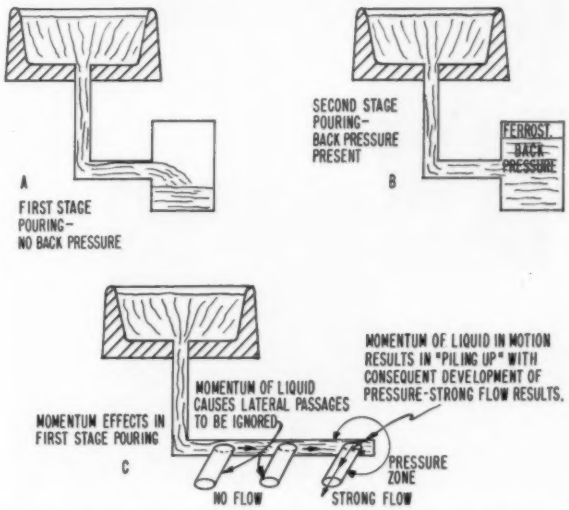


Fig. 1—Illustrating concepts of first and second stage pouring.

The present report entails quantitative studies of the effects of the various gating systems on the flow characteristics during first stage pouring. The flow characteristics, as determined by the interactions of the component parts of a gating system comprised of a pouring cup, sprue, runner, and finger-gates were determined, in effect, by dissecting the system for study. Flow characteristics thru simple sprues of various lengths were studied first. Then a short runner was added to the sprue to introduce a change in flow from the vertical to the horizontal direction with the consequent introduction of momentum-pressure and frictional effects. Lengthening the runner introduced further changes in the flow stream; and the addition of finger-gates at right angles to the runner produced final modifications within the system.

Experimental Procedures

Molten steel at 2900 F was poured into gating systems of various designs made of cores and allowed

* Metallurgist and † Head, Metal Processing Branch, Metallurgy Division, Naval Research Laboratory, Washington, D. C.

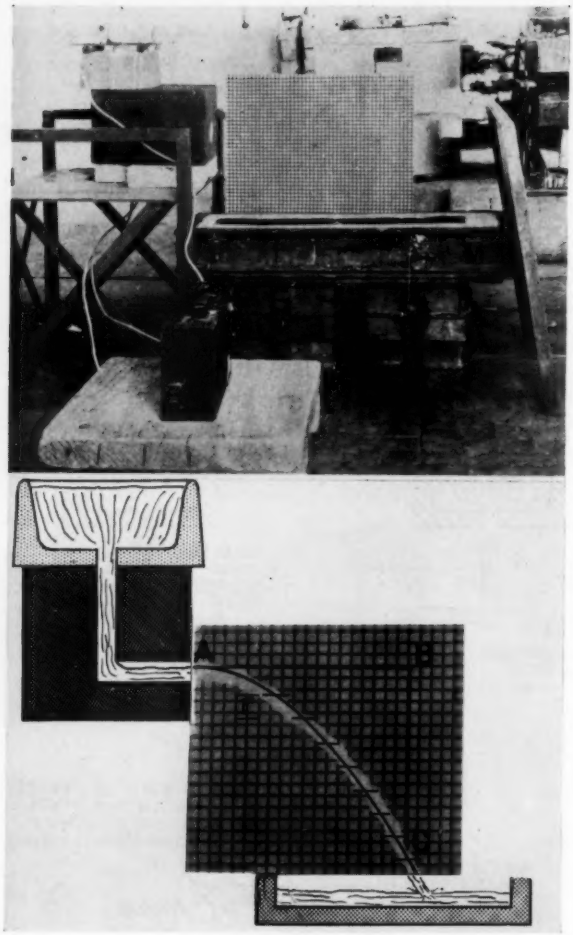
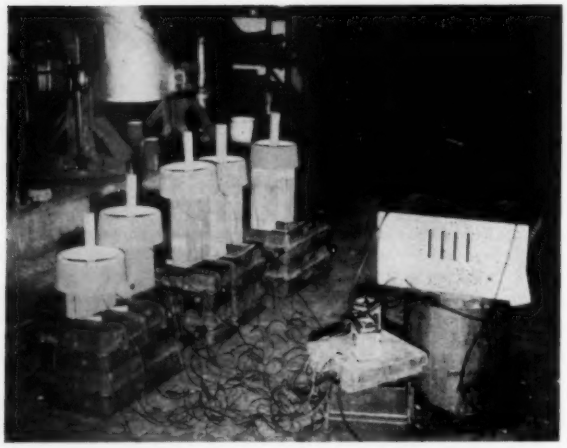


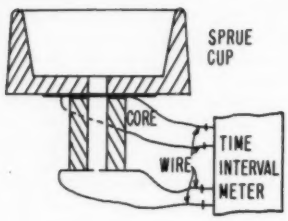
Fig. 2—Method for velocity determination of horizontal flow by photography of stream trajectory against a grid background.

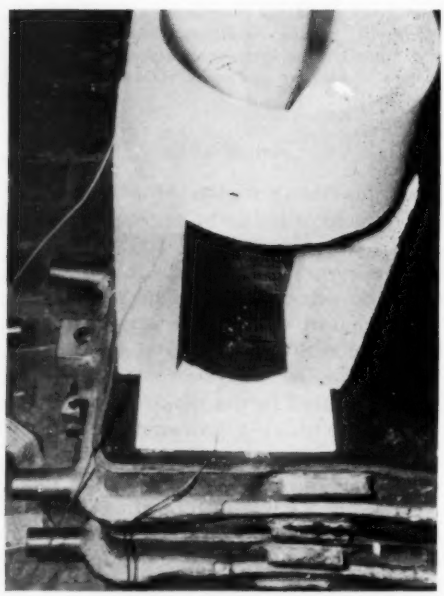
to empty by fall into open molds. In the case of horizontal flow, velocities were calculated from photographs of the trajectory of the exit stream against a grid background. Figure 2 shows the experimental arrangement employed. In the case of vertical flow produced by sprue systems (no runner attached), an electronic-time-interval meter capable of measuring time intervals of 0.1 millisecond was employed to measure the time of fall thru the sprue, from which velocities were calculated. Figure 3 shows the experimental details for the vertical fall tests and also schematic illustrations of the manner in which the counter is started and stopped as the falling stream completes the two electrical circuits. Methods of calculating flow velocities for both cases are described in Appendix A.

Plugger pouring cups, in which liquid metal was maintained at a fixed height of 4 in., were used to maintain a constant pressure head on the metal entering the sprue. Flow rates were determined from the time elapsed between the removal and re-insertion of the plug and the weights of metal passing thru the system (50 to 60 lb per test).

In selected gating systems flow conditions during







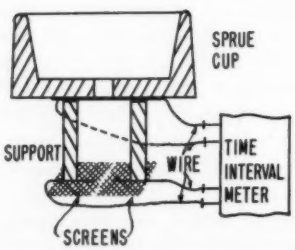


Fig. 3—Method of determining velocity of vertical flow by electronic timing of fall.

pouring were observed by means of radiographs taken with a flash X-ray unit, Fig. 4, capable of discharging 270 kv in 1 microsecond.

Experimental Results

Thermal Studies—If excessive heat loss occurs within the gating system, solid metal may form which would complicate the analysis of the flow phenomena observed. This possibility was investigated by temperature measurements along the surfaces of two component (sprue and runner) gating systems having sprue diameters of 1 in. and runner diameters of 1 in., ¾ in. and ½ in. The measurements were made at the following locations: (1) sprue cup, approximately ½ in. removed from the plug and mid-

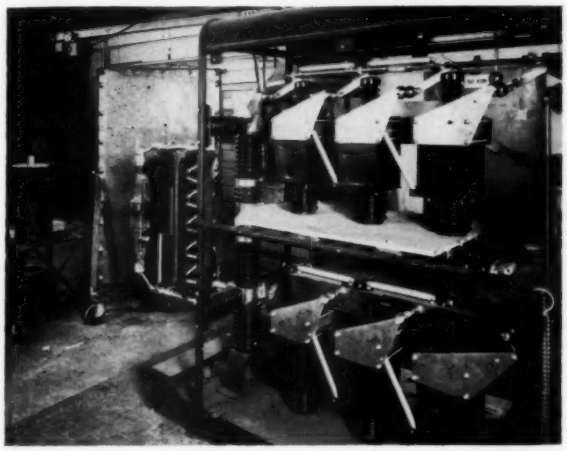


Fig. 4—Flash x-ray unit.

way with respect to the level of metal within the cup, (2) base of the sprue, and (3) end of the runner. The accuracy of these measurements was probably no better than ± 10 F due to the dynamic metal conditions. The temperatures measured in the pouring cup were approximately 2900 F. No significant temperature drop was observed between the pouring cup and the base of the sprue but at the end of 15- to 24-in. long runners the metal temperatures were approximately 2800 F, indicating a 100 F temperature loss as metal flowed through the runner.

Since temperatures of 2800 F are above the liquidus temperature (2775 F) of the 0.30 pct C steels used in these studies it is concluded that the flow channels in the tests were not obstructed by skin formation and that the metal flow characteristics were not complicated by the presence of solid particles.

Effects of Cup and Sprue on Velocity and Volume Rates of Flow—The average rate of metal flow thru a sprue cup (no sprue attached) having a 1-in. diam opening was determined to be about 10 lb per sec, which is considerably below the 12.1 lb per sec which should theoretically be obtained under the 4-in. hydraulic head maintained in the sprue cup. This difference, which amounts to about 16.5 pct of the theoretical flow rate, occurred despite the fact that a ½-in. radius existed at the junction of the sprue cup reservoir and the opening.

When sprues of various length were added to the

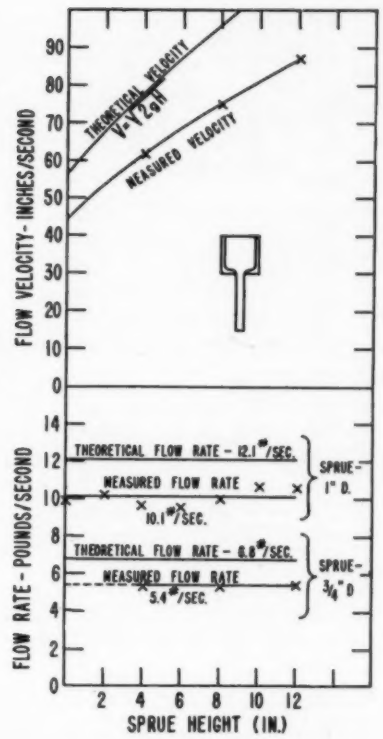


Fig. 5—Flow velocity and flow rate through integral sprues.

sprue cup the quantitative rates of flow, Fig. 5, were essentially the same as those obtained thru the sprue cup alone. Exit velocities from the sprue increased as the sprue height increased, Fig. 5, but were lower than the velocities calculated on a basis of free fall ($V = \sqrt{2gh}$). The measured velocities were approximately 22 pct below the theoretical regardless of sprue height. These velocity losses are believed to be due to properties of the molten metal, probably viscosity, rather than to any frictional turbulent effects resulting from contact with the sprue wall, since the velocity of a stream falling freely for a given distance thru the atmosphere, was determined to be the same as when falling the same distance thru a sprue. This should be expected since the metal stream is contracted during the fall and therefore does not contact the sprue walls.

Similar flow rate determinations made on ¾-in. diam systems showed, Fig. 5, that the quantity of metal flow was 5.4 lb per sec or 20.5 pct less than the theoretical rate of 6.8 lb per sec which should flow thru a ¾-in. diam orifice under a 4-in. head. The greater loss in these systems as compared to the 1-in. diam systems is probably due to greater frictional resistance at the smaller sprue cup orifice. It is concluded that the velocity and quantity of metal flowing thru an open-end, constant area sprue is determined entirely by the sprue cup conditions.

Sprue-runner Systems

The above experiments established the nature of flow through a rectilinear gating system which while not practical provides a basis for determining the effects of runners and turns in conventional gating systems. The next step was to determine the effects

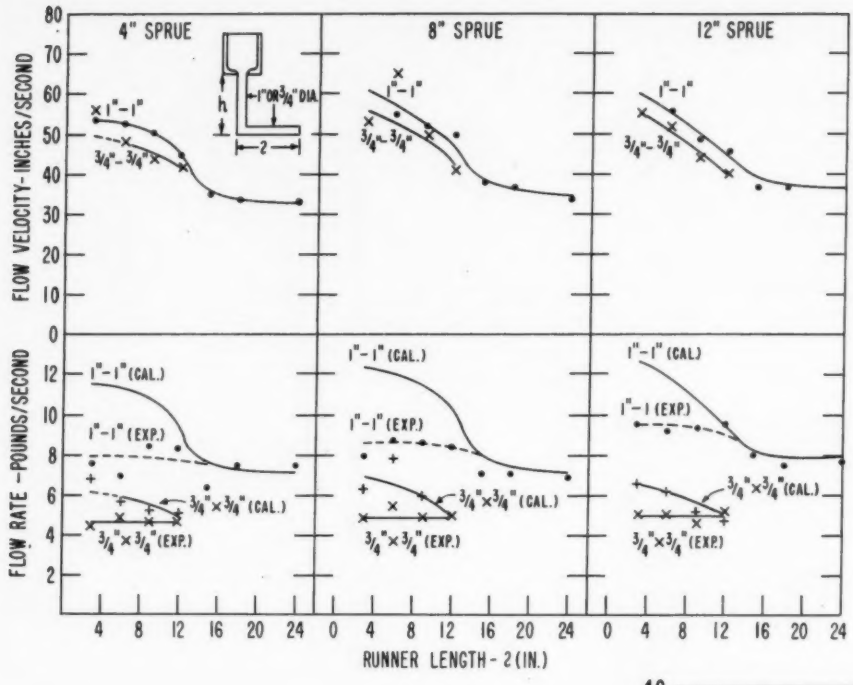


Fig. 6—Flow velocity and flow rate vs runner length for 4, 8 and 12-in. sprues.

on velocity and flow rate as the flow stream is changed from the vertical direction and directed along horizontal runners of varying lengths and diameter.

Constant Area Systems—Flow velocities and flow rates were measured in 1-in. and 3/4-in. diameter sprue-runner systems in which the length of each component was varied. The data obtained from systems having 4, 8 and 12-in. sprues are shown graphically in Fig. 6 along with the theoretical flow rates which would have been obtained if the *runners were full* at the measured velocities of flow. It can be noted that for a given sprue height increasing runner lengths up to 12 in. causes only small and possibly not significant changes in measured flow rates but flow velocities decrease continually as the runner length increases within this range. It can also be noted that as the result of changing direction and of flowing thru a runner the velocity and quantity of metal flow are less than those previously observed for free fall thru the sprue; the losses result primarily from the frictional effect of the turn at the sprue-runner junction.

By considering the exit velocities from systems having the short 3-in. long runners the quantitative effect of the turn on metal velocity can be approximated. The percentage reductions in metal velocity and flow rates resulting from the addition of a vertical to horizontal turn to sprues of various lengths in 1-in. diam systems are shown in Fig. 7. Even though the velocities thru systems having long sprues may be greater than thru systems having short sprues the velocity losses percentagewise increase with increasing sprue height. The percentage of flow rate losses follows an opposite trend varying from 30 pct with a 2-in. sprue to 6 pct with a 12-in. sprue.

Since the volume of metal leaving the runner is less than that which can be delivered by the sprue

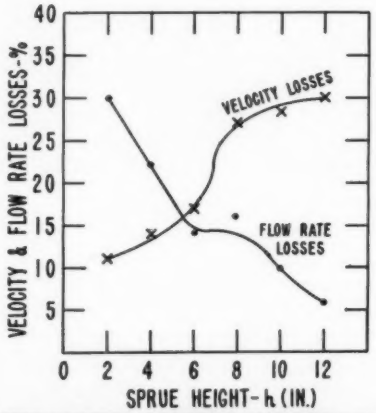


Fig. 7—Flow velocity and flow rate losses, per cent.

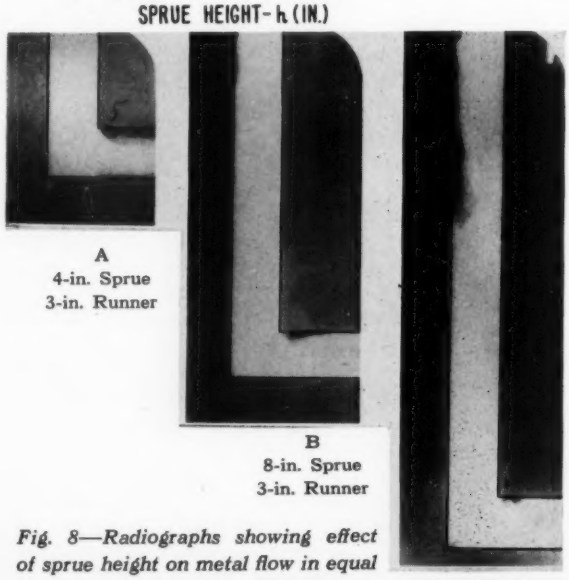


Fig. 8—Radiographs showing effect of sprue height on metal flow in equal area sprue-runner systems.

cup opening it is obvious that the sprue must eventually become full. The short sprues, due to their small volume, become full very rapidly while the longer sprues, which have greater volumes, require a longer time. Radiographs, of the type shown in Fig. 8, indicated that only sprues having a height of 6 to 8 in. or less become completely full; the 8-in. sprue being a borderline case which becomes almost full. At this critical sprue height it can be noted, Fig. 9, by a comparison of systems having a given runner length that there is an inflection of flow velocities and flow rates. These inflections at 6 to 8-in. sprue

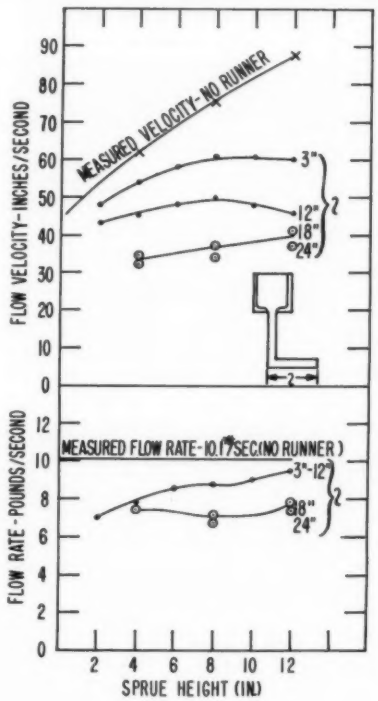


Fig. 9—Flow velocity and flow rate through 1-in.-1 in. sprue-runner systems.

heights are reflected in the percentages of both velocity and flow rate losses at turns, Fig. 7.

The reasons for this flow behavior are illustrated schematically in Fig. 10. In the system with the 6-in. high sprue there is a total pressure head of 10 in. (sprue-head plus 4-in. cup head), but when the sprue is lengthened to 8 or 10 in. there no longer is a continuous column of metal to the top of the sprue cup. As a result, the hydraulic head in the cup does not reinforce the sprue head and the effective pressure head upon the metal entering the runner must be less than 10 in., leading to the noted reversals. As sprue heights are increased further, the flow rates and velocities thru the runners tend to increase slightly. This results from the kinetic energy present in the falling stream, some of which is imparted to the column of metal at the base of the sprue, acting in effect as pressure head, to force metal into the runner.

In 1-in. diam sprue-runner systems with runners less than 15 in. in length no portion of the runner becomes completely full as can be noted by the separation of the curves, Fig. 6, showing the measured flow rates and the theoretical flow rates obtainable

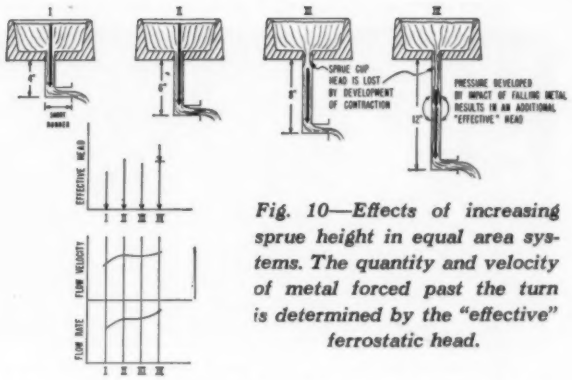


Fig. 10—Effects of increasing sprue height in equal area systems. The quantity and velocity of metal forced past the turn is determined by the "effective" ferrostatic head.

if the runner were flowing full at the measured velocity. This is also substantiated by X-ray studies, Fig. 11. However, as the metal flows thru the runner for greater distances, frictional effects cause velocity to continually decrease while flow rates thru the sprue-runner junction remain essentially constant with the result that the runner becomes increasingly more full. This is shown by the convergence of the actual and theoretical flow rate curves, Fig. 6. With runner distances greater than 15 in. the runner be-

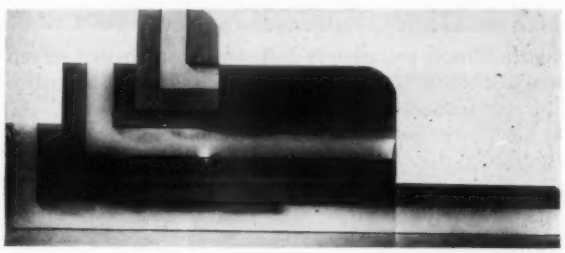


Fig. 11—Radiographs showing effect of runner length on metal flow in equal area sprue-runner systems. Sprue height constant, 4 in.; runner length varies from top to bottom, 3, 12 and 24 in.

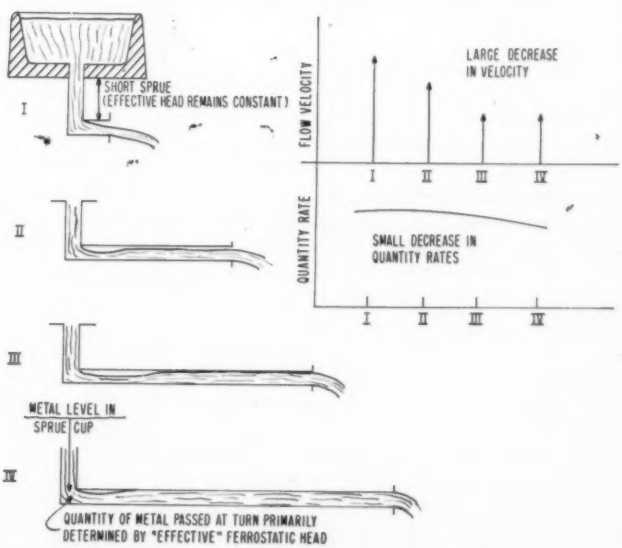


Fig. 12—Effects of increasing runner length in equal area systems.

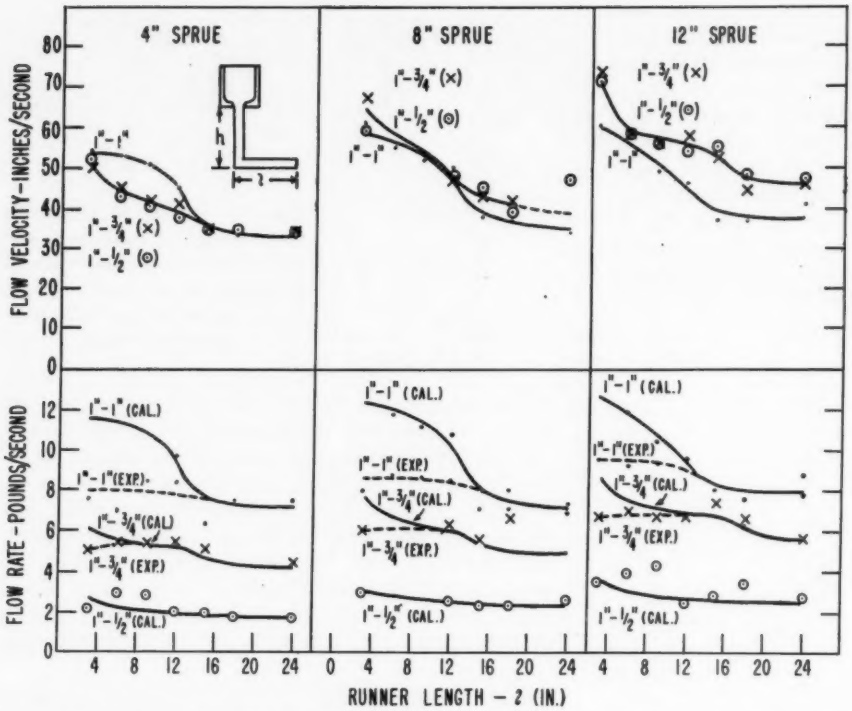


Fig. 13—Flow velocity and flow rate vs runner length for reduced area sprue-runner systems

comes almost completely full, with consequent development of a back pressure which serves to reduce the amount of metal entering the runner from the sprue. Flow rates as well as flow velocities are reduced as the result. Figure 12 presents schematic illustrations of the effects of increased runner lengths.

The flow rate and velocities thru equal area sprue-runner systems having ¾-in. diam, Fig. 6, indicate that flow velocities are approximately 5 ft per sec lower in these systems than in the 1-in. diam systems due to greater frictional effects; the flow rates are necessarily much lower due to the reduced channel areas. The behavior of ¾-in. systems is analogous to the 1-in. systems although ¾-in. runners become full at a slightly shorter distance from the sprue as indicated by the meeting of the measured and theo-

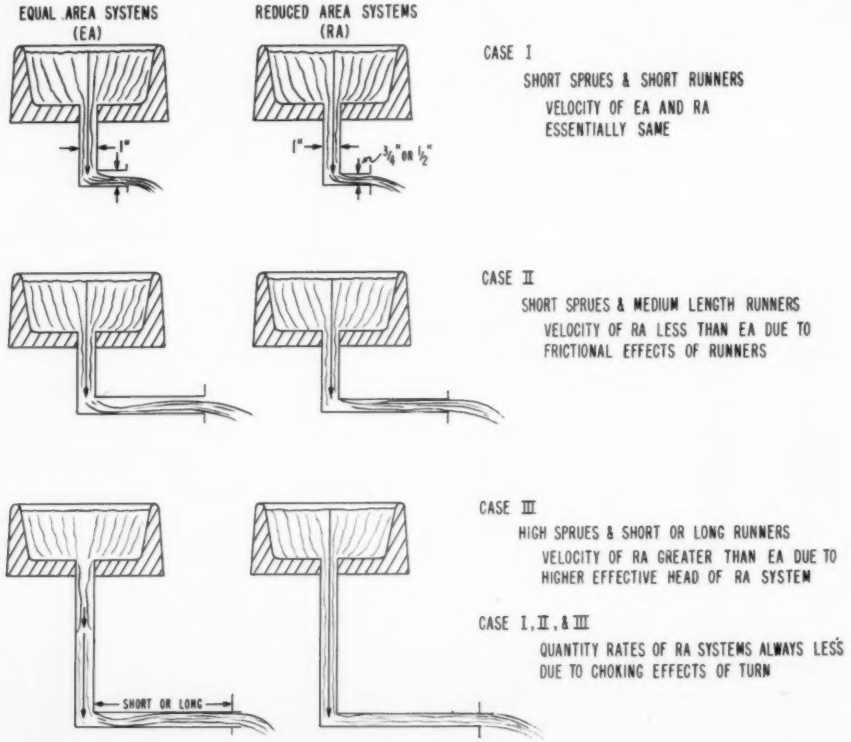


Fig. 14 — Differences between equal area and reduced area systems.

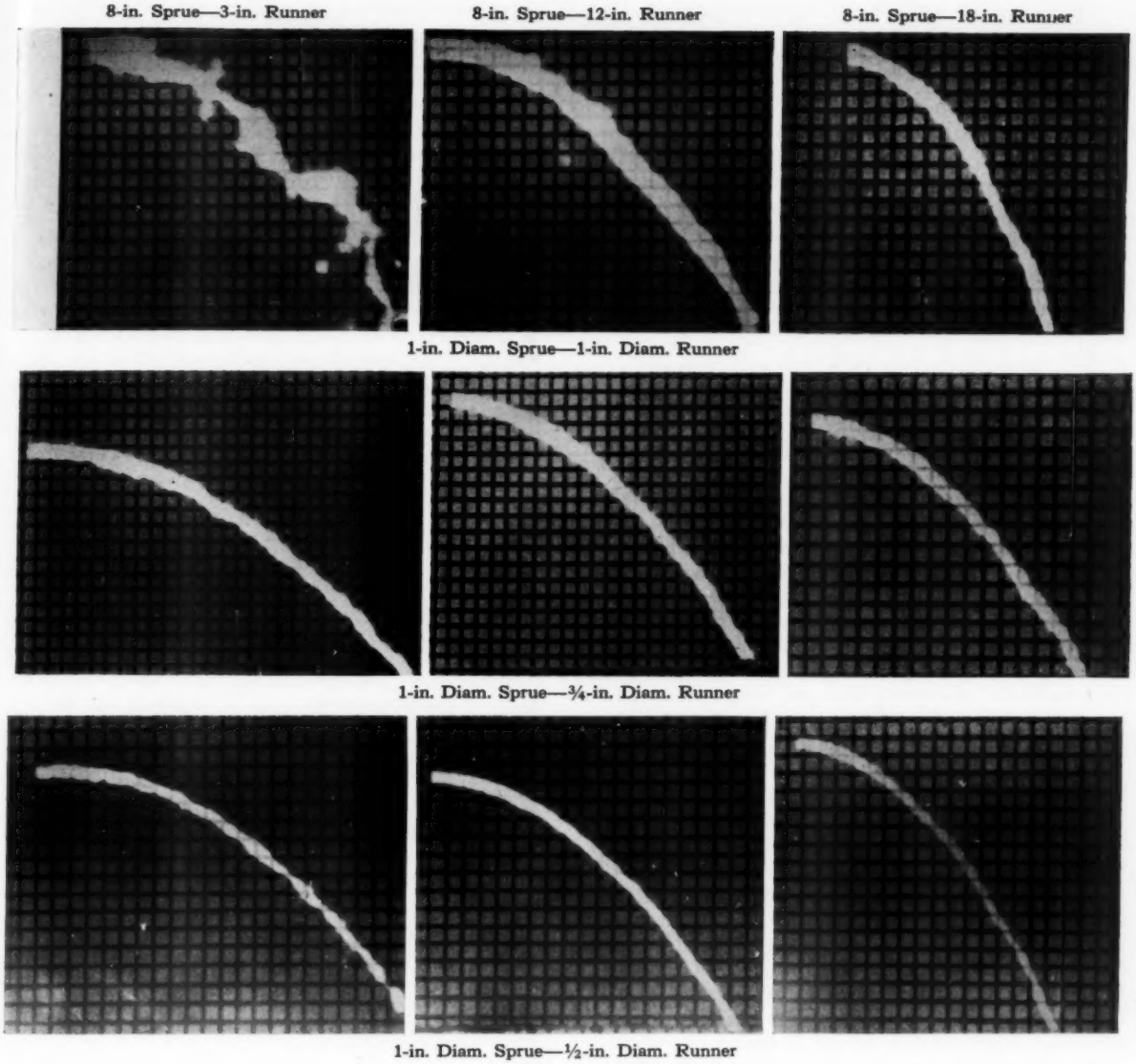


Fig. 15—Photographs of exit stream.

retical flow rate curves at a point 12 in. from the sprue.

Reduced Area Sprue-Runner Systems—Velocity and flow rate measurements for systems having ¾- and ½-in. diam runners attached to 1-in. diam sprues are compared in Fig. 13 with similar data obtained from the 1-in. diam, equal area systems. It can be noted from a comparison of measured flow rates with the theoretical, full-runner flow-rates based upon the measured velocities that the point at which the runner becomes full is a function of the runner diameter. As pointed out earlier the 1-in. diam runner becomes full at a point 15 in. from the sprue, the ¾-in. diam runner becomes full at a point 12 in. from the sprue, and the ½-in. diam runner is essentially full at the shortest length (3 in.) tested as noted by the coincidence of the theoretical and actual flow rate curves.

Increasing sprue heights and runner lengths in equal area and reduced area systems produces different velocity changes thru the two systems. These differences and the conditions within the systems are depicted schematically in Fig. 14. With short sprues and short runners both sprues are filled completely and exert equal hydraulic pressures upon the metal entering the runner so that the exit velocities are essentially the same. As the runners are lengthened, velocities are greater thru the runner having the largest diameter despite the fact that both systems are under equal hydraulic pressures. This is due to the greater surface area to volume ratio of reduced area runners which results in greater frictional resistance which reduces flow velocity. However, when the sprues are long the sprue in the equal area system is only partially full as was described earlier, while the sprue attached to the reduced area runner is completely full as the result of the choking effect produced at the sprue-runner junction. Hence, in

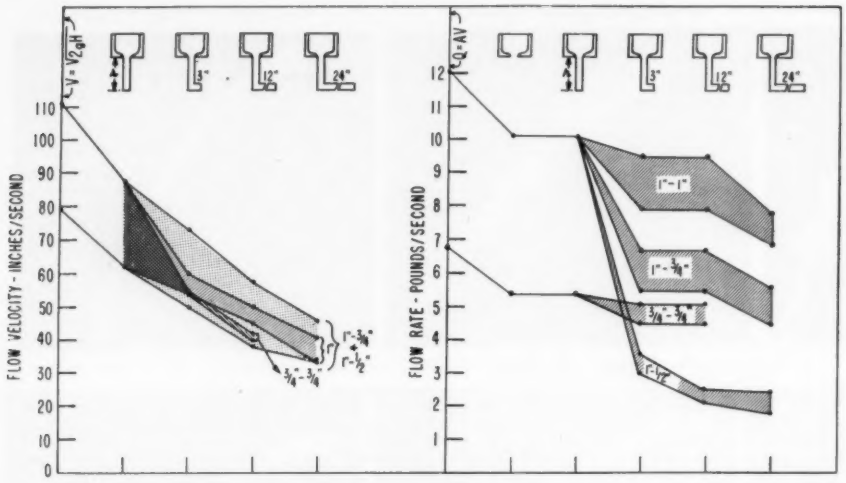


Fig. 16—Summary curves for sprue-runner systems.

the latter case there is a greater hydraulic head upon the system which results in higher flow velocities despite greater frictional resistance which is present.

The turbulence present in the metal flowing thru short runners, particularly of equal area type, is reflected in the irregularities of the exit streams, Fig. 15. As the runners are lengthened or reduced in diameter so that they become full, smoother exit trajectories are observed. Reduced area systems having long runners develop smooth, lamellar-like exit flow.

A summary of the velocities and flow rates at different points in the various combinations of sprues and runners which have been described is presented in Fig. 16. The bands represent ranges obtained by variations in sprue heights between 4 and 12 in. In the reduced area sprue-runner systems the velocity spread in the runners is great due to wide differences in pressure head produced in the sprue which become full almost immediately after the start of pouring. In the equal area system where only the short sprues become full, the height of the sprue has only a small effect on metal velocity in the runner.

It is of interest to note that the ¾-in. diam, 12-in. long runner can deliver only 4½ to 5 lb of metal per second when attached to a ¾-in. diam sprue but when this same runner is attached to a 1-in. diam sprue it can deliver up to 6½ lb of metal per second. Practical relationships of this type may be evolved by consideration of the summary charts.

Sprue Runner-Finger Gate Systems

One Finger-Gate Systems—Flow characteristics were determined for 3 component systems made by the addition of a finger-gate to the end of and at right angles to the runner. The lengths of each component as well as the diameter of the finger-gates were varied systematically. The data, Fig. 17, show the same general trend toward increasing velocities and flow rates with increasing sprue height as observed previously for sprue-runner systems. As the finger-gate diameters are reduced a corresponding reduction of flow rates follows: the exit velocities thru systems having long sprues are markedly increased due to the greater hydraulic heads which are developed. It is

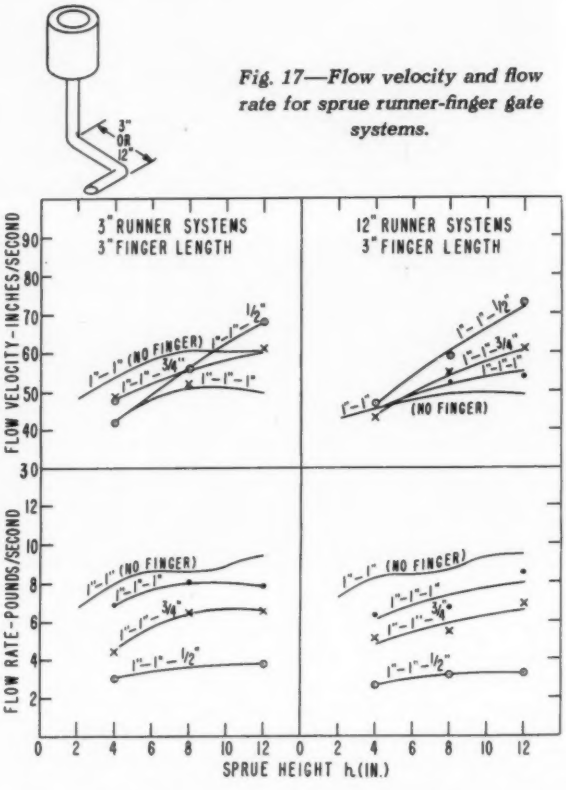


Fig. 17—Flow velocity and flow rate for sprue runner-finger gate systems.

probable that the sprues in all systems having the ½-in. finger-gates become completely filled almost immediately after the start of pouring.

The addition of a 1-in. diam finger-gate to a 1-in. diam runner produces apparently anamolous velocity effects, Fig. 17, as the runner length is increased from 3 to 12 in., reducing velocities when added to a 3-in. runner and increasing velocities when added to a 12-in. runner. When the metal flowing thru a runner strikes the end of the runner a pressure zone is generated, Fig. 1. The pressure which is developed results in forced flow of the metal thru the nearest finger-gate. Since the 3-in. runner is only partially filled, an effective pressure zone cannot develop. In

the case of the 12-in. runner the conditions of a more completely filled channel and longer path for the development of momentum result in high pressurization at the runner-finger junction with consequent forced flow of metal. As a result, the difference in flow velocities in simple sprue-runner systems having 3- and 12-in. long runners are essentially eliminated by the addition of a finger-gate as can be noted by the similarity in the exit velocities, Fig. 17.

Two Finger-Gate Systems—In these studies two ¾-in. diam finger-gates, which have a total area approximately equal to one 1-in. diam finger-gate, were employed. One finger-gate was attached to the extremity of the runner, the other at an intermediate position. As in the preceding experiments the lengths of the sprue, runner and finger-gates were varied. Figure 18 shows that the quantity and velocity of metal flow is greatest thru the end finger-gate regardless of runner length or location of the intermediate finger-gate. Moreover, these differences are essentially constant regardless of the geometry of the gating system. Approximately 60 per cent of the total metal discharged flows thru the end finger at a velocity which is 5 ft/sec (12 pct) greater than thru the intermediate finger-gates. While the maximum velocity of metal exit is essentially the same for one finger-gate and two finger-gate systems (Figs. 17 and 18) the total quantity of metal delivered is greatest thru the two finger-gate system. As was the case in one finger systems, runner length seems to have no marked effect on the flow characteristics thru the fingers as can be noted by the similarity in the velocities and flow rates, Fig. 18, in systems where the only variation is runner length. Thus, while metal velocities thru the runner itself are a function of runner length these differences are minimized as the metal passes through the finger-gates.

Four Finger Systems—In the flow studies thru systems having four identical finger-gates, sprue and runner diameters were again maintained at 1 in. while finger-gate diameters of ¾, ½, ⅜ and ¼ in. were employed, making the sprue area to total finger-gate area ratios equal to 1-2, 1-1, 1-½ and 1-¼, respectively. The finger-gate lengths in this phase of the work were maintained at constant lengths of 3 in. and were equally spaced from end to end along 12-in. runners. Each variation of finger-gate diameters was studied with sprues located both at the center and at one extremity of the runner. However, when end sprues were used, the runner was lengthened by 2 in. at the sprue end so that the metal was required to travel in the horizontal plane before entering any of the four finger-gates. The flow rate and velocity measurements obtained from these systems are shown in Fig. 19. As has been observed in previous studies, increased choking at the finger-gates increases exit velocities and reduces the amount of metal delivered, but at the same time tends to equalize velocities and flow rates thru the finger-gates. Increased choking increases the average exit velocities by causing the runners and sprues to become full which results in raising the hydraulic head of the metal in the sprue. Since there is no further increase in velocities, either in systems having central or end sprues, when finger-gate diameters are reduced from ⅜ to ¼ in. in diameter it may be concluded that the systems are completely filled in both cases and a sprue to finger area ratio of 1 to ½ is required to obtain a pressurized system during first stage pouring. This observation is substantiated by a comparison of the total flow rates thru the various systems when sprue height is increased, Fig. 20. Flow rates thru systems having ¾ and ½-in. diam finger-gates do not follow a definite trend as the sprue height is increased but show the fluctuations attributed earlier to lowered hydraulic heads in sprues above 6 in. in height which are only partially filled. Flow rates thru the systems having the smaller ⅜ and ¼-in. diam finger-gates, however, show a continuing flow rate increase with increasing sprue height which is to be expected if the sprues are maintained full.

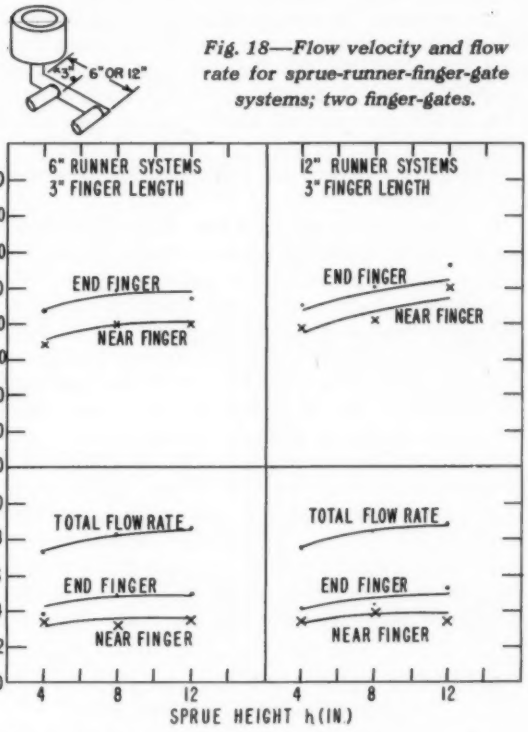


Fig. 18—Flow velocity and flow rate for sprue-runner-finger-gate systems; two finger-gates.

Even with the drastic choking effect produced by the ¼-in. diam finger-gates, momentum-pressure effects are not completely overcome as indicated by the tendency toward greater exit velocities thru the finger-gates at the runner extremities, Fig. 19. Essentially equal quantities of metal are delivered thru each finger-gate in the center sprue system when the finger-gates are ⅜ in. in diameter and in the end sprue system when the finger-gates are ¼ in. in diameter. Complete equalization of velocity and flow rates thru the four finger-gates probably can be obtained only during second stage pouring when velocities thru the system are retarded by back pressure produced when the mold fills above the level of the finger-gate openings.

Figure 21 presents a summation of the velocities and flow rates thru various sprue-runner and sprue-runner-finger-gate systems indicating the modifica-

tions resulting from the addition of various parts of the gating systems. The bands represent maximum and minimum values obtainable under sprue height variations of 4 to 12 in.

Summary Conclusions

The results of this investigation of first stage pouring conditions indicate that the effect of sprue height is dependent on the nature of the gating system, i.e., on whether the system is pressurized (choked) or free-flowing. Choking effects which produce a pressurized condition may be developed by constrictions (decrease in area relationships) or by frictional effects developed by turns and long runner distances. In equal area systems turns are by far the most potent variables affecting the development of choked conditions. Sprue height variations have relatively little effect on systems which remain free during pouring.

The major conclusions deduced from these studies are as follows:

1. The quantity of metal flow thru equal area gating systems is governed primarily by the metal head in the sprue cup and size of sprue cup opening.

2. Flow velocities thru the sprue are related to the metal head in the sprue cup and to the distance of fall within the sprue. The inherent characteristics (viscosity and surface tension) of molten steel reduce the velocity of fall to approximately 22 per cent less than the velocity of a freely falling body.

3. Under 4-in. sprue cup heads, resistance to flow around the turn at the base of the sprue in equal area systems is such that it will cause sprues having heights of 6 to 7 in. to become completely full so that the hydraulic heads of both the sprue and sprue cup act upon the metal in the gating system. Slight additional increases in sprue (to 8 in.) height will cause reduction in flow velocities due to reduced total head, but further sprue height increases permit the kinetic energy of the falling stream to reinforce the existing hydraulic head and thus increase flow velocities.

4. In sprue-runner systems where the runner has a cross-sectional area less than that of its sprue, the flow resistance at the turn is increased to the extent that high sprues become filled; as the result greater hydraulic heads are developed within the sprues. Thus, when the sprue height exceeds 6-8 in. velocities thru reduced area systems exceed those thru equal area systems.

5. Metal flowing along a runner after leaving the sprue travels at a constantly diminishing velocity which causes the metal to "pile up"; when the flow distance is equal to about 15 times the runner diameter the runner becomes full.

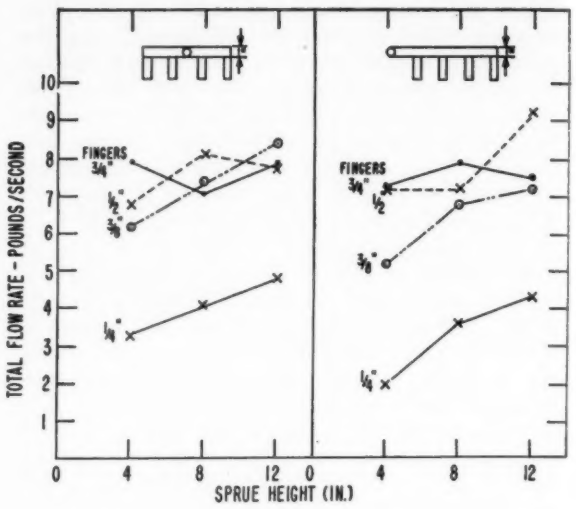


Fig. 20—Flow rate vs sprue height and finger-gate diameter; four finger-gates.

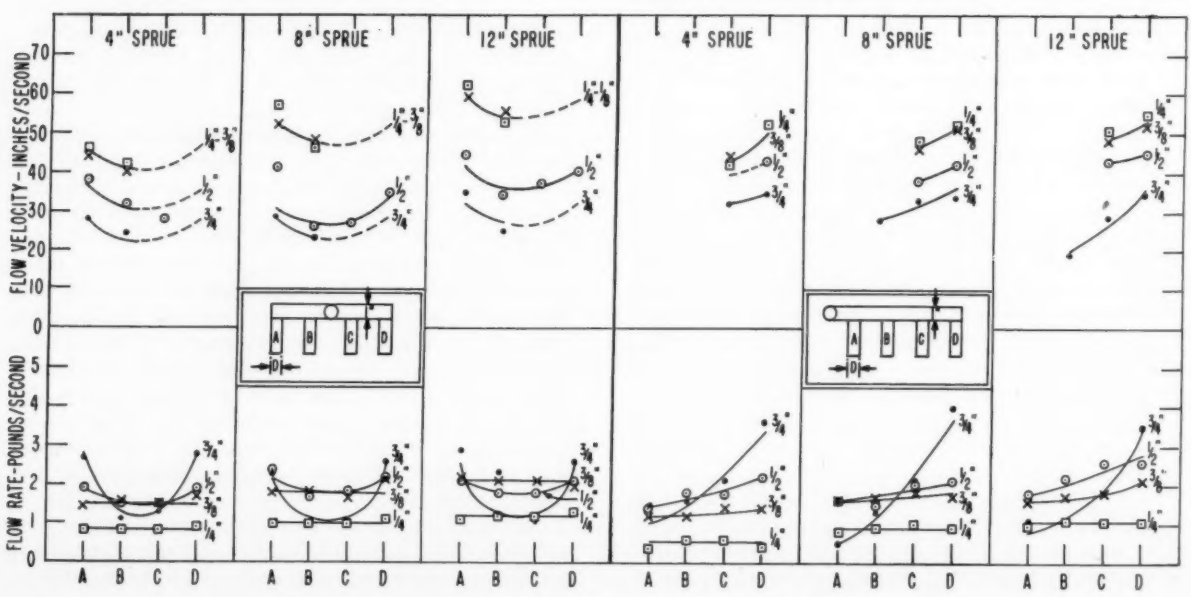


Fig. 19—Flow velocity and flow rate for sprue-runner-finger-gate systems; four finger-gates.

6. The addition of a 1-in. diam finger-gate to the ends of runners having lengths between 3 and 12 in., has the effect of minimizing velocity variations normally present along the length of the runner. Velocities and flow rates thru the finger-gates of such systems are essentially independent of runner length.

7. The addition of two finger-gates to equal area sprue-runner systems produces a slight increase in total flow rates even though the total cross-sectional area of the two fingers is made equal to the runner area. The momentum of the stream flowing thru the runner causes 60 per cent of the metal to be discharged thru the finger at the runner extremity. The velocity thru this finger-gate is 12 pct greater than thru the finger nearest the sprue.

8. In any four finger system the maximum velocity of the metal streams leaving the finger-gates is essentially the same regardless of whether the sprue is located at the center or extremity of the runner. Total flow rates, however, are somewhat greater thru systems where the sprue is centrally located due to the shorter flow paths and less frictional resistance.

9. Choking of four finger-gate systems by the use of sufficiently small fingers (SA:FA = 1:½ for systems having central sprues and SA:FA = 1:¼ for end sprues) causes flow thru all four fingers to become nearly equal. However, under these conditions metal velocities are high and total flow rates are low. In practice it would be desirable to sacrifice some degree of flow uniformity for the lower velocities and higher flow rates which prevail with the larger fingers.

APPENDIX A

To measure velocities thru sprue-runner and sprue-runner-finger systems, the exit stream was photographed against a grid background during the entire interval of pouring on a double exposure 16-mm film. The procedure consisted of first photographing the grid under normal lighting conditions, rewinding the film and then photographing the metal stream at low motion setting. It was found that the characteristics of the exit stream remained essentially constant during the entire pouring cycle. Consequently, velocity calculations were made from one 16-mm frame chosen from the film strip and enlarged to 4 × 5 in. as shown in Fig. 2.

If air resistance during fall is neglected, the horizontal component AB, Fig. 2, of the exit velocity of the flow stream may be assumed to be V, the actual velocity at the exit. The metal falls in the vertical direction, BC, with the acceleration of gravity. If a particle of metal travels from A to C in t seconds, the horizontal distance AB and the vertical distance BC in terms of t are expressed by (1) $AB = Vt.$ and (2) $BC = \frac{1}{2}gt^2 = 16.1t^2$. Substituting the value of t from (1) for t in (2), results in (3), $V = \sqrt{\frac{16.1 \times AB^2 \times 12}{BC}}$ (inches per second), the equation used throughout this investigation for experimental velocity calculations.

The use of equation 3 for a single velocity calculation may result in error, due to fluctuations in the falling stream; for this reason a number of calculations were made along the entire parabola of metal flow and an average was then taken.

The experimental data obtained in the experiments were compared to the theoretical flow velocities and flow rates of the metal at the base of the sprue as determined by the use of formula (4) $V = \sqrt{2gH}$ and the quantity equation (5) $Q = AV$, in which

V = Velocity in in./sec
H = Total head in in.
Q = Flow rate as volume; converted to lb/sec by suitable factors.
A = Cross-sectional area in square inches of governing member of gating system.

Flow velocities in the vertical direction thru sprues were obtained from the measured time intervals of

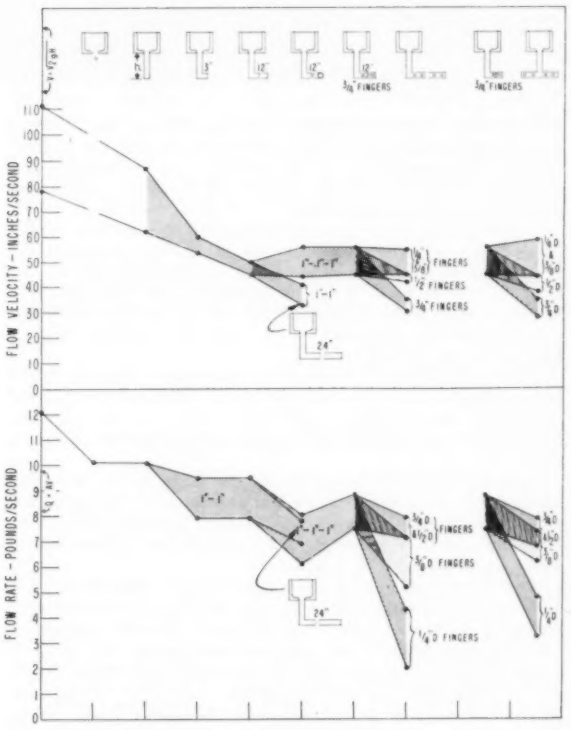


Fig. 21—Flow rate and flow velocity summary curves.

metal fall since velocities are directly proportional to time, (6) $V = gt$.

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DISCUSSION

Chairman: T. D. STAY, Reynolds Metals Co., Cleveland.

Co-Chairman: P. J. PETTO, JR., Arrow Aluminum Castings Co., Cleveland.

H. J. FISHER *(Written Discussion):*[1] As it is well-known the

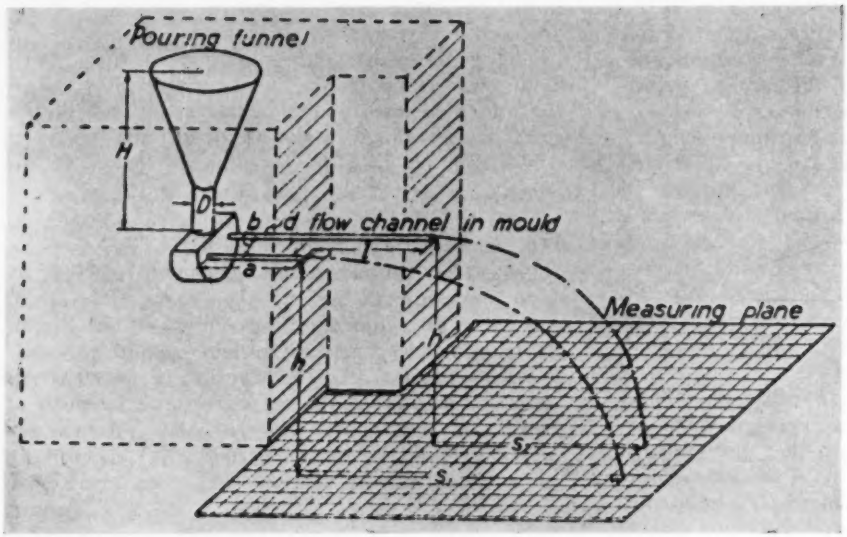


Fig. A—Apparatus for the determination of discharge velocities.

development of the fundamental laws of fluid motion is based on an ideal conception of frictionless fluids and the present authors' investigation is a contribution in establishing the extent of deviations to be expected in foundry applications dealing with liquid metal flow.

In this regard there are certain points of interest in this paper which it is felt merit discussion. These are namely:

1. In the section "The effect of cup and sprue on the velocity and volume rates of flow" it is stated that the measured velocities were approximately 22 per cent below the theoretical values regardless of sprue height and this loss was believed due to liquid viscosity.

This is considered to be a valid assumption especially since it is recognized that the onset of liquid turbulence within the sprue cup during filling will further increase the apparent viscosity of the molten metal and thus reduce its ability to flow.

However, it would be of interest to learn if any controlled variation in metal temperature was also found to influence flow rates through the sprue sizes indicated, especially since some correlation with viscosity data for iron-carbon alloys, over a range of temperature is available.[a]

This further examination of temperature influence on flow rates would to some extent aid in assessing the contributory influence of known true (not apparent) viscosity changes in affecting flow rates in the gating systems used. This point is raised since it is considered[b] on theoretical and some experimental basis that when inertia forces (—Mass × Acceleration) predominate, as indicated by large Reynold's Numbers for flow conditions under examination, viscosity forces may be neglected in influencing flow rates since they are small in comparison with inertia forces.

2. It may be of interest also to mention Ruff's[c] investigation of the running quality of liquid malleable iron and steel (by measurement of discharge velocities as shown in Fig. A and using much smaller amounts of metal in channels of 5, 6 and 8 mm diameter) which corroborates in general the present authors' results for drop in velocity in relation to length of channel run.

However, he found,[c] contrary to expectation, that the drop in velocity decreased as the casting temperature was lowered. In other words, less resistance to velocity of flow in the runner at lower metal temperatures. Is this somewhat contradictory point corroborated with the larger channels used in the present work since Ruff acknowledges some doubt as to the validity of his explanation of the phenomena involved?

[1] Metallurgical Engineer, Physical Metallurgy Research Laboratories, Department of Mines and Technical Surveys, Ottawa, Canada.

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[b] P. P. Ewald, Th. Poschl and L. Prandtl, "The Physics of Solids and Fluids," Blackie and Son Limited (1930).

[c] W. Ruff, "The Running Quality of Liquid Malleable Iron and Steel," Iron and Steel Inst., Carnegie Sch. Mem., vol. 25, pp. 1-39 (1936).

J. G. MEZOFF:[2] The authors are to be congratulated for their extensive studies of flow velocities and flow rates in sprues. The knowledge derived from such studies will be extremely valuable to practical foundrymen.

My comments are concerned with the time element involved in the experimental work, about which little was said in the paper. I understand that flow rates and velocities were measured at a time 2 sec after the metal started down the sprue.

It appears to me that such measurements are subject to an end (or starting) effect. It would be well to clarify this point in the paper especially since under some conditions the velocity and flow rates could be constantly changing. I refer to the conditions described in the paper wherein the runner is of reduced area. In this case, as the author states "it is obvious that the sprue must eventually become full."

Hence, the ferrostatic head will continually increase during the pour. This will result in a corresponding change in velocity and flow rates.

The significance of these comments may lie with castings of considerably greater weight, the pouring of which may involve changing flow velocity rates more noticeable than in your laboratory work. However, since the purpose of your work is to demonstrate principles and phenomena, I think you would do well to comment upon the relationship of time to your measurements.

MEMBER: From the paper it seems that the choke is at the top of the sprue, and the other choke occurs in the runner as the pouring continues. In the various flow rate calculations, at what time during the short pouring rate were these studies made?

MR. JOHNSON: This was done about 2 sec after the sprue and pouring cap had been removed. We took random exposures all along the motion picture film.

MEMBER: Will further work be done with the shape of the sprue and the runners to eliminate turbulence in the systems? In light metals a great deal of turbulence causes oxide inclusions and entrapped gases.

MR. JOHNSON: This merely was a quantitative experiment to prove how good our qualitative experiments had been.

[2] The Dow Chemical Co., Midland, Mich.

SOLIDIFICATION AT CORNER AND CORE POSITIONS

By

F. A. Brandt,* H. F. Bishop* and W. S. Pellini†

ABSTRACT

The solidification characteristics of cast steel plates joined as "L" and "T" sections and of bushings of various inside diameters were determined by thermal analysis. It was established that "L" and "T" sections require 1⅓ and 1½ longer time for solidification than flat plates. The solidification time of bushings is longer than that of flat plates of equivalent thickness by an amount which depends on the core diameter; however, the effect does not exceed 15 per cent, which amount is obtained with core diameters of ¼ to ½ the wall thickness. The relation of geometric and effective V/SA ratios with respect to the solidification times is discussed. The effects of casting geometry on the rate of heat flow from casting to sand which determine solidification rates are shown to be related to the features of divergency or convergency of heat flow patterns in the sand.

Introduction

Previous thermal analysis studies[1,2,3,4] have established quantitatively the characteristics of wall growth as defined by the travel of "start of freeze" and "end of freeze" waves from the casting surface to the thermal center of the section. These studies were concerned with the case of plate and bar castings of uniform wall thickness which present the simplest possible problem of analysis, inasmuch as solidification proceeds at equal rates from opposite surfaces and the thermal center (last position to solidify) is the same as the geometric centerline. The present report is concerned with the case of wall growth from opposite surfaces which occurs at different rates due to differences in the rate of heat extraction from the respective casting-mold interfaces. Two effects may be expected to result from such unbalanced solidification conditions:

(1) Displacement of the thermal center with respect to the geometric center.

(2) Change of the time of final solidification.

Quantitative information as to the nature and degree of these effects is of primary importance to foundry practice, inasmuch as positions of unbalanced solidification typified by points of junction or of curvature represent the positions of development of shrinkage, hot tearing and other casting defects.

Studies of the nature of solidification and of heat flow were conducted for "T" and "L" sections and for bushings of various core diameters. "L" sections represent the case of unbalanced solidification due to rapid heat extraction from the external corner and slow heat extraction from the opposite internal corner. "T" sections represent the case of slow heat extraction from internal corners without the benefit of rapid heat extraction from opposite positions. Bushing, depending on the ratios of I.D. to O.D. provide conditions of unbalanced solidification due to differences in rates of heat flow from convex and concave

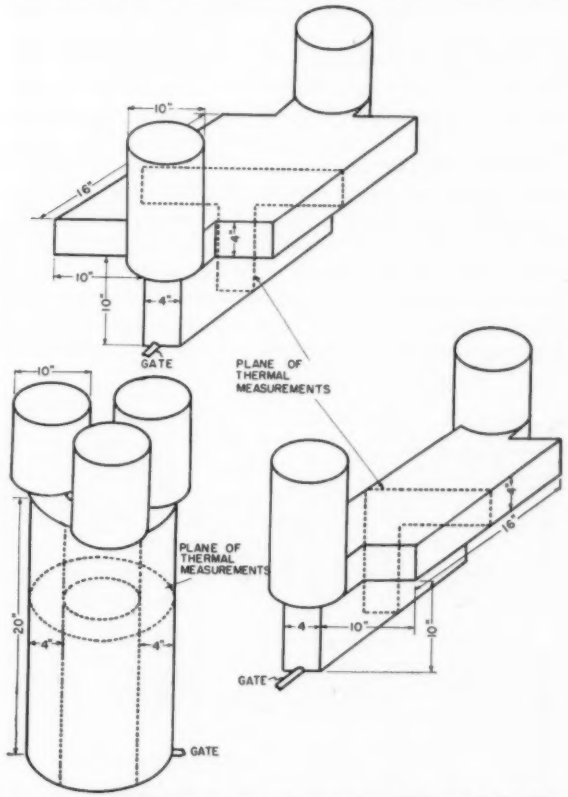


Fig. 1—Test Castings Employed for Thermal Studies Showing Gating and Risering Practices.

* Metallurgist and † Head, Metal Processing Branch, Naval Research Laboratory, Washington, D. C.

surfaces and also conditions of insufficient sand volume for heat absorption, as represented by small cores which develop heat saturation prior to completion of solidification.

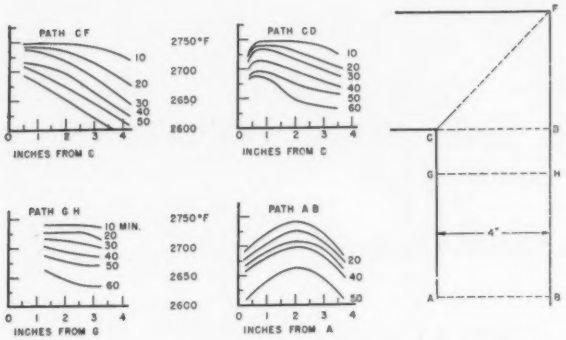


Fig. 2—Thermal Gradients in "L" Section at Various Times After Pouring.

Test Procedures

The test castings represented 4-in. thick sections of the systems shown in Fig. 1; gating and risering arrangements are indicated in the figures. All test casting were made of a length sufficient to essentially eliminate "end" and "riser" heat flow effects at the position of thermal measurements. Accordingly, the data are representative of conditions such that the section length is great compared to the wall thickness. Thermocouples were located along various critical directions (to be described) in the plane shown in Fig. 1. The thermal analysis techniques used have been described in previous reports.[1,2,3]

The castings were poured with induction furnace steel having, with one exception indicated in the text, a nominal analysis of 0.25 pct C, 0.40 pct Si, 0.60 pct Mn with 0.10 pct Al added to the ladle for deoxidation. The molds were air dried and made with an A.F.S. No. 80 silica sand bonded with 3 pct water, 3 pct bentonite, 0.5 pct cereal and 1.5 pct dextrine. Pouring temperatures ranged from 2850 to 2900 F.

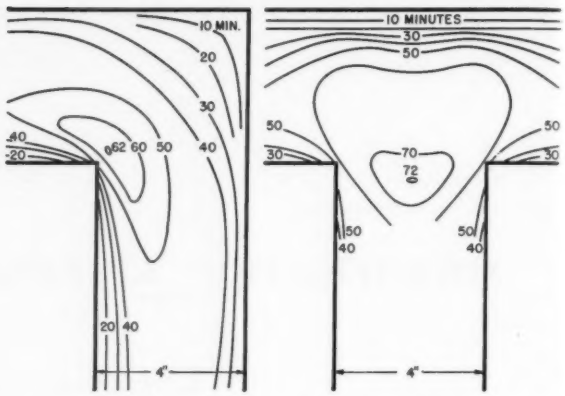


Fig. 4—Progression of "End of Freeze" Wave Contour in Solidifying "L" and "T" Sections.

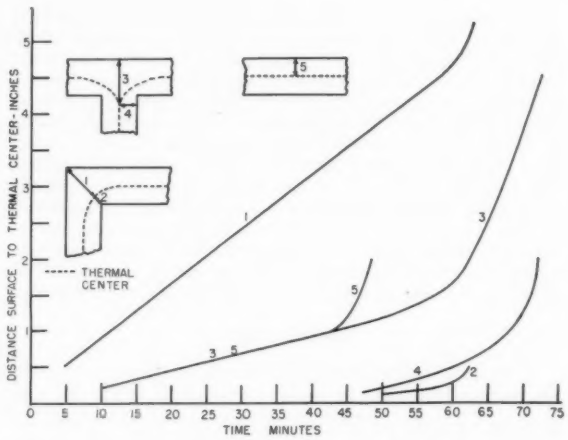


Fig. 5—Progression of "End of Freeze" Wave Along the Paths Indicated.

Solidification of "L" and "T" Sections

Approximately 70 cooling curves were obtained at the various "L" and "T" section locations indicated by Figs. 2 and 3. The conditions of solidification as represented by isochrones are shown in Fig. 4. The areas on the mold side of the isochrones indicate completely solid metal while the center areas indicate metal which is partially liquid (mushy state). The rate and extent of movement of the "end of freeze" waves from various critical positions are indicated in Fig. 5.

The relative effects of the internal and external corners may be deduced by comparison with the rates of solidification from the lateral surfaces of flat plate. Except for the last several minutes of the freezing interval, the rate of solidification from the external

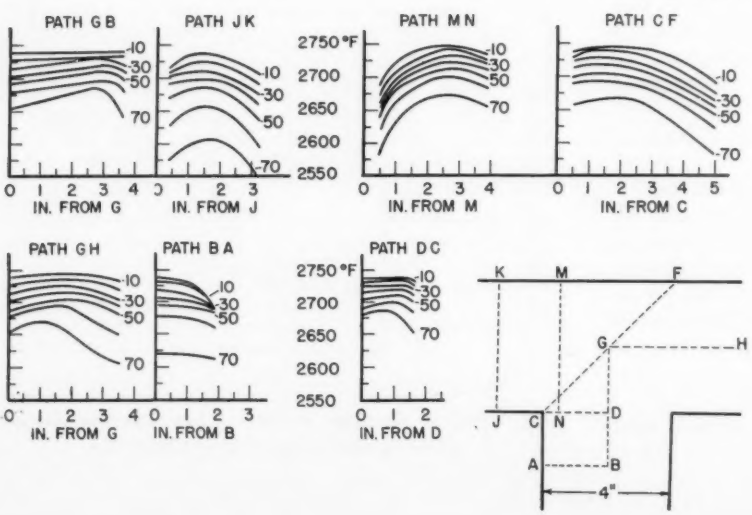


Fig. 3—Thermal Gradients in "T" Sections at Various Times After Pouring.

corner is approximately three times greater than for the case of the flat plate. At 20 min, for example, there is a completely solid skin to approximately 1.5 in. from the external corner and approximately 0.5 in. from the flat surface. In the case of the internal corner a completely solid skin is not developed until approximately 50 min. Very soon after pouring the internal corner becomes essentially ineffective in removing heat and contributes relatively little to process of heat extraction during remainder of solidification.

The position of final solidification in the "L" section is located approximately 0.5 in. from the internal corner along a line in the direction of the external corner. In the case of the "T" section this position is located along the center-line of the vertical leg. The vertical leg provides the final point of solidification inasmuch as the solidification of the two horizontal legs at locations near the joint is always in a more advanced stage than corresponding positions in the vertical leg. This behavior results from the fact that the solidification of the horizontal legs near the junction is influenced by only one surface of slow heat loss while the vertical leg is influenced by two such surfaces. In the case of the "L" section each leg is influenced equally in this respect with the result that an equivalent delay in solidification is developed in each leg; accordingly, the last position to solidify is located at the geometric junction of the two legs.

It should be noted that the position of shrinkage which may be developed in a hot spot region is not the position of final solidification. Because of gravity the remaining liquid drains to the bottom of the hot spot region and the shrinkage void develops at a position which is located directly above the last position of solidification. Thus, depending on the orientation of the "T" section with respect to the vertical direction, the shrinkage void will be developed either in the vertical leg side of the hot spot or in the horizontal leg side.

The relative solidification times of a 4-in. thick flat plate and of the 4-in. thick "L" and "T" sections are 48, 62 and 72 min respectively. Thus, the solidification times which may be calculated for flat plates by volume to surface area relationships (V/SA) require empirical adjustment by factors of 1⅓ and 1½ respectively for flat plate sections joined in "L" and "T" combinations. This information provides a basis for the adjustment of riser size at such positions.

Solidification in Bushings

The thermal gradients developed during the solidification of 4-in. thick bushings having core diameters of 2, 4, 8 and 16 in. are shown in Fig. 6. It can be noted for the casting with the smallest core (2-in. diam) that after approximately 30 min the gradients over the entire section slope toward the outer surface of the casting indicating that essentially all of the heat is leaving the casting thru the mold surface. This same condition occurs in the casting with the 4-in. diam core although it does not begin until approximately 40 min after pouring. The 8- and 16-in. diam cores continue to extract heat from the casting during the entire solidification period as indicated by the gradient slopes in the metal adjacent to the core surfaces. However, near the end of solidification the gradients are milder in the metal adjacent to the core than they are in the metal adjacent to the mold indicating a more rapid rate of heat flow into the mold.

The progression characteristics of the "end of freeze" curves from the core and mold surfaces are shown in Fig. 7. For comparison purposes similar data are presented for a 4-in. thick flat plate which may be considered as a bushing having an infinitely large core diameter and for a cylinder of 8-in. diam which may be considered as a bushing of infinitely small core diameter.

It is observed that the symmetrical solidification characteristics of the flat plate are not developed even in the bushing with the 16-in. diam core. The rate of wall growth from the subject core surface is markedly slower than from the mold surface with the result that the thermal center is displaced approximately 0.5 in. from the geometric center in the direc-

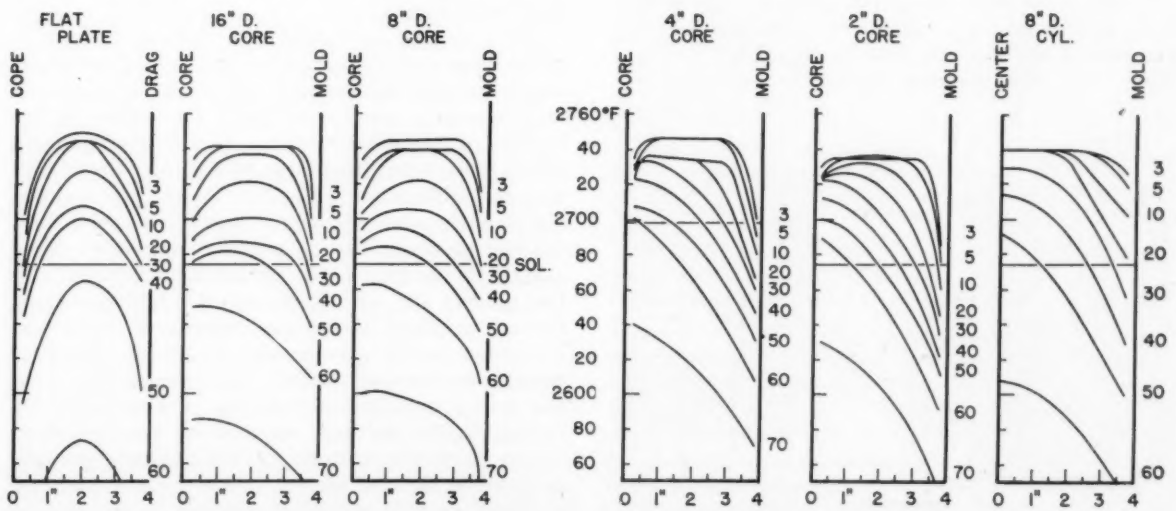


Fig. 6—Thermal Gradients Across 4-In. Thick Walls of Solidifying Bushings.

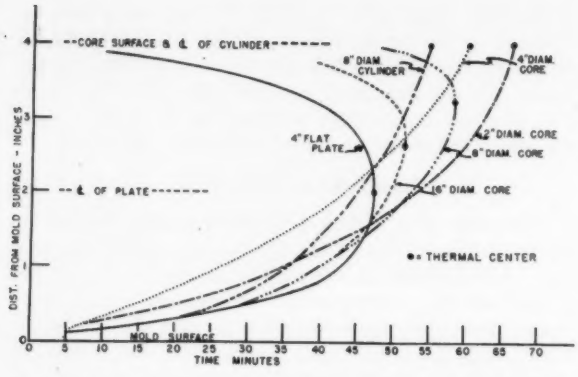


Fig. 7—Progression of "End of Freeze" Waves in Bushing Walls.

tion of the core. As the core diameter is decreased to 8 in. the rate of solidification from the mold surfaces increases while the rate from the core surfaces shows a further decrease. While a completely solid skin is not formed adjacent to the 8-in. diam core until approximately 50 min after pouring, heat is being continually extracted by the core during the entire solidification cycle. The effect of this heat transfer is to increase the solid to liquid ratio in the metal adjacent to the core as can be observed by the fact that the gradient curves for this casting, Fig. 6, are constantly approaching the solidus line. When a completely solid skin finally does form at the surface the conditions of solidification in the adjacent portions are sufficiently advanced to permit a rapid "sweep" of the "end of freeze" wave during the time of 50-58 min. The rapid sweep is characteristic of movement of "end of freeze" waves through regions in which very little liquid remains to be solidified. At this time the "end of freeze" waves from the mold surface also enters a period of fast "sweep" through similar nearly solidified regions. The opposite waves effect a junction at a position approximately ¾ in. from the core surface indicating a displacement of thermal center of 1¼ in. from geometric center.

In the case of the 4-in. and 2-in. diam cores the delay in developing a completely solid skin at the core surface is sufficiently long to permit the "end of freeze" wave from the mold surface to reach the core

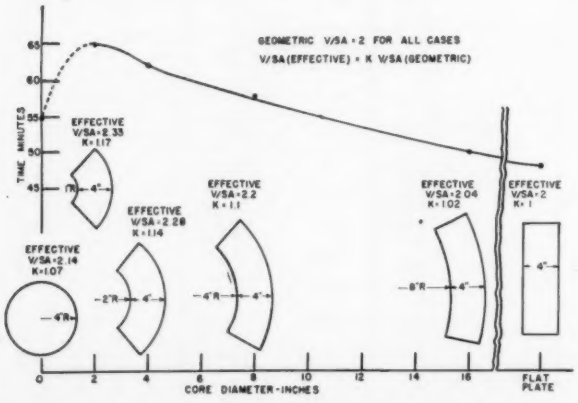


Fig. 8—Relation of Solidification Time to Core Diameter.

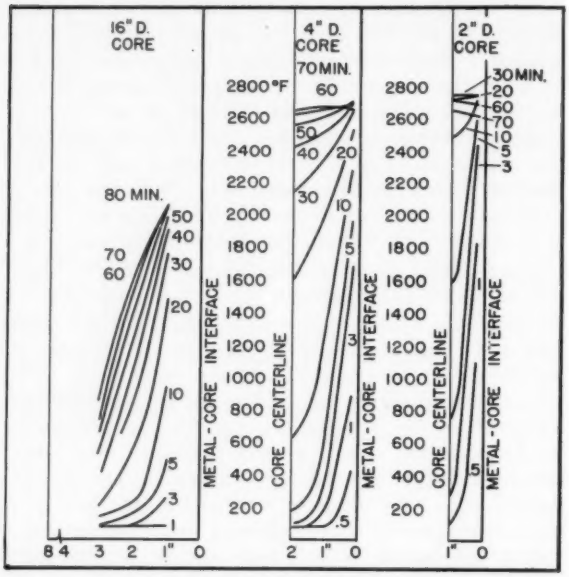


Fig. 9—Thermal Gradients Within Sand Cores During Solidification of the 4-In. Thick Steel Walls Surrounding Them.

surface. Accordingly, the position of final solidification is located at the core surface, representing a complete shift of the thermal center from the geometric center to the surface.

The "end of freeze" times at the thermal center positions for the various castings indicate the final times of solidification. The plot of final solidification times *vs* core diameter, Fig. 8, shows that the 16-in. diam bushing closely approaches the behavior of a flat plate. The difference of 8 pct in solidification time (48 min for plate, 52 min for 16-in. diam bushing) indicates that the more rapid rate of heat extraction from the mold surface does not completely offset the slower rate of heat extraction from the core. This effect is not due to insufficiency of sand in the core inasmuch as the center of the core was not heated appreciably above room temperature at the time of final solidification.

Effects related to insufficiency of sand are clearly evident for the case of the 4- and 2-in. diam cores, Fig. 9, as may be observed from the fact that the temperatures of near center positions of the core rise rapidly during solidification. The 4-in. diam core closely approaches heat saturation during the last 10 min of solidification; the 2-in. diam core becomes completely saturated approximately 10 min after pouring at which time it must be considered to be completely ineffective for further heat removal. In fact, during the last 15-20 min of solidification the thermal gradients in the 2-in. diam core slope from the center to the core surface indicating that heat which was absorbed earlier when the casting interface was at a higher temperature is returned to the casting. While the heat removed by the 2-in. diam core is insufficient to cause an "end of freeze" wave to originate at the core surface, it does increase the solid to liquid ratio in the metal adjacent to the core. It was calculated from the heat constants of the sand

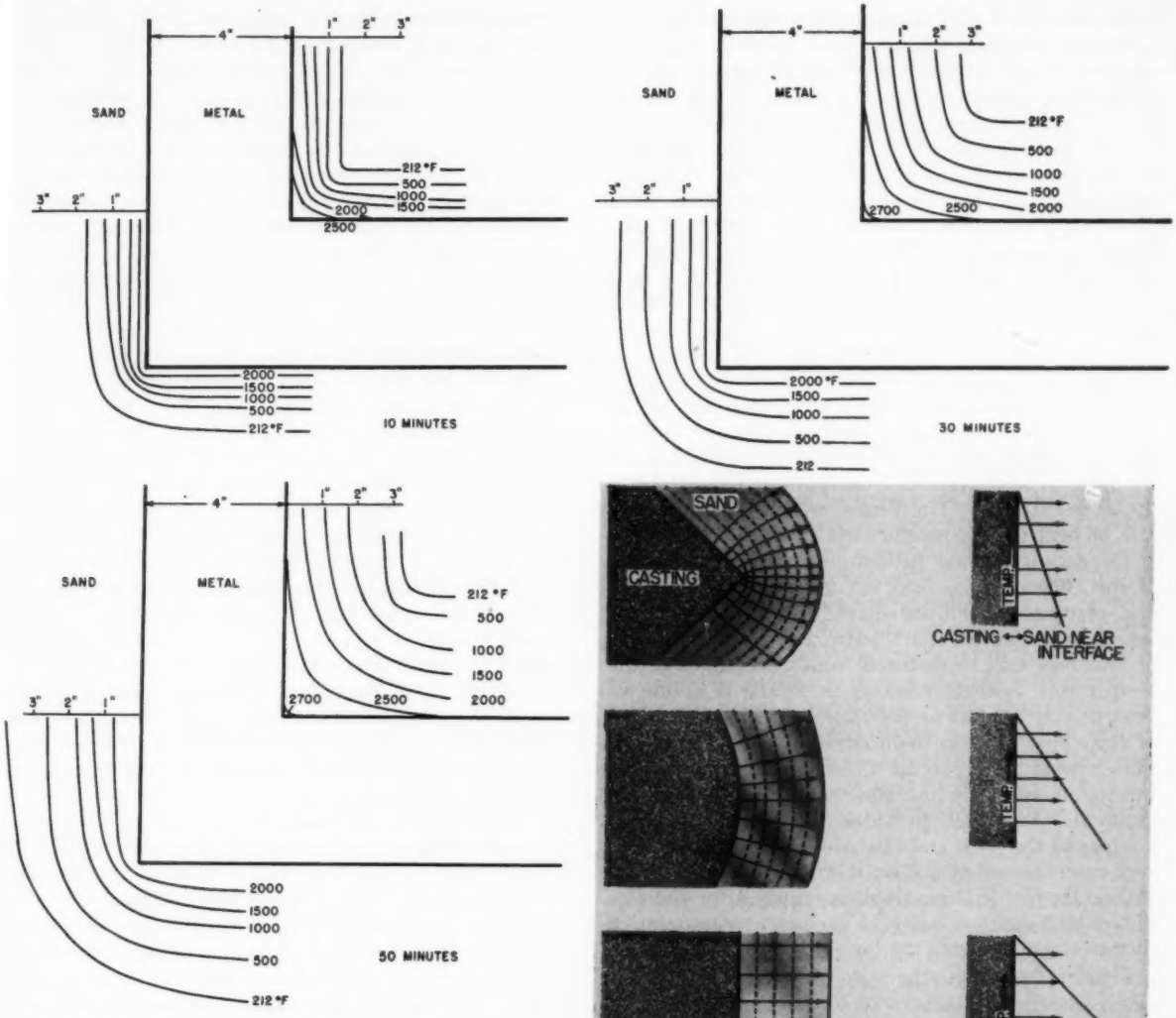
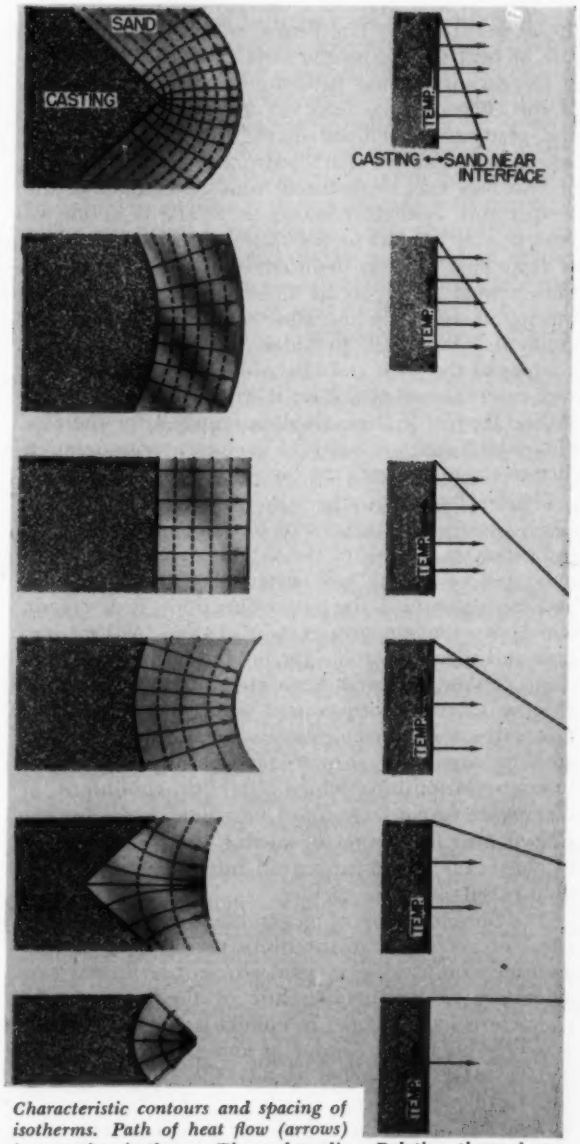


Fig. 10 —Isotherms in Sand Adjacent to Internal and External Corners During Solidification of "L" Section.

and metal that the heat absorbed by the core is sufficient to cause solidification of approximately 5 pct of the total liquid metal volume. Inasmuch as the core surface area comprises approximately 17 pct of the total surface area of the casting it is indicated that the effective surface area of the core is only 30 pct of the geometric surface area. Accordingly the mold must assume a greater heat burden to compensate for the inadequacy of the core, which is a major factor in prolonging the time of final solidification. Similar calculations for the system with the 4-in. diam core indicated that the effective surface area of the core is 57 pct of the geometric surface area.

The relationships plotted in Fig. 8 indicate that as the core diameter is decreased to zero value a reversal in the trends of solidification time is obtained. These data provide a basis for empirical adjustment of the V/SA relationships for calculation of freezing times.



Characteristic contours and spacing of isotherms. Path of heat flow (arrows) is normal to isotherms. Thermal gradient condition indicated by spacing of isotherms.

Relative thermal gradient and rates of heat flow (arrows).

Fig. 11—Schematic Representation of Heat Flow Characteristics Resulting from Variations in Casting Geometry.

If the effective V/SA ratio of a flat plate is taken as equivalent to the geometric ratio (V/SA = 2) the effective V/SA ratios of the various forms (all of which have geometric ratios V/SA = 2) are as indicated in the subject figure. As a practical approximation for calculation of freezing times, bushings having core diameters greater than twice the wall thickness (8 in.) may be considered to solidify essentially as a flat plate of equivalent wall thickness, while bushings of core diameters less than twice the wall thickness may be considered to solidify essentially as cylinders of radius equal to the wall thickness.

Heat Flow in Sand

The wide variations in rates of wall growth solidification which have been observed for the various types of casting surfaces indicate similar variations in the rates of heat transfer from the casting surface to the adjacent sand. The factors which determine the rate of heat passage to the sand from each unit area of casting surface may be deduced from consideration of the effects of geometry on the path of heat flow and gradients developed in the sand and from the known heat transfer characteristics of sand. The path of heat flow may be deduced from the contour of the temperature isotherms inasmuch as the direction of heat flow is normal to the isotherm lines. The rate of heat flow may be deduced from the steepness of the thermal gradients as indicated by the relative spacing of the isotherms; close spacing indicates steep gradients. Figure 10 provides the data required for analysis of the path and rate of heat flow for internal and external corners. The thermal gradients shown in Fig. 9 provide the only data required for the case of the bushings, inasmuch as geometric consideration dictate circular symmetry for isotherm contours.

Figure 11 presents schematic illustrations of the nature of isotherms, both as to contour and spacing, as indicated by analysis of the subject data for the various types of casting geometries. At external corners and convex surfaces the path of heat flow is divergent, which provides for greater area of sand for heat passage and greater volume for heat absorption at positions farther removed from the interface. For the case of internal corners and concave surfaces heat flow takes a converging path which provides for decreasing area and volume relationships. The geometric relationships which establish conditions of divergence or convergence of heat flow determine the rate of heat flow from the casting surfaces, inasmuch as the sand temperature gradients which result are established by these factors.

The development of generalized concepts of the effect of geometric relationships on the rate of heat transfer from casting to sand requires an initial consideration of the basic nature of the heat transfer characteristics of sand. In comparison to steel, sand is a very poor heat conductor and as a result the interface sand approaches closely the temperature of the casting interface. Effectively, this represents a "piling up" of heat at a point of abrupt change from high to low conductivity. It is also characteristic of sand that the conductivity increases rapidly with increasing temperature.[5,6] The increase is due to the fact that heat transfer at the higher temperatures is for the most part by radiation from surface to surface of the individual sand grains; the efficiency of such a form of heat transfer increases markedly as the temperature of the radiating surfaces increases. Thus, at positions near the casting interface the potential heat transfer capacity of the sand is considerably higher than that of the colder sand farther removed from the interface. For example, the conductivity of sand at near-casting temperatures may be effectively three times greater, depending on the type of sand, than that of the same sand at temperatures below the visible "red heat" range of 1000-1200 F. The relative heat transfer characteristics of the hot metal and sand accordingly may be in the ratio of 6-1, depending on the type of sand, rather than 18-1 as indicated by consideration of room-temperature thermal constants obtained from handbooks. The degree to which the "hot" conductivity of the interface sand may be utilized to transfer heat from the casting surface is determined by the heat flow patterns in the adjoining colder, lower conductivity sand. If an expanding volume of colder sand is provided, a greater amount of heat may be accepted for storage (specific heat) and for passage (increased path for heat flow) than for the case of a constricting volume of sand. Accordingly, steeper gradients will be established in the high conductivity layer with resulting greater rate of heat flow from the casting for the case of expanding volume.

The various geometric forms of casting surfaces shown in Fig. 11 are grouped in descending order of heat transfer characteristics. The most rapid rate of solidification is indicated for the external corner inasmuch as the greatest degree of heat flow divergency is established by such a geometric form; accordingly, the temperature gradients in the near-interface sand have the highest value thus resulting in the most rapid rate of heat transfer across the casting-sand interface. The slowest rate of solidification is indicated for the case of the small core inasmuch as the extreme convergency coupled with insufficient sand volume (for the wall thickness involved) results in heat saturation at very early times. After this condition is reached the temperature gradient condition becomes of zero value; at this stage the potential heat transfer properties of the hot sand cannot be utilized for the transfer of heat from the casting.

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USE OF STANDARDS IN CONTROLLING COSTS

By

Wm. Busby*

The purpose of this paper is to convey some idea of the tremendous cost reduction potential that the writer believes the development and use of standards in the foundry industry can high light and thus make possible. The writer uses the word "standards" here in its broadest sense. A standard is simply a means of measurement and thus a means of comparison.

The title of this paper, "Use of Standards in Controlling Costs," has been chosen reluctantly. Actually, the most elaborate standard costs installation does absolutely nothing about controlling costs. The plant manager, the superintendent and more important perhaps than these the line foremen are the ones who actually do or do not control costs. And how about that "control" costs? The writer heard cost accountants compare cost control with driving an automobile. The car starts for the side of the road they say and you *control* the car by turning it back into the traffic lane. Then as it tends to cross the center line you again control the car and again turn it into the lane. This technique implies keeping the car in line and by analogy keeping costs in line.

If you have thought very much about cost control, you are not interested in merely controlling costs as such. You want to eliminate them entirely or if that does not prove possible, you want to reduce them as much as you can. Because by that means and that means only, can we meet competition, put aside a little profit and by so doing raise the American standard of living.

Now that we examined the "in controlling costs" part of the title, let us now examine the "use of standards" part. Too often we tend to think of a standard in terms of something which is "*par* for the *course*," or perhaps in a high task standard, particularly the ultimate or irreducible minimum.

A prominent Certified Public Accountant described this as a "tendency to look upon standards, once set, as veritable anchors of absolute truth."

For a number of years, the writer was a member of a consulting management engineering firm engaged in installing management techniques in companies representing a number of different manufacturing industries. As an engineer, it was his privilege to work with a large number of these companies ranging from a manufacturer of poultry health remedies to a large motor car manufacturer. The most impressive thing to him was in each case, no matter how poor their operation might be in general, there was always one process or one procedure or one operation which was outstanding. It was beautiful in the way it operated and usually far surpassed in quality or low cost or effectiveness anything which the writer had seen before in that line or in other words, his personal "standard" for that particular type of operation or procedure.

Effect of Good Incentive System

There were many examples of this but the writer will mention only two. During the war the writer was sent to a gas mask manufacturing plant. When he first walked down the production lines, he was utterly amazed at the speed and skill with which the operations were performed. The writer had been led to believe by his experience that under a good incentive plan one might find an occasional gifted worker who could attain a performance level of 170 per cent of standard, or above. Imagine his surprise when he found that these girls were averaging 175 per cent to standard day after day on good, well maintained standards. This had been accomplished by an aggressive, smart and capable management through the use of well designed applicant testing technique and excellent training methods.

A large laundry in the middle west had not one, but 16, shirt finishers averaging 38 shirts per operator per hour with a high operator turning out up to 45 shirts per hour when the country wide average for that type of equipment was about 24 shirts. It had taken the superintendent over three years to attain that department average and you can bet he did not spend one minute of that time concerning himself that the usual time study standard for that operation was 24-25 shirts per hour. He was not controlling costs, he was reducing them by careful layout, work

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arrangement, and selection and training of employees.

Shortly after the writer became affiliated with his present company the plant installed an overhead sand system for squeezer molding. The resultant reduction of work of shoveling was to pay for the installation by allowing an increase in production of 30 per cent. Except for overhead sand, the layouts of the work station were to be essentially the same as before. Several months of study by the plant manager, the foundry superintendent, the industrial engineer and others resulted in an average increase in production of not 30 per cent but 76 per cent.

The writer knows of a foundry that reduced their man hours per ton over a three year period by exactly 50 per cent with little cost for expensive equipment. The author is sure they accomplished it by use of standards but not by standard that they looked at as veritable anchors of absolute truth.

The writer's purpose in citing these cases is two fold. They serve to point out and emphasize the following facts:

1. Effective cost control is cost reduction effected by smart management, and

2. Standards are essential not as a par but as a comparison to measure accomplishment made by management.

Now the author should like to discuss operations at his plant in using standards as a means of comparison in a cost control program. Before discussing this program itself, however, it would be well to briefly explain the type of problem we have at the plant. We are strictly a jobbing foundry producing malleable iron castings. There are approximately 350 hourly paid employees and our 1951 shipments amounted to 11,000 tons. There are about 100 different patterns run in the foundry each week and seldom is a pattern run for over a week. Since we are the only malleable jobbing plant south of St. Louis and east of California we supply many relatively small users of malleable castings. Only recently we ran several patterns for a customer who had not ordered for four years and who now has another four year's stock of castings. This is, of course, the exception but it does serve to illustrate the need on the part of all concerned to do their job right the first time around because there often is no second chance.

Direct Labor Costs

The first item to be included in a cost control program is that of direct labor and it is in this area that most of the standards work has been done in industry. Once methods, work place and materials have been standardized, it is a relatively easy matter to establish production standards on the operation involved. This we have done at our plant. We have elemental time study data on core making, molding and grinding and finishing operations which enables us to set production standards on virtually all jobs which are run. We have piece rate incentives based upon these production standards. Since most day work operations average about 75 per cent performance to standard and incentive workers perform at an average of 130 per cent, it is easy to see the cost reduction potential in an incentive application to the company and the 30 per cent increase in wages the worker receives as his share of the increase in production is beneficial to all concerned.

We issue a weekly direct labor control report which summarizes for the foreman the results of his operation. Table 1 illustrates this control report for our molding department for the week of July 21, 1952. In the first column on the left are the days of the week. The second column is for the total hours worked by all direct labor operators and the succeeding columns indicate just how those hours were used.

You will note that there are 178 hr of indirect labor and 131.92 hr of direct day work. We allow and pay all workers for a 20-min canteen period daily. This is included in the indirect labor hours. A large portion of the direct day work is made up

TABLE 1—DEPARTMENTAL CONTROL REPORT (MOLDING)

Period Ending Date 7-27-52	Total Hours	Day Work Hours Indirect	Day Work Hours Direct	On Standard Hours	On Standard % to Total	Standard Hours Earned	Performance to Standard
7-21-52	254.00	35.86	21.32	196.82	77.5	257.22	131%
7-22-52	274.20	42.42	46.20	185.58	67.7	265.81	143%
7-23-52	282.70	43.94	36.86	201.90	71.4	287.55	142%
7-24-52	261.30	47.76	27.54	186.00	71.2	276.85	149%
7-25-52	8.00	8.00	—0—	—0—	—0—	—0—	—0—
7-26-52	1.00	1.00	—0—	—0—	—0—	—0—	—0—
Total	1,081.20	178.98	131.92	770.30	71.2	1,087.43	141%

Allowances and Expenses

	Fall Down	Wait Material	Wait Repair	Wait Job	Canteen Allowance	Machine Change	Rework	Short Run	Faulty Process	All Others	Total	% to Standard
7-21-52	$—0—	$ 3.73	$ 8.44	$—0—	$—0—	$10.33	$—0—	$—0—	$ 2.75	$ 5.93	$31.18	7.9%
7-22-52	—0—	1.41	10.88	—0—	—0—	9.56	—0—	—0—	2.42	6.91	31.18	7.6%
7-23-52	—0—	1.75	5.72	—0—	—0—	17.24	—0—	—0—	4.55	22.08	51.34	11.6%
7-24-52	—0—	3.00	2.89	—0—	—0—	6.54	—0—	—0—	2.33	16.73	31.49	7.4%
7-25-52	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—
7-26-52	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—
	—0—	9.89	27.93	—0—	—0—	43.67	—0—	—0—	12.05	51.65	145.19	8.6%
Add Finishing Excess due to Bad Molds ($45.38).									45.38		45.38	2.7%
Add Finishing Excess due to Bad Fdry. Practice ($9.44)									9.44		9.44	.6%
	—0—	9.89	27.93	—0—	—0—	43.67	—0—	—0—	66.87	51.65	200.01	11.9%

by the time worked on sample and extremely short runs. The "on standard" columns show hours and the per cent of on standard hours to total hours. The higher this per cent, of course, the better the operation.

"Standard Hours Earned" column gives the computed hours of 100 per cent performance work turned out during the hours on standard and when divided by the hours on standard indicates the per cent performance to standard as shown in the last column. They key to low cost operation is, of course, the per cent of hours on standard and the per cent performance to standard.

The lower section of the report shows the cost in dollars of that time the direct labor operators spent at other than productive work such as "fall down," "wait material," "wait repair," etc. The last column indicates the per cent of this total controllable cost to the dollars spent for productive work or standard hours earned.

In addition to this control report the molding supervision is given a daily report covering all job changes and delays and they receive a weekly summary giving details of the control report showing each individual operator's daily as well as weekly performance.

In addition to direct labor the following operations are also on incentive based upon production standards:

1. Cupola charging
2. Spruing castings
3. Inspect, chip and pack castings
4. Graveling and mudding
5. Pot dumping and sorting
6. Shipping
7. Starlug assembly
8. Anchor assembly
9. Coke, sand, sacked material and brick unloading
10. Furnace tear out
11. Pouring castings
12. Shifting
13. Dumping castings

We are also now in the process of developing standards and incentives for cleaning castings and several other miscellaneous operations.

Indirect Labor Incentives

We have established production standards and resulting incentives on a goodly part of our indirect labor as well as direct labor and these standards have resulted in appreciable manufacturing cost reductions. There is one large area of potential cost reduction, however, which our standards and control reports do not measure. That is in the work method improvement field. For example, a molding foreman may add or eliminate a tuck or peen on a job and the standard will be changed to reflect the change which has increased or decreased our cost of manufacture. The control report will not in any way reflect this cost change. The hours on standard and the standard hours earned will be essentially the same.

Likewise a cracking strip may be added or removed in the pattern shop and the finishing departments cost will have gone up or down without their control report reflecting this change since again the standard will be changed when the grinding is changed.

This apparent and real lack of control was a problem for some time. Several years ago, however, we began searching for a means of controlling or measuring the effect of these types of cost changes as well as all others and which would clearly indicate to the man who could control these costs just what was happening. Our search resulted in establishing a cost control report which we call a Performance Report.

This report is in its second year now and has changed considerably over that period of time. Undoubtedly it will change for the better in the future but we believe it is a definite answer to our own particular problem in the way of cost control.

All figures are in dollars and at the present time we are covering 85 per cent of our total manufacturing costs in these reports. The balance are overhead costs over which the superintendents and foremen can exercise no control. They cover the operation of 21 departments and indicate cost reductions or increases in direct labor, indirect labor, materials and supplies, scrap and gating yield.

It is an adaptation of the flexible budget technique and since we are particularly anxious to encourage cost reduction, through better methods, we feel it is impossible to establish standards which can be used as a par or high task goal since the opportunities for improvement are literally unlimited. Therefore, we have established standards or bases as we call them, based upon past performance applied to the current operating conditions.

Perhaps it would be helpful to show some examples of how we develop these standards or bases. The performance reports cover approximately 260 cost accounts. These costs vary from a straight fixed to a straight variable in nature. Most of them, of course, act as a combination of fixed and variable. By this classification the author is not necessarily referring to the accountants definition of fixed and variable accounts. Most accountants will classify a direct

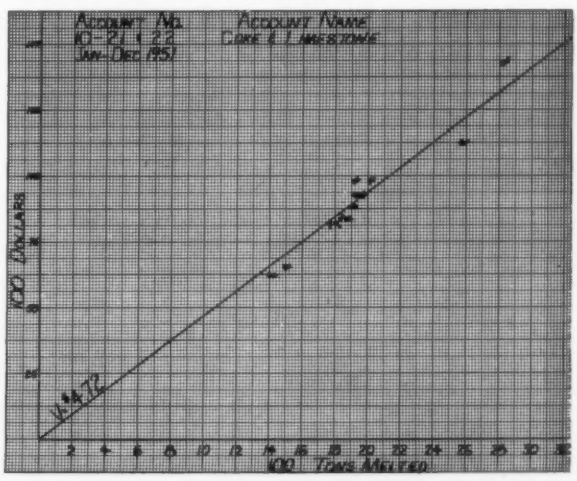


Fig. 1

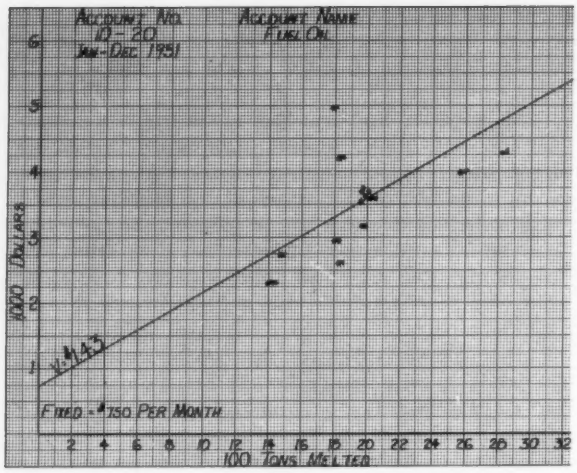


Fig. 2

labor account in which the operations are on a one for one incentive such as a standard hour or piece work plan as a variable account. This is not true in 99 cases out of a 100, however. It will act primarily as a variable but it is often surprising just how much fixed cost is involved. In our Molding Department direct labor control report, because of the jobbing nature of our business, there was a considerable amount of labor charged to job changes. This, as most accountants will agree, is a direct labor charge and varies only with the number of changes made and not with production. All or a good portion of this job change cost will act as a fixed charge.

The importance of an understanding of the nature of fixed and variable costs on the part of management cannot be over emphasized. C. S. Carney of Stevenson, Jordan and Harrison, as consultant's consultant, so to speak, tells of many actual experiences in which he has encountered the tendency for Management to look upon cost control as a division of accounting instead of as an integral part of Management. One of his stories goes something like this:

"A salesman was making one of his routine callbacks to see how the foreman liked the machine which he had sold and delivered to him recently. He was astonished when he discovered that the work was being done on the old machine and the new machine was standing idle. He said, "Joe, why aren't you using the new machine?" Joe said, "Well, you see, it costs more to make it on the new machine than it does on the old machine, because the old machine is fully depreciated. We do not have any depreciation charge on that, but the new machine has a high depreciation, so we make all we can on the old machine."

A little over a year ago we instituted a quality control program at the author's plant. Everyone concerned has done a good job of making it operate and as a result our total scrap has been lowered by almost 3 per cent from the time of its inception. The question came up as to how much savings we are making. Our Accounting Department might have come up with some figures but the writer doubts if they would have been too accurate. From our flexible analysis charts, however, we know that we have an actual variable or out of pocket cost of $190 per ton. Since we are not operating at capacity this figure less the scrap value times the tons saved gives us the answer which is almost $60,000 per year.

The first analysis chart is of a Melting Department material account "Coke and Limestone." See Fig. 1. The ordinate or vertical line indicates dollars and the abscissa or horizontal scale shows tons melted. Our present base period is the year 1951 as so indicated on the charts. On this and other charts are plotted the costs by months against the tons melted. You can see that this results in a straight variable of $4.72 per ton.

Figure 2 is also a material account "Fuel Oil." This is the cost of fuel oil burned in our air furnace in which we condition our iron and hold temperature for pouring. You will note that the first, second and third month costs are high but on the way down and that the balance of the year the costs have leveled out resulting in a lower variable and perhaps a slightly lower fixed. The fixed costs are $750 per month and the variable cost is $1.43 per ton. The Melting Department supervision has done an outstanding job in using their performance reports and in actually reducing costs of operations. It is not just by chance that the last seven out of eight months figures show a reduced cost. The savings resulted from a careful study resulting in a decrease in the size of the combustion chamber and close and careful burner control.

The entire Melting Department costs perform as shown on Fig. 3. Again as in the case of fuel oil costs you will note that the entire department costs were reduced throughout the year and particularly in the first six months. If the last eight months only were considered there would result a definite reduction in the variable or slope of the line with about the same fixed of $6,880 per month. The average variable line is set at $33.30 per ton. The last eight months costs were definitely in control in spite of a fairly wide variance in volume or tons melted.

Each account was analyzed in the same way. Some accounts are rather difficult to analyze and draw a representative line. In a few of these more difficult ones we have arbitrarily determined the line after a

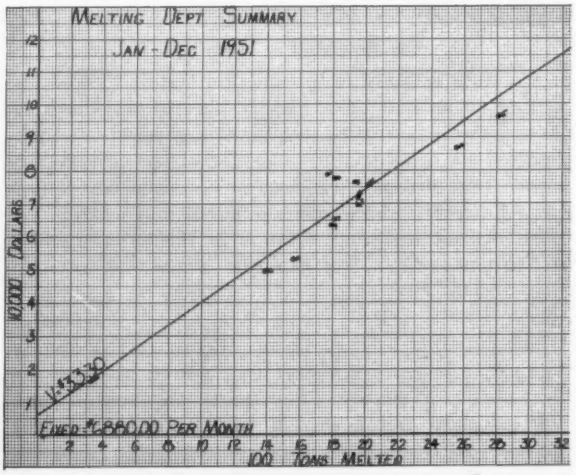


Fig. 3

TABLE 2—MELTING DEPARTMENT—PERFORMANCE REPORT

Tons Melted 5827.89, Base Total Scrap 10.22, Actual Total Scrap 7.42

Metallurgist..... Foreman..... 2nd Quarter Week Ending 6-29-52

Acct. No.	Account Name	Base Labor Std. Mat'l.	Actual Labor Std. Mat'l.	(Over) Under	Actual Mat'l. Cost	Price Variance
	Labor					
10-100	Scrap Prep.	$ 2,564.00	$ 2,717.01	$ (153.01) *	$	$
10-105	Unload Steel					
10-200	Cupola Air	6,647.00	5,279.65	1,367.35		
& 250	Furnace Oper.					
10-240	Pour. Alum. Pellets	28.96	28.96	—0—		
10-280	Allowances	117.00	123.67	(6.67)		
10-281	Meetings & Appr.	—0—	138.42	(138.42)		
	Training					
10-285	Canteen	772.00	653.89	118.11		
10-300	Overtime Prem.	1,011.00	1,260.36	(249.36)		
10-400	Super & Clerks	5,447.00	4,458.76	988.24		
10-500	Cupola, Air	3,775.00	4,031.03	(256.03)		
& 510	Furnace Repairs					
10-520	Mech Repairs	1,206.00	1,513.72	(307.72)		
& 530						
	Total Labor	21,567.96	20,205.47	1,362.49		
	Supplies					
10-10,11	All Metals	136,200.00	131,588.26	4,611.74		(8,552.73)
12 & 13						
10-20	Fuel Oil	10,583.00	10,210.20	372.80		(852.04)
10-21	Coke, Limestone	27,508.00	25,201.15	2,306.85		632.52
& 22	& Flux					
10-600	Refractories	13,269.00	13,416.14	(147.14)		(450.96)
& 610						
10-620	Mech Repairs	843.00	1,382.18	(539.18)		(7.97)
& 630						
10-700	Misc. Supplies	932.00	1,480.57	(548.57)		(1.39)
10-730	Misc. Cupola &					
	Air Furnace	2,215.00	787.54	1,427.46		(2.35)
	Total Supplies	191,550.00	184,066.04	7,483.96	193,300.96	(9,234.92)
	Total Labor & Supplies	213,117.96	204,271.51	8,846.45		
	Scrap Adj.	6,209.00				
	Total Adj.			6,209.00		
	Labor & Supplies	219,326.96	204,271.51	15,055.45		
	Net Yield Adj.	(5,900.00)		(5,900.00)		
	Net Labor & Supplies	213,426.96	204,271.51	9,155.15		

Per cent Yield 47.6
Est. Tons Salable Castings 2,726.48
Cost per ton Est. Salable Castings $78.31

*Figures in ( ) are over.

thorough discussion with the foreman and supervisor concerned.

After the standards have all been determined by charting we are then ready to use these standards as comparisons with actual costs of operation. This is done on the performance reports themselves.

Before showing you samples of the reports I must point out some of the steps that we had to take before our program could function effectively:

1. It was necessary to assign definite responsibilities for all costs and furthermore to be sure that the supervisor concerned recognized this responsibility.

2. We had to make the supervisors realize that we were not setting a par for his operation but were literally only showing him that if he were operating this year under this year's conditions as efficiently and effectively and no more so than as he did last year his costs would be at our standard or base figure. If he improved his operations they would be lower and the actual dollars difference between base and actual costs represents the savings he has achieved.

3. The foreman was held responsible for normal maintenance costs. Unusual maintenance is defined as any maintenance job costing over $100 and occuring according to the plant engineering records less frequently than once per year. Unusual maintenance costs are pulled out of the departmental account by means of budget orders and shown on the general factory report, where they are also compared with a standard. It has been advisable to institute a repair and equipment request procedure whereby the foreman actually authorized repair work for his department in order to control those repair costs.

4. The plant engineer is also held responsible for overall plant repairs and plant engineering costs including both normal and unusual or budget order repairs. This is consistent with the fact that both the foreman and the plant engineer actually do and should control repair costs.

5. Those management staff men who can control scrap receive recognition of this by receiving credit on their departmental performance reports for the difference in their departmental standard cost based upon the 1951 scrap record and their actual costs.

TABLE 3—MOLDING DEPARTMENT—PERFORMANCE REPORT

2nd Quarter

Supt..... Foreman.....

Week Ending 6-29-52

Tons Melted 5827.89, Avg. Wt. of Cstg. 2.7, Base Total Scrap 10.22%, Actual Total Scrap 7.42%.

Acct. No.	Account Name	Base Labor Std. Mat'l.	Actual Labor Std. Mat'l.	(Over) Under	Actual Mat'l. Cost	Price Variance
	Labor					
20-100 to 119	Direct	$ 30,824.00	$ 29,019.52	$ 1,004.48	$	$
20-200 to 218	Pouring, Shifting & Dumping	31,513.00	29,133.11	2,379.89		
20-240	Bottom Boards	346.83	346.83	—0—		
20-250	Core Changes	117.00	205.50	(88.50) *		
20-260	Moving & Fitting Flasks	3,945.00	4,221.94	(276.94)		
20-265	Night Clean Up	3,744.00	3,119.44	624.56		
20-270	Miscellaneous	6,465.00	4,676.67	1,788.33		
20-280	Allowances	3,613.00	3,966.86	(353.86)		
20-281	Meetings & Appr. Training	—0—	385.35	(385.35)		
20-285	Canteen Time	3,322.00	3,139.18	182.82		
20-300	Overtime Prem.	2,215.00	1,529.12	685.88		
20-400	Super. & Clerks	9,100.00	9,488.43	(388.43)		
20-500 to 510	Repairs	5,373.00	4,614.97	758.03		
	Total Labor	100,577.83	93,846.92	6,730.91		
	Supplies					
20-600 to 610	Repairs	3,322.00	3,973.90	(651.90)		(3.58)
20-650 & 660	Woods, Nails, Jackets, Flasks	3,905.00	5,180.76	(1,275.76)		181.01
20-700	Loose & Hand Tools	3,905.00	3,266.48	638.52		91.29
20-710	Core Charges	117.00	193.78	(76.78)		4.87
	Total Supplies	11,249.00	12,614.92	(1,365.92)	12,341.33	273.59
	Total Labor & Supplies	111,826.83	106,461.84	5,364.99		
	Scrap Adj.	2,727.00		2,727.00		
	Total Adj. Labor and Supplies	114,553.83	$106,461.84	8,091.99		
	Net Yield Adj.	(2,591.00)		(2,591.00)		
	Net Labor & Supplies	$111,962.83	106,461.84	$ 5,500.99		

Per cent Yield 47.6
Est. Tons Salable Cstgs. 2726.48.
Cost Per Ton Est. Salable Cstgs. $38.95.

*Figures in ( ) are over.

TABLE 4—SHIPPING DEPARTMENT—PERFORMANCE REPORT

2nd Quarter

Foreman..... Week Ending 6-29-52

Tons Shipped 2825.71

Acct. No.	Account Name	Base Labor Std. Mat'l.	Actual Labor Std. Mat'l.	(Over) Under	Actual Mat'l. Cost	Price Variance
	Labor					
90-200	Truck, Weigh & Load	$ 2,458.00	$ 2,158.57	$ 299.43	$	$
90-280	Allowances	57.00	51.36	5.64		
90-281	Meetings & Appr. Training	—0—	—0—	—0—		
90-285	Canteen Time	124.00	97.82	26.18		
90-300	Overtime Prem.	141.00	53.60	87.40		
90-400	Super. & Clerks	6,110.00	5,520.00	590.00		
90-500	Mech. Repairs	113.00	136.70	(23.70) *		
	Total Labor	9,003.00	8,018.05	984.95		
	Supplies					
90-600	Mech. Repairs	85.00	104.53	(19.53)		—0—
90-700	Misc.	28.00	22.98	5.02		.04
90-710	Bags, Wire, etc.	3,052.00	2,614.63	437.37		137.83
	Total Supplies	3,165.00	2,742.14	422.86	262.84	137.87
	Total Labor & Supplies	$12,168.00	$10,760.19	$1,407.81		

Per Cent Yield 47.6.
Est. Tons Salable Cstgs. 2726.48
Cost Per Ton Est. Salable Cstgs. $3.90

*Figures in ( ) are over.

This may, of course, be a gain or a loss.

6. The gating yield which with the total scrap determines the overall yield is charged as a responsibility of the chief inspector, the foundry superintendent and the pattern shop superintendent. It can be noted from our reports that so far some of our scrap gains have been offset by a decrease in gating yield.

7. As the writer have pointed out, one of our larg-

TABLE 5—YARD DEPARTMENT—PERFORMANCE REPORT

2nd Quarter

Supt..... Foreman.....

Week Ending 6-29-52

Tons Melted 5827.89

Acct. No.	Account Name	Base Labor Std. Mat'l.	Actual Labor Std. Mat'l.	(Over) Under	Actual Mat'l. Cost	Price Variance
	Labor					
140-215 to 250	Unloading Materials	$ 2,448.00	$1,702.89	$ 745.11	$	
140-255	Miscellaneous	1,070.00	810.62	259.38		
140-270	Road Repairs	39.00	23.47	15.53		
140-280	Allowances	58.00	—0—	58.00		
140-281	Meetings & Appr. Training	—0—	—0—	—0—		
140-285	Canteen Time	506.00	308.87	197.13		
140-300	Overtime Prem.	641.00	210.91	430.09		
140-400	Superv. & Clerks	2,535.00	2,102.88	432.12		
	Yard Trucks	1,298.00	1,353.22	(55.32) *		
	Yd. Trk. Repairs	117.00	37.62	79.38		
	Total Labor	8,712.00	6,550.58	2,161.42		
	Supplies					
	Yard Trucks	350.00	200.46	149.54		—0—
	Yard Truck Repairs	208.00	174.99	33.01		(.05)
	Total Supplies	558.00	375.45	182.55	659.38	(.05)
	Total Labor & Supplies	9,270.00	6,926.03	2,343.97		
140-275	Storeroom	728.00	710.50	17.50		
	Grand Total	$9,998.00	$7,636.53	$2,361.47		

Per cent Yield 47.6.
Est. Tons Salable Cstgs. 2,726.48
Cost Per Ton Est. Salable Cstgs. $2.80

*Figures in ( ) are over.

TABLE 6—PLANT ENGINEERING DEPARTMENT—SUMMARY REPORT

2nd Quarter

Plant Engineer..... Foreman.....

Week Ending 6-29-52

Department	Labor Base	Labor Actual	Labor Over-Under	Supplies Base	Supplies Std. Actual	Supplies Over-Under	Total Labor & Supplies Act'l Labor Base	Act'l Labor Std. Mat'l.	Over-Under
Melting	$ 1,206.00	$ 1,513.72	$ (307.72) *	$ 843.00	$ 1,382.18	$ (539.18)	$ 2,049.00	$ 2,895.90	$ (846.90)
Laboratory	117.00	41.94	75.06	182.00	73.68	108.32	299.00	115.62	183.38
Sand	3,030.00	3,026.59	3.41	3,613.00	4,865.89	(1,252.89)	6,643.00	7,892.48	(1,249.48)
Molding	5,373.00	4,614.97	758.03	3,322.00	3,973.90	(651.90)	8,695.00	8,588.87	106.13
Core Room	415.00	854.31	(439.31)	178.00	492.31	(314.31)	593.00	1,346.62	(753.62)
Hard Iron	382.00	333.70	48.30	424.00	485.91	(61.91)	806.00	819.61	(13.61)
Annealing	1,052.00	1,353.57	(301.57)	1,127.00	2,486.62	(1,359.62)	2,179.00	3,840.19	(1,661.19)
Soft Iron	852.00	503.87	348.13	1,485.00	584.52	900.48	2,337.00	1,088.39	1,248.61
Finishing	1,494.00	1,597.92	(85.92)	1,485.00	2,059.70	(610.70)	2,979.00	3,675.62	(696.62)
Inspection	137.00	274.78	(137.78)	55.00	139.27	(84.27)	192.00	414.05	(222.05)
Shipping	113.00	136.70	(23.70)	85.00	104.53	(19.53)	198.00	241.23	(43.23)
Maintenance	422.00	495.57	(73.57)	838.00	536.94	301.06	1,260.00	1,032.51	227.49
Pattern	52.00	107.00	(55.00)	104.00	26.39	77.61	156.00	133.39	22.61
Yd. & Yd. Trucks	156.00	61.09	94.91	208.00	174.99	33.01	364.00	236.08	127.92
Starlug	91.00	2.09	88.91	13.00	(77.50)	90.50	104.00	(75.41)	179.41
Permasteel	81.00	109.81	(28.81)	410.00	85.91	324.09	491.00	195.72	295.28
Alloy Iron	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—	—0—
General & Ex. W.O.	11,706.00	6,798.85	4,907.15	15,509.00	9,542.94	5,966.06	27,215.00	16,341.79	10,873.21
Total Depts.	26,679.00	21,808.48	4,870.52	29,881.00	26,974.18	2,906.82	56,560.00	48,782.66	7,777.34
Cap. W.O.	4,110.00	242.06	3,867.94	17,069.00	6,477.45	10,591.55	21,179.00	6,719.51	14,459.49
Total All.	$30,789.00	$22,050.54	$8,738.46	$46,950.00	$33,451.63	$13,498.37	$77,739.00	$55,502.17	$22,236.83

*Figures in ( ) are over.

est potential cost reductions is in the field of method improvement. Starting in 1953 we are going to show as a separate charge on our performance report:

a. A portion of the original equipment cost based upon the expected life of the installation.

b. Interest on the investment.

c. A portion of the replacement cost based upon the expected life of the installation.

8. It was necessary to freeze or standarize material prices as of a particular time in order to have dollar costs represent actual usage. However, a foreman or superintendent may specify a different material as a result of his own investigation and when approved, a change will be made in the standard cost so that the resulting cost reduction will be reflected in the performance report. An example of this might be a special shot to be used in cleaning machines or a lower cost pattern spray which would result in a sav-

TABLE 7—PERFORMANCE SUMMARY
Week Ended 6-29-52
Second Quarter

Dept.	Base Labor Std. Mat'l	Actual Labor Std. Mat'l.	(Over) Under	Scrap Adj.	(Over) Under	Per Cent	Yield Adj.
Melting	$213,117.96	$204,271.51	$8,846.45	$ 6,209.00	$15,055.45	7.06	($5,900.00) *
Lab	5,640.00	4,354.19	1,285.81	77.00	1,362.81	24.16	(74.00)
Sand	30,394.00	30,837.59	(443.49)	770.00	326.41	1.07	(732.00)
Alloy	16,479.24	13,722.46	2,756.78	—0—	2,756.78	16.73	—0—
Total	265,631.20	253,185.75	12,445.45	7,056.00	19,501.45	7.34	(6,706.00)
Molding	111,826.83	106,461.84	5,364.99	2,727.00	8,091.99	7.24	(2,591.00)
Core	33,179.00	32,253.43	925.57	—0—	925.57	2.79	—0—
Total	145,005.83	138,715.27	6,290.56	2,727.00	9,017.56	6.22	(2,591.00)
H. Iron	17,293.00	15,737.98	1,555.02	470.00	2,025.02	11.71	(447.00)
Anneal	44,102.24	39,654.10	4,448.14	1,317.00	5,765.14	13.07	—0—
Total	61,395.24	55,392.08	6,003.16	1,787.00	7,790.16	12.69	(447.00)
S. Iron	6,709.00	5,415.70	1,293.30	218.00	1,511.30	22.53	—0—
Starlug	24,142.70	23,934.66	208.04	—0—	208.04	.86	—0—
P. Steel	2,176.14	1,944.42	231.72	—0—	231.72	10.65	—0—
Total	33,027.84	31,294.78	1,733.06	218.00	1,951.06	5.91	—0—
Finish	53,484.00	57,886.40	(4,402.40)	1,352.00	(3,050.40)	(5.70)	—0—
Total	147,907.08	144,573.26	3,333.82	3,357.00	6,690.82	4.52	—0—
Inspect	17,397.00	16,371.03	1,025.97	336.00	1,361.97	7.83	—0—
Ship	12,168.00	10,760.19	1,407.81	—0—	1,407.81	11.57	—0—
Personnel	20,401.72	20,194.90	206.82	—0—	206.82	1.01	—0—
Canteen	12,491.00	11,195.94	1,295.06	—0—	1,295.06	10.37	—0—
Total	32,892.72	31,390.84	1,501.88	—0—	1,501.88	4.57	—0—
Yard	9,998.00	7,636.53	2,361.47	—0—	2,361.47	23.62	—0—
Ind. Eng.	15,678.34	13,220.51	2,457.83	—0—	2,457.83	15.68	—0—
General	47,680.00	39,124.59	8,555.41		8,555.41	17.94	—0—
Pattern	7,663.00	9,642.29	(1,979.29)	—0—	(1,979.29)	(25.83)	—0—
N. Yield	47.5%	47.5%	(506.00)	—0—	(506.00)	.21	—0—
Total			(1,473.29)		(1,473.29)		—0—
Maint.	9,586.00	11,622.61	(2,036.61)	—0—	(2,036.61)	(21.25)	—0—
Rep. Sum	(55,300.00)	48,782.66	6,517.34	—0—	6,517.34	11.78	—0—
Total	(64,886.00)	(60,405.27)	(4,480.73)	—0—	(4,480.73)	6.91	—0—
T. Plant	$711,607.17	$676,242.87	$35,364.30	$13,476.00	$48,840.30	$6.86	($9,744.00)
Per Cent	100.00	95.03	4.97	1.89	6.86		(1.37)

Total Plant Savings = $48,840.30 — ($9,774.00) = $39,096.30
$39,096.30 + $711,607.17 = 5.49%

*Figures in ( ) are over.

ings. In either of these cases the reports would reflect the results of the new material usage as to cost reduction.

9. All of our jobs both hourly paid and salaried are covered by job evaluation and each has a rate range. Supervision and clerical costs are included in the performance reports since these are controllable. The standards are adjusted to cover general plant raises only. Individual merit raises within these rate ranges are not considered since raises are given for performance the effect of which should be reflected in departmental cost reductions.

These performance reports are issued weekly, monthly and quarterly and each report is discussed in detail in a weekly meeting held by each superintendent with his foremen. All material costs are substantiated with stores issues copies of which each foreman receives for each account he controls. These issues show quantity, item, unit price, total cost, account charged and signature of persons receiving the material. This assures accuracy in accounting since these are gone over with a fine tooth comb by each foreman.

With this background we are now ready to look at representative samples of the performance report itself. The first of these is that of the Melting Department. See Table 2. Table 2 you will note covers the second quarter of 1952 and the heading shows the tons melted, base total scrap (or 1951 scrap) and actual total scrap. The first figures are the account numbers, 10-100 and 10-105, scrap preparation and unload steel. The top section of the report covers labor costs only and the first column is base labor cost. The $2564 is the standard cost under 1951 operations at the current tonnage. The next column indicates an actual cost of $2,717.01 resulting in an over expenditure of $153.01 for the quarter. The next account "cupola and air furnace operation" shows a savings of $1,367.35. All the labor accounts in the department are thus itemized and compared to base or standard cost. Incidentally, the allowances account 10-280 includes the cost of not only any down time for the cupola charging crew which is on incentive but the bulk of this cost is made up of charge backs from the Molding Department labor due to waiting for metal.

Just why some of the accounts are sizably in the black and some are in the red? This is, of course, the actual cost control part of the job. The report only indicates the results.

The supply or material accounts are listed in the lower section. The Standards or bases are in stand-

ard dollars or at prices frozen as of January, 1952. The last column shows the price variance of actual costs to actual standard costs. Price increases since January have resulted in a cost of $9,234.92 on materials used for this quarter. Our Purchasing Department is interested in these figures and in some cases can effect savings in this area. The totals are shown at the bottom of Table 2. Total labor and supplies usage indicates a saving of $8,846.45. The line directly below is the "scrap adjustment" and indicates a saving of $6,209.00. This literally means that if we had operated at the 1951 scrap level of 10.22 for this quarter, we would have spent this additional $6,209.00 or our scrap reduction resulted in a savings of $6,209.00. This brings the total savings to $15,055.45.

The next line entitled "Net Yield Adjustment" indicates the effect of the gating yield. While the scrap reduction resulted in a savings of $6,209.00, the total yield was not increased accordingly because the gating was such as to, in itself, offset $5900 of this savings. The net departmental savings is, therefore, $9,155.45. This gating responsibility is not that of the foreman of the Melting Department and is shown separately.

Table 3 shows figures for the Molding Department. The heading is the same as that for Melting except that in addition the average casting weight is given. Several accounts vary by casting weight as well as tonnage. The form is the same for all departments with the totals at the bottom of the sheet.

The Shipping Department operation is covered by Table 4. The volume measure is tons shipped. In this case this is shown to be 2825.71 tons. You will note that this department shows a sizable per cent savings. This is the result of considerable work and resulting changes in departmental operation.

Table 5 is for our Yard Department operations which is comparable to the Shipping Department report in that it is a relatively small operation but shows a large savings. Labor savings are primarily the result of standardizing unloading and other operations and establishing incentives upon these standards. This was completed in 1951 so that these figures do not show all of the savings actually made.

You will note that in both Table 5 and Table 4 no scrap or yield adjustment is shown. The foremen of these departments have no direct responsibility for scrap or yield as such and so the performance reports do not reflect these costs.

The Storeroom account at the bottom (Table 5) is an isolated labor account separate from the Yard operation but a responsibility of the same supervision.

You will remember the writer mentioned the split responsibility of the foremen and the plant engineer for repairs and plant engineering costs. Table 6 is a summary report showing all those accounts over which the plant engineer has control. All of the repair accounts are shown on individual department reports as well. You can see that this gives a good picture of the plant department operations. Labor and material on jobs to be capitalized is shown since capital charges are, of course, not included in our reports except in this summary (Table 6).

Table 7 is also a summary report. This summarizes the entire plant performance against standard. The subtotals are shown so that each superintendent has a total of the departments which he supervises as well as the individual reports for each of these departments. The figures indicate totals only and after scrap adjustment gain or loss is included, a per cent gain or loss is also shown under the column entitled "Per cent."

Not far from the bottom of Table 7 is included a total yield figure shown in per cent and with the calculated difference in dollars indicated. All 21 departments are included as shown. At the bottom of Table 7 will be found the plant totals and per cent figures. The overall plant savings for the quarter is shown at the bottom to be $39,096.30 or 5.49 per cent of the total expense.

As the author mentioned, we feel these control reports give us the answers we need for the Management staff to compare our current operation with standard. It is certainly not perfect and not yet in its final state. We will make improvements as we use them.

We are at the present time working on a method to include in this report in dollars, if possible, the effect of schedule performance on the operation. Schedule performance is the only important measure of the foreman's job which we do not now include.

We also wish to work out such a report for the measurement of our office work although it is going to be difficult. As the industrial engineer quickly concludes, however, the more complex the problem, the more the potential cost reduction.

DISCUSSION

Chairman: R. L. LEE, Grede Foundries, Inc., Milwaukee.

Co-Chairman: C. E. WESTOVER, Westover Engineers, Milwaukee.

G. H. ROCKWELL: [1] Don't you have a melting loss? How is it correlated in your Standards? How were your Standards developed?

MR. BUSBY: Yes, we do have a melting loss, of course. This loss is included in the Standard in the yield adjustment factor. This yield adjustment includes the effect of gating, melting loss, spills, etc., in other words everything other than actual hard iron, soft iron and returns from customer's scrap. The Standards were developed in all cases by analysis of the past years performance by means of flexible budget technique as explained in the talk. This means, of course, that our Standards are merely measures of the performance of the last year as applied to present period conditions.

C. N. OUTMAN: [2] What adjustment in standard do you make for variation in volume of production?

MR. BUSBY: Variation in volume of production is accounted for in the Standards in the very nature of establishing those Standards. Flexible budget type of analysis results in a fixed and a variable cost which when applied to the actual volume, results in a Standard for that particular volume. We do, however, limit our Standard application to a plus or minus 25 per cent variation from actual previous years volume.

H. R. WILLIAMS: [3] You show that average weight per casting is a factor considered in your molding analysis. Would not average weight *per mold* be a more pertinent figure for this analysis?

MR. BUSBY: We obtain a very good correlation on direct and indirect molding labor against average weight per casting.

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[1] Foundry Planning Supt., Caterpillar Tractor Co., Peoria, Ill.
[2] Comptroller, Hansell-Elcock Co., Chicago.
[3] Williams Management Engineering Co., Milwaukee.

However, we might obtain even better correlation if we used the average weight per mold as suggested.

WM. PITTS: [4] What department prepares and keeps these data up to date in forms available to the departments affected?

MR. BUSBY: The Accounting Department prepares the reports for the departmental supervisors and at the present time this requires the time of approximately 1¼ clerks.

A. R. KRAEGEL: [5] Am I correct in concluding that the main purpose of your Standards is to have a basis of comparison between prior years' performance and current operations? If this assumption is correct, do you make any adjustments in your standards for a poor showing in specific elements of cost in prior years? If not, do you then show a favorable variance in the current year, even though you have not yet reached the desired performance?

MR. BUSBY: You are correct in concluding that the main purpose of our Standards is to have a basis of comparison between the prior years performance and current operations. We do not make any adjustment in Standards for a poor showing in specific elements of cost in prior years because of the fact that we do not feel capable of determining what is a desirable performance particularly since the cost reduction plan is directed toward methods improvement and we feel the possibilities in this field are virtually unlimited. It is true, however, that in some cases a poor performance in a previous year may be improved upon sizeably in one department with less effort than that necessary in the case of a department which has functioned previously at a higher level of efficiency and effectiveness.

FRED BOENEKER: [6] Are your foremen and supervisors additionally compensated for performance improvement?

MR. BUSBY: Our foremen and supervisors are compensated for performance improvement and have been since January 1, 1953. This compensation is based entirely upon improvement and continued improvement must be made in order to be elligible for this compensation.

D. L. WATSON: [7] What basis do you use for separating direct costs from indirect costs?

MR. BUSBY: At Texas Foundries our performance reports are completely separate from jobbing cost accounting. In job costing we define direct costs as only those which can be isolated and charged direct to the particular job or casting made. This, of course, is in contrast somewhat to the general definition of direct costs referring to direct costs being those which result in a change to the product itself. If we were to consider our product as castings rather than a specific customer's casting, many of the costs which we term indirect would then be considered direct.

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[4] Standards Foreman, Oklahoma Steel Castings Co., Tulsa, Okla.
[5] Assistant Works Auditor, American Steel Foundries, E. Chicago, Ind.
[6] Bronze Alloys Co., St. Louis, Mo.
[7] Metallurgist, Holmes Foundry Co., Ltd., Sarnia, Ont., Canada.

AIR POLLUTION CONTROL EQUIPMENT FOR MELTING OPERATIONS IN THE FOUNDRY INDUSTRY

By

Robert T. Pring*

For nearly a generation, the ferrous and non-ferrous castings producers in the United States have taken a leading role in the elimination of pneumoconiosis-producing dusts from the working environment. In coping with internal dust hazards through the application of local exhaust ventilation, the foundry industry soon became one of the first and foremost users of industrial dust collection equipment.

Thousands of tons of airborne particulate matter is carried daily into the countless local exhaust systems which have been installed on shake-outs, sand conditioning plants, and castings cleaning operations. To the credit of the industry, it may be stated that in only a few plants are these exhaust systems permitted to discharge to amosphere the solids entrained in the ventilation air. It is, and has been for years, a nearly universal practice to install dust collection equipment in conjunction with local exhaust ventilation of dusty operations in foundries. Admittedly, the selection of air cleaning equipment has not always been the best and some installations are operating at collection efficiencies somewhat lower than could be desired; nevertheless, the net effect has been to eliminate, even before it arose, an air pollution problem from scattered process dust of a magnitude which defies the imagination.

Recent years have seen a shifting of emphasis from dust control within the foundry to the abatement of pollution of the air surrounding the plant. Here, the foundry industry faces an entirely new set of problems. Its principal remaining sources of pollution involve not dust from sources susceptible of control by local exhaust systems and conventional dust arresters, but rather hot and corrosive gases laden with fume particles from melting operations.

The selection and application of air cleaning equipment for industrial dust control systems has been adequately treated in many publications.[1–6] For this reason, and more particularly because of the present interest in the abatement of pollution from melting operations, this paper will be restricted to a discussion of available equipment for the elimination of atmospheric pollution from cupolas, open hearth (reverberatory), and electric furnaces. The distinction between conventional dust collection and atmospheric pollution abatement equipment is made solely on the basis of (a) the small particle size of the pollutants, and (b) the high temperature of the entraining gases which the latter is required to handle.

* American Wheelabrator & Equipment Corp., Mishawaka, Ind.

Atmospheric Pollution Defined

Atmospheric pollution may be defined as the presence in the air of substances put there by the acts of man in concentrations sufficient to interfere with the comfort, safety or health of man, or with the full use and enjoyment of his property.[7] Largely, the problem is one of *nuisance,* since measurable injury to human health by industrial pollutants is rare. Nevertheless, the dramatic episodes of the Meuse Valley of Belgium (1930) and Donora, Pa. (1948) served to focus public attention on atmospheric pollution as a community problem.

What constitutes a nuisance in one area may not be of great concern in another. For a given mass rate of emission from industrial stacks, it is the topography of the surrounding country and the meteorology of the area that determine the ground level concentration of pollutants. For a given concentration of pollutant at ground level, it is the land usage in the area that determines the presence or absence of a nuisance.

Atmospheric Pollution Codes

In certain critical areas, where topographical and meteorological conditions are unfavorable to the rapid dispersion of industrial stack emissions, legal restrictions have been placed on either the mass rate of emission or on the concentration of pollutants in the discharged gases. Atmospheric pollution regulations are also in effect in other urban areas where usage of the surrounding land justifies some measure of control. The stringency of the various codes varies from the extreme of the Los Angeles County Air Pollution District to the indefinite and often unenforceable regulations in certain other areas. In many cases, the foundry industry is singled out as a major contributor to the overall pollution problem.

Any code must, to be fair and equitable, take cognizance of the fact that control requirements in one area may bear no resemblance to what is reasonable elsewhere. Those responsible for the preparation, enactment, and enforcement of atmospheric pollution regulations cannot ignore the factors of land usage, meteorology and topography, nor is it equitable to enact standards of such stringency that no feasible means of compliance is available.

Where it is necessary to place restrictions on the discharge of pollutants to atmosphere, these restrictions, if clear and specific, may serve as a guide in the selection of control equipment. On the other hand, ordinances which leave in the hands of the individual enforcement officer the responsibility for determining what constitutes violation in each individual case are susceptible to capricious and discriminatory administration. These ordinances often confuse the purchaser of control equipment and lead to unwise and costly experiments.

In areas where no atmospheric pollution ordinances have yet been enacted, the plant operator must decide whether to install relatively inefficient control equipment to placate complaining neighbors or to "go all the way" with equipment which will meet the requirements of any foreseeable future atmospheric pollution law. He must gamble the cost of a less expensive installation which may later be scrapped against the higher investment in more efficient equipment. The odds are seldom known when the decision is made.

Much has been written on the peculiar combination of topography and meteorological conditions of the Los Angeles area which, with the rapid industrial expansion of the last decade, has contributed to the acute pollution of the surrounding atmosphere. To combat this situation, the Los Angeles County Atmospheric Pollution Control District was established and the most stringent anti-pollution regulations existing anywhere in the United States were adopted.[8] Of interest to the foundry industry everywhere are the provisions of Rule 54 of the code which relates allowable discharge of gas-borne solids to process weight (Table 1). This is an example of a specific code which, although its adoption in many other localities is improbable, represents the most severe restriction on industrial air pollution likely to be encountered at any time in the foreseeable future.

In contrast, the Allegheny County, Pa., Ordinance[9] permits the discharge of 0.85 lb of solids per 1000 lb of effluent gas from cupolas (0.454 grains per SFC) without regard to process weight or mass rate of emission. The selection of control equipment capable of compliance with this requirement is relatively broad.

No matter how stringent the code requirements for gas cleaning, proper evaluation of any dust and fume collection device requires the development of suitable standard methods for industrial stack sampling which give accurate, readily duplicated data on equipment performance. At present, a dozen different sampling methods are used, giving an equal number of divergent results. Several committees representing trade groups and professional societies are at present attempting to evaluate sampling methods with the ultimate objective of developing a standard technic for adoption by all agencies.

TABLE 1—PERMISSIBLE STACK SOLIDS EMISSIONS FOR VARIOUS PROCESS WEIGHTS*

Process Wt/hr (lb)	Maximum Weight Disch/hr (lb)	Process Wt/hr (lb)	Maximum Weight Disch/hr (lb)	Process Wt/hr (lb)	Maximum Weight Disch/hr (lb)
50	.24	1,900	4.03	4,700	6.45
100	.46	2,000	4.14	4,800	6.52
150	.66	2,100	4.24	4,900	6.60
200	.852	2,200	4.34	5,000	6.67
250	1.03	2,300	4.44	5,500	7.03
300	1.20	2,400	4.55	6,000	7.37
350	1.35	2,500	4.64	6,500	7.71
400	1.50	2,600	4.74	7,000	8.05
450	1.63	2,700	4.84	7,500	8.39
500	1.77	2,800	4.92	8,000	8.71
550	1.89	2,900	5.02	8,500	9.03
600	2.01	3,000	5.10	9,000	9.36
650	2.12	3,100	5.18	9,500	9.67
700	2.24	3,200	5.27	10,000	10.0
750	2.34	3,300	5.36	11,000	10.63
800	2.43	3,400	5.44	12,000	11.28
850	2.53	3,500	5.52	13,000	11.89
900	2.62	3,600	5.61	14,000	12.50
950	2.72	3,700	5.69	15,000	13.13
1,000	2.80	3,800	5.77	16,000	13.74
1,100	2.97	3,900	5.85	17,000	14.36
1,200	3.12	4,000	5.93	18,000	14.97
1,300	3.26	4,100	6.01	19,000	15.58
1,400	3.40	4,200	6.08	20,000	16.19
1,500	3.54	4,300	6.15	30,000	22.22
1,600	3.66	4,400	6.22	40,000	28.3
1,700	3.79	4,500	6.30	50,000	34.3
1,800	3.91	4,600	6.37	60,000 or more	40.0

* Los Angeles County, Calif.

Nature of Solids in Furnace Stack Gases

Dust, fume, and smoke are commonly present in gases from melting operations. They may be classified according to particle size and mode of generation.

Dusts are formed from solid, inorganic or organic materials by mechanical force (i.e., crushing, grinding, etc.). Particle size ranges from macroscopic to submicroscopic; however, particles over 50 microns settle rapidly and are of lesser significance.

Fumes are formed from solid materials by oxidation, sublimation, and condensation. Fumes are usually less than 1 micron in size, although they may flocculate and form larger particles.

Smokes are formed from the combustion of carboniferous materials and are generally the products of incomplete oxidation. The size of smoke particles is usually less than 0.5 microns.

Generally, gases from furnaces contain both dust and fume particles, the proportion of each depending on the type of material charged, the temperature, and the phase of the melting cycle. To attempt to generalize on volume, dust loading, and temperature of gases from melting furnaces serves little useful purpose because of variations in air infiltration and in individual furnace practices. Published data regarding concentration, particle size, and composition of dust and fume in furnace gases are meager and conflicting. Figure 1 shows particle size ranges of dusts and fumes

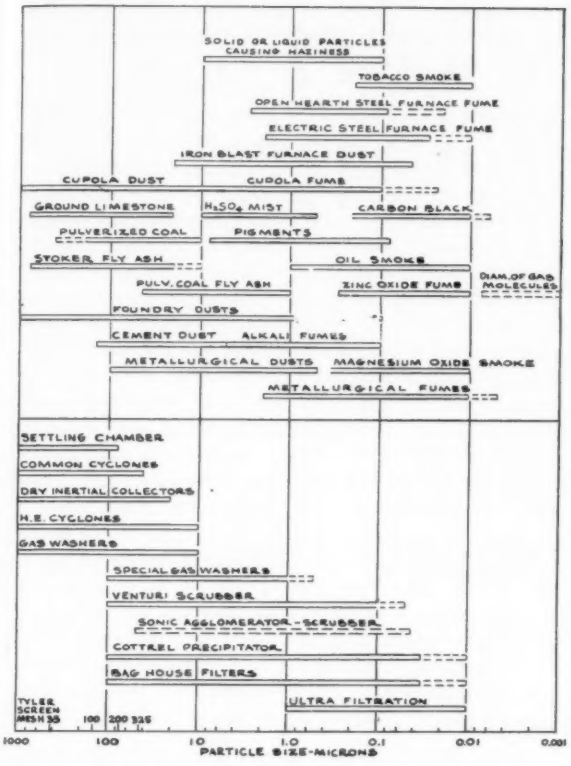


Fig. 1—Graphical representation of particle size ranges of dusts and fumes from typical industrial operations with approximate operating ranges of control equipment.

from various sources, along with certain familiar "standards" of comparison. In the lower portion of the chart are given typical performance ranges of commercial dust and fume collectors. Broken lines indicate areas in which data are uncertain or controversial. This information is adapted from Miller[10] and Keyser and Munger[11] modified in the light of more recent experience.

Table 2 is an attempt to summarize and reconcile published data on the particle size, concentration, and emission rate of solids in furnace gases, giving, where possible, the source of the information. It serves to emphasize the meagerness of our knowledge and the wide variation in reported values.

In the case of cupolas and other furnaces using dirty scrap material in the charge, considerable coarse dust is carried up the stack to become a problem in the immediate neighborhood; however, the metallic fume, oil smoke (from oily scrap) and gaseous impurities may be dispersed by air currents over wide areas, contributing to haze and pollution of the type encountered in Los Angeles County.

Obviously, where land usage permits and topography and meteorology are favorable, it may be sufficient to install dust collection equipment of the type which will prevent settlement of coarse material around the plant, exhausting the fume particles through a tall stack in order to disperse them over a wide area. It must be pointed out that the instances in which this procedure would be acceptable are becoming fewer as public awareness of atmospheric pollution increases. The abatement of atmospheric pollution, as contrasted to a purely local nuisance, requires the elimination of a major portion of the extremely fine particles at the lower end of this particle size spectrum.

The problem of selecting equipment for cleaning furnace gases is intensified by the high temperatures usually encountered, as well as the extreme fineness of the offending particles and the difficulty of entrapping them in many conventional types of dust arresters. Pre-cooling or conditioning the hot gases is usually required, depending on the type of equipment installed, and special materials to withstand heat and corrosion must be employed in many cases.

A wide variety of gas cooling methods is available,[12] comprising the following, either singly or in various combinations:

1. Radiation and convection cooling (requires large capital investment when used alone).
2. Evaporative cooling (requires careful control to avoid condensation).
3. Admission of tempering air (increases total volume of gas to be cleaned).

Cupola Fume Collection Equipment

The choice of equipment for cleaning gray iron cupola stack gases rests largely on the present permissible solids discharge rate in the individual area, an appraisal of reasonable anticipated future code requirements, and a comparison of capital and operating costs of the equipment which will provide the desired collection efficiency.

Most of what we know about cupola fume collection comes from Los Angeles County[13] and one or two larger industrial concerns located elsewhere in the country. Cupolas studied produced 64,000 to 182,000 SCF of stack gas containing 14 to 29 lb of solids, both per ton of charge. Of these solids, 45 to 75 per cent was greater than 44 microns in particle size and could be efficiently removed from the gas stream by many types of dust collectors without difficulty. The remaining 25 to 55 per cent comprised fine particles including 4 to 11 per cent under 5 microns and could be collected only by the most efficient equipment.

In one instance, a 54-in. I.D. cupola having a process weight of 15,730 lb per hr was permitted to discharge only 13.58 lb per hr under Rule 54 of the Los Angeles code. The actual solids emission during the charging period was 174 lb per hr in 17,700 SCFM. Compliance with the regulations required the removal of at least 92 per cent of the solids passing up the cupola stack, including a substantial portion of the minus 5-micron material. Stated another way, the dust loading of 1.15 grains per SCF must be reduced to approximately 0.09 grains per SCF, only slightly above the point of no visible discharge, estimated to be in the range of 0.05 grains per cubic foot.

Only two types of dust and fume collection equipment are known to be capable of meeting this rigid performance requirement:

1. Cloth bag filter
   (a) Orlon bags at 275 F,

TABLE 2—TYPICAL DATA ON FURNACE GASES

Type Furnace	Data Source	Size	Product	Stack Gas Volume SCF / Lb Melt	SCF / Lb Charge	Process Air Lb Air / Lb Melt	Dilution Ratio	Max. Temp., F	Stack Loss—Solids Grains SCF	Lb Hour	Lb Ton Charge	Particle Size—Weight Mesh	Micron	%
Cupola	Note[a]	Av. of 3	Gray Iron		32-91			1200-1700	0.8-1.6	69-196	14.8-29	100		16-24
												200		16-18
												325		13-32
													50-25	4.8-13.3
													25-10	3.4-15.9
													10-5	1.4-13.3
													-5	4.1-10.6
Cupola	I'Anson[b]	54" I.D.	Gray Iron		67.5			1400	1.15	174	22			
Cupola (Preheat)	Radcliffe[c]	60" I.D.	Gray Iron					290	.07-.32					
Cupola (Meltdown)								914	.52-.84					
Cupola (Normal Op.)								1076	1.24-3.45					
Cupola	Witheridge[d]		Gray Iron	7.8-23.4 (Combustion gas)		0.5-1.5	1-10		0.2-13.5				-10	10
													-20	23
												—325	-44	47
													-50	50
												—200		63
												—100		82
												— 60		91
												— 20		99
Arc Furnace	Allen[e]	3-6 T.	Stl. Cstgs.		120-180				0.4		6-8		-0.5	95
		50-75 T.	Stl. Ingots							198	12			
Arc Furnace	Kane[f]		Stl. Cstgs.						1.0*				0.5	90
Arc Furnace	Zang[g]	3 T.	Stl. Cstgs.						0.35-.57*					
Open Hearth	Allen		Sec. Metal						0.8					
Open Hearth	Bishop[h]		Stl. Mfr.						0-2.0				-5.0	50
Open Hearth	Vajda[j]	225-250 T.	Stl. Mfr.						1.0	75			-1.0	50
Open Hearth	Roberts[k]	58 T.	Stl. Mfr.					1200	0.18-.44	38-180			1-3	7.3
													0.5-1	28.4
													0.15-0.5	49.5
													-0.15	14.8
Open Hearth	Jones[l]		Stl. Mfr.										0.25-0.33	Mean
Brass Furnaces (Average)	Allen	Variable	Brass Cstgs.						0.139*	9.15			0.03-1.0	(Range)

*At stack temperature

SOURCES

[a] "Characteristics of Emissions from Gray Iron Foundry Cupolas," Tech. Subcommittee, Gray Iron Founders Smog Committee.
[b] I'Anson, J. E., Harsell, T. L. Jr., and Pring, R. T., AMERICAN FOUNDRYMAN, Jan. 1953.
[c] Radcliffe, J. C., A.F.S. SYMPOSIUM ON AIR POLLUTION, 1952
[d] Witheridge, W. N., *The Foundry*, Mar. 1950
[e] Allen, G. L., Viets, F. H., and McCabe, L. C., U. S. Bureau of Mines I. C. 7627
[f] Kane, J. M. ASME Process Industries Div., Pittsburgh Regional Conference, Apr. 24, 1950
[g] Zang, V. E., Remarks at AIME Electric Furnace Steel Conference, Pittsburgh, 1952
[h] Bishop, C. A., Paper before A.P. & S.P.A., June 10, 1952
[j] Vajda, S. and Dreher, G. M., Paper before A.P. & S.P.A., June 9-12, 1952
[k] Roberts, L. M. and Beaver, C. E., Proc. 1951 Annual Convention A.P. & S.P.A.
[l] Jones, W. P., *Ind. & Eng. Chem.*, Nov. 1949

(b) Glass fiber bags at 450-500 F (inlet),
(c) Natural fabrics with quench cooling and reheating.

2. Cottrell precipitator.

Preliminary data from very small scale pilot plant testing may warrant consideration of:

3. Sonic agglomerator—wet scrubber.

Elsewhere, restrictions on cupola emissions are less rigid. Allegheny County, Pa., for example, limits cupola emissions to 0.85 lb. "Fly ash" per 1000 lb of gas, or about 0.454 grains per standard cubic foot (60 F). Assuming a maximum grain loading of 1.6, equipment having a collection efficiency of 71.6 per cent by weight would be required to meet Allegheny County standards. In contrast, the two wet scrubbers tested for efficiency by the Gray Iron Foundrymen's Smog Committee in Los Angeles removed 57 and 70 per cent by weight respectively of the solids in the cupola gases. It follows that some, but not all, wet scrubbing devices may meet efficiency requirements in areas where concentrations of solids in cupola gases in the range 0.4 to 0.5 grains per SCF will meet code requirements.

Among dry inertial collectors, only a unit utilizing the louvred cone principle is known to be in successful use on a commercial scale. One such installation in a mid-western city is reported to have been approved by city authorities, although visible stack discharge is by no means eliminated.

Where it is desired to eliminate a local "neighborhood" nuisance from rapidly settling coarse particles, various types of stack washers are available. These are generally "spark arresters" capable of removing only the larger particles from the stack gases. In some cases, stack washers are operated primarily as coolers and are followed by more efficient dynamic, centrifugal, or orifice-type scrubbers located at ground level.

High Efficiency Equipment

Figure 2 is a photograph and Fig. 3 a flowsheet describing a completely automatic bag filter installation on a 54-in. I.D. cupola at a Los Angeles foundry.[14] A refractory-lined, counterweighted baffle (A, Fig. 3) restricts air infiltration through the charge door to reduce filtration volume and permit secondary combustion in the raised stack (B). Cupola lids (C) are counterweighted and power operated from the charge floor. Hot gases pass through a stainless steel stub pipe (D) to a spray quencher (E) where they are cooled to 450 F before passing through a carbon steel duct (F) to the secondary cooler (G) where the temperature is further reduced to 275 F. An exhauster (H) blows the gases through a five compartment, continuous automatic bag filter (J), equipped with orlon filter bags and electrically-operated shaker mechanisms. A thermocouple, located in the inlet duct (L) actuates a controller which, in turn, regulates cooler sprays and provides for by-passing at (K) in the event of water or power failure. Performance of this system is shown in Table 3.

TABLE 3—PERFORMANCE OF FUME COLLECTION SYSTEM

	Measured 10-8-52
Gas volume entering filter @ 275 F, cfm	16,900
Square feet of cloth	
with 5 compartments filtering	7,320
with 4 compartments filtering	5,856
Gas-to-cloth ratio	
with 5 compartments filtering	2.3
with 4 compartments filtering	2.9
Shaking cycle, minutes between shaking periods, each compartment	15
Shaking duration, minutes per cycle	1
Gas temperature, degrees F	268
Calculated dew point, degrees F	167
Pressure loss through bags, inches water	
with 5 compartments filtering	5.9
with 4 compartments filtering	6.7
Exhauster motor hp rating	40
Average weight dust caught, lb per hr	
in quencher	31.2
in secondary cooler	6.4
in dust collector	47.3
Total	84.9
Average dust loss in collector exhaust (thimble test), lb per hour	0

The cost of purchased equipment was about $.94 per actual CFM filtered, and the installed cost about double that figure. The collection efficiency was so close to 100 per cent that the sampling method employed detected no solids discharging to atmosphere. No trouble was experienced in obtaining Air Pollution Control District approval.

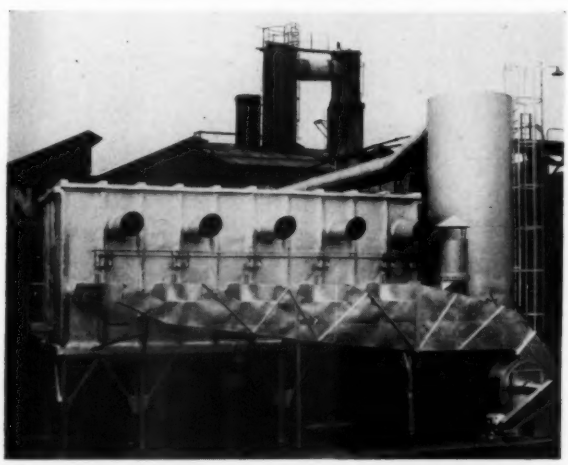


Fig. 2—Continuous automatic cupola fume control system at Los Angeles foundry employing filter with orlon bags.

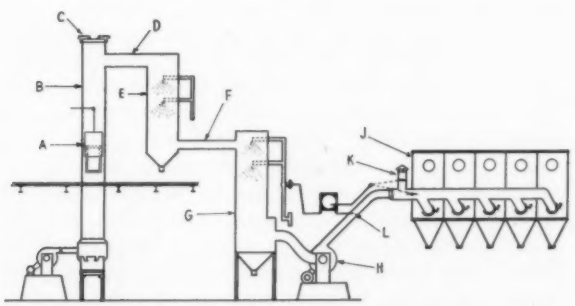


Fig. 3—Flow sheet of continuous automatic cupola fume collection system illustrated in Fig. 2.

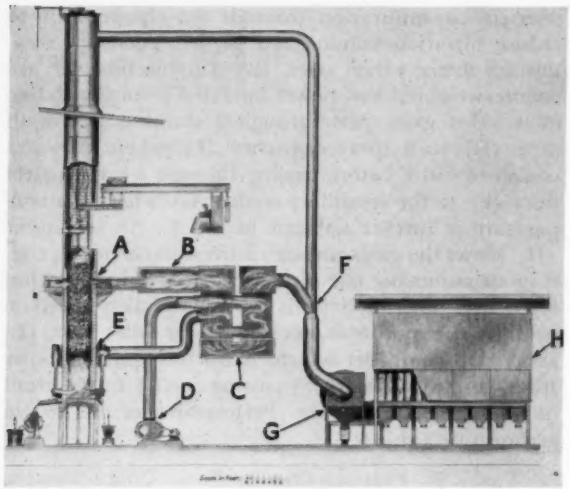


Fig. 4—Flow sheet of hot blast equipment in conjunction with glass bag filter.

Another application of filtration of cupola stack gas involves the use of hot blast heat exchangers as a substitute for part of the spray cooling. In Fig. 4, a portion of the combustible gas is withdrawn from the cupola at (A) and burned in an external combustion chamber (B), the hot combustion products passing through a tubular heat exchanger (C), where some of the heat content is transferred to clean air from the cupola blower (D). This hot air is blown into the cupola through the tuyeres (E) with a consequent reduction in coke consumption. The gas burned in the cupola stack rejoins the effluent from the heat exchanger at (F), the whole passing through a primary separator (G) and fan to the bag house (H). In this example, the filter is a pressure-type unit with hand shaken fiberglas bags, housed only for weather protection. This type of semi-enclosed bag house is operated at an inlet gas temperature of 450-500 F, and is used also without hot blast equipment; in which case, additional spray cooling, in the form of a stack washer or separate spray chamber, is required. The cloth area of the large (11-in. diam by 15 ft) filter bags is based on a 3.0:1 air-to-cloth ratio. When the pressure drop builds up to about 6.0 in. w.g., a workman isolates each section and operates the shaker. Collection efficiency is well within code requirements. For installations not involving hot blast, the equipment cost is stated to be about $1.80 per CFM filtered.

Electrostatic precipitators may be used in cleaning cupola gas with or without preceding hot blast equipment. Because of the high cost of electrostatic units, they are more apt to be selected by larger production foundries for use in conjunction with hot blast. Radcliffe[15] has described one mid-western installation, schematically illustrated in Fig. 5. Data on efficiency of Cottrell precipitators against cupola fume are lacking; however, recoveries in excess of 90 per cent by weight are indicated. In conditioning the gas with water vapor, the temperature may be reduced to as little as 225 F, requiring precautions against condensation and corrosion. Installation cost for precipitators

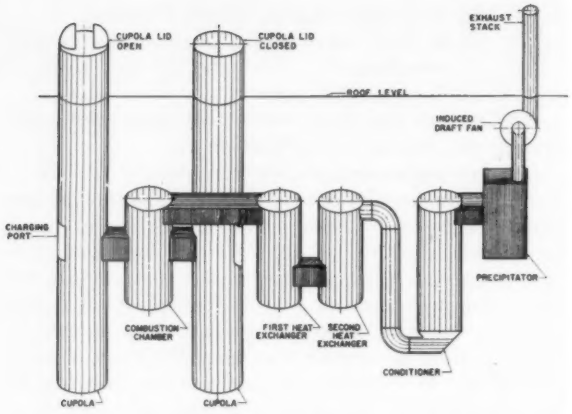


Fig. 5—Schematic diagram of Cottrell precipitator and hot blast equipment cleaning gas from two cupolas in midwest foundry.

and coolers only may run about $2.80 per CFM at 300 F, based on 5500 CFM at the tuyeres.

Possibly, because of its high cost (estimated at around $6.00 per CFM), sonic agglomeration has not been commercially applied to cupola fume collection; although, a pilot plant test of sonic agglomeration, in series with a wet scrubber, showed that high collection efficiencies could be obtained.

Moderate Efficiency Equipment

Many dry-type dust collectors are in use on cupola gas, ranging from wire screen "spark arresters" and sheet metal settling boxes to more elaborate inertial and centrifugal units. Most of these trap only the coarsest material.

Hot blast is used in conjunction with other collectors than bag filters or Cottrell precipitators. Figure 6 shows the stack appearance of a vortex collector downstream from heat exchange equipment in another mid-

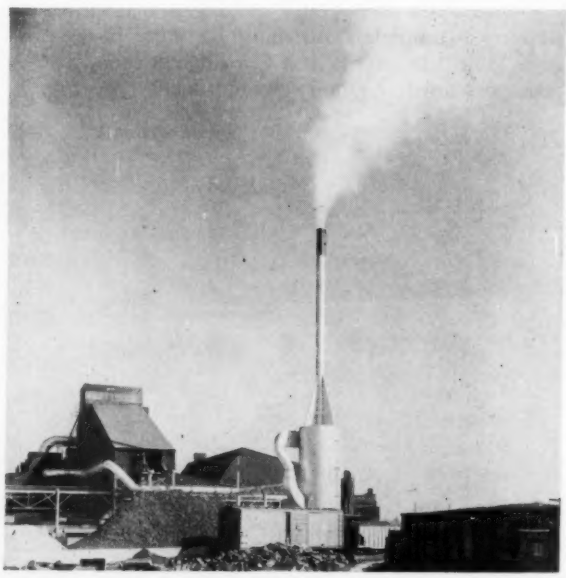


Fig. 6—Photo of discharge from vortex collector stack following hot blast equipment at midwest foundry.

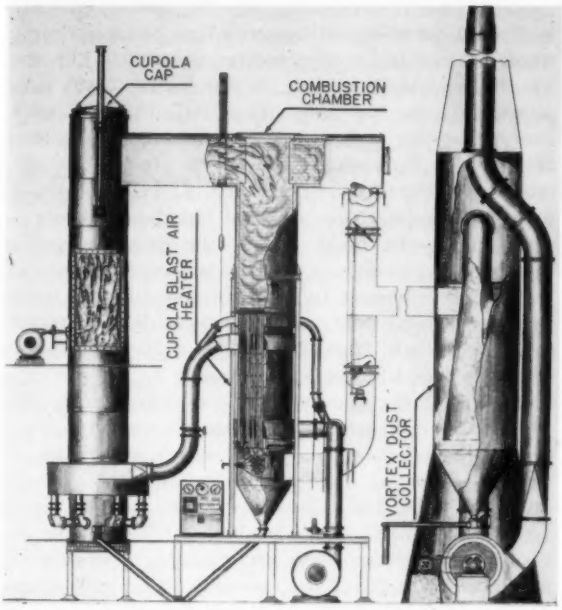


Fig. 7—Schematic diagram of hot blast equipment with vortex collector for dust elimination.

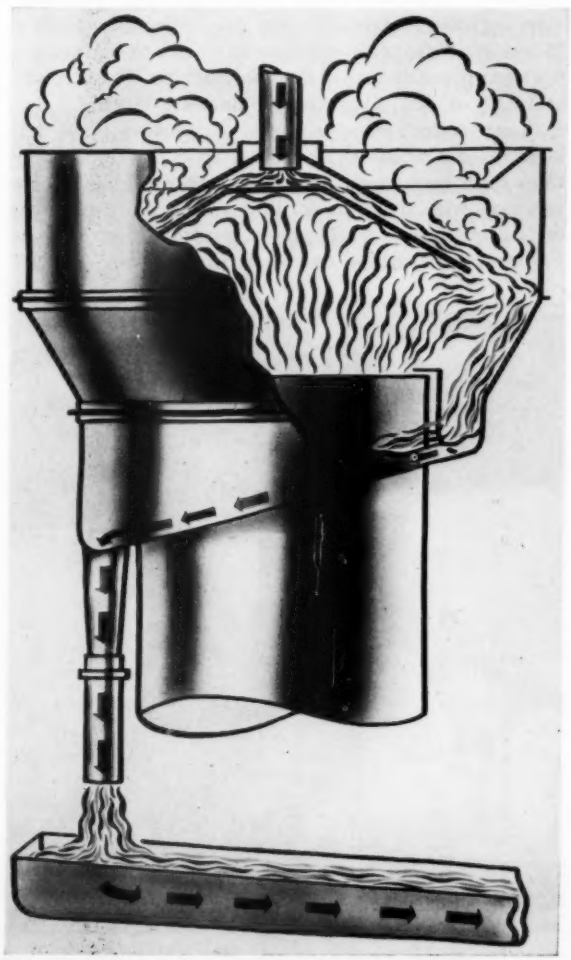


Fig. 8—Cupola stack washer of type widely used for elimination of sparks and coarse material.

western city. This installation is reported to have received approval of the city authorities, and is recovering about 16.5 lb of dust per ton of iron melted. The installation is schematically illustrated in Fig. 7. The manufacturer estimates that, for 10,000 CFM of blast air, the cost of equipment as illustrated will vary from $100,000 to $150,000 for each pair of cupolas, including the hot blast.

Figure 8 illustrates one type of cupola stack washer used to trap cinders and coarser dust fractions. Water flows over the cone baffle and forms a curtain across the peripheral vent space. A gutter and drain carries the sludge to settling basins or dewatering tanks from which the clarified water may be recirculated. It is doubtful that this type of equipment can be expected to meet the more recent atmospheric pollution code requirements, but stack washers have served and will continue to serve a useful purpose at reasonable cost. They may be used in conjunction with secondary cleaners such as the centrifugal scrubber illustrated in Fig. 9, and have been, in one installation, used as gas coolers preceding a bag filter.

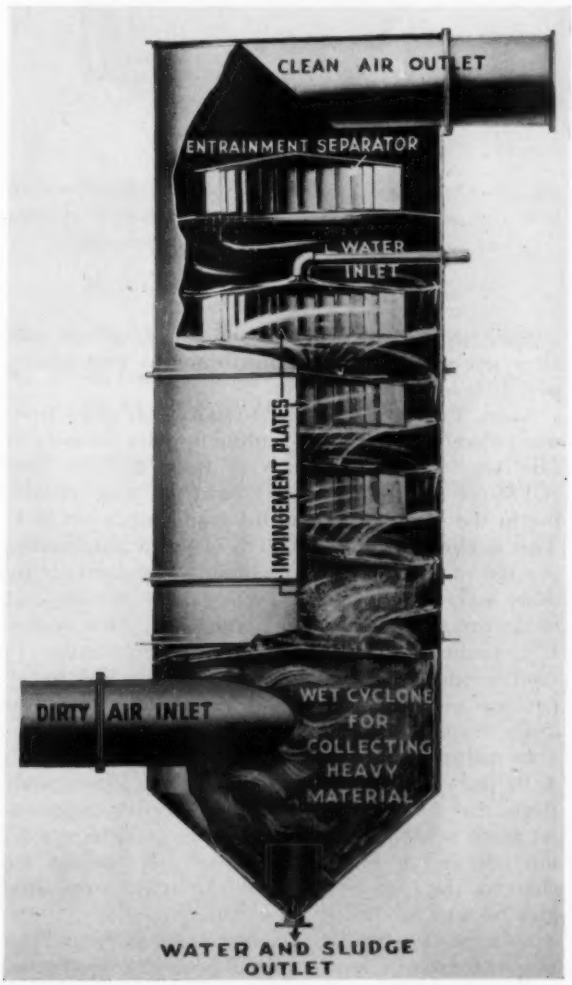


Fig. 9—Combination centrifugal and impingement scrubber sometimes used in connection with stack washers and widely employed in foundries generally.

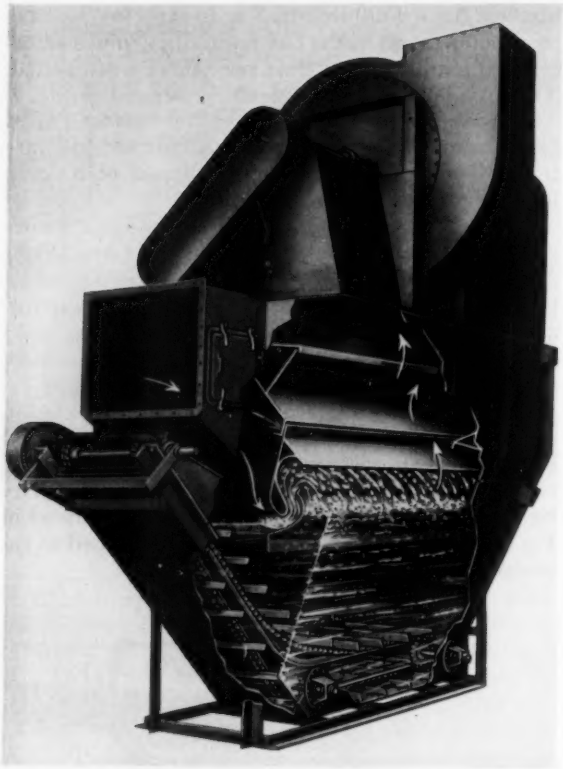


Fig. 10—Orifice type scrubber with sludge removal system used frequently on electric furnace ventilating systems and widely for general dust control in foundries.

Electric Furnaces

The electric arc furnace melting of carbon and alloy steels is a familiar contributor to atmospheric pollution of industrial areas.

Allen, Viets and McCabe[13] report that gases from small electric furnaces used in the foundry industry in Los Angeles County may vary from 2000 to 3000 SCFM per 1000 lb of process weight per hour containing in the neighborhood of 0.4 grains solids per SCF. This is equivalent to 5 to 8 lb of particulate matter per ton of metal melted per hour. Expressed in another way, solids emission may be 0.3 to 0.4 per cent of the process weight. Dust characteristics and operating conditions for larger furnaces are similar to smaller ones but fume losses are higher—0.5 to 0.6 per cent of process weight. One source[15] claims dust losses nearer 1.0 than 0.6 per cent.

In melting carbon steel, the fume may run as high as 97 per cent iron oxide while the fume from alloy steels may assay high in various alloying elements. As much as 90 to 95 per cent of the particles are 0.5 microns and less in size. Where oily borings are charged, the furnace emissions will contain considerable oil and smoke and some vapor.

In removing dust, fume, and gases from arc furnaces by exhaust ventilation, a variety of hood types are employed, depending on the arrangement of electrodes and gear above the furnace top, as well as the method of charging and operating used. Canopy hoods located above the craneway offer the least interference with established operating procedure but are wasteful in air volume and equipment requirements. Effective, but by no means perfect, is the furnace roof hood attached to the top ring. They require stiffening to prevent sagging at the high temperatures encountered and employ non-conducting inserts around the electrodes to prevent short circuiting. Lower gas volumes at higher temperatures are used but the excess air infiltration at the hood and at the duct connection prevents excessive heat at the collector. Emerson[16] reports no difference in roof life and no appreciable increase in electrode consumptions with the furnace-top hood, while Harrod[17] reports a 70 per cent increase in roof life due to air cooling.

The possibility of evacuating the gases directly from the furnace body or roof has been explored in several plants, there being an obvious advantage in the absence of external hooding on the furnace roof. In one case,[18] the design provides for evacuating up to 1000 SCFM (70 F) from the shell of a 7½-ton furnace at 3150 F maximum with spray cooling to reduce the temperature to 275 F for filtration. The gas volume, being only 3440 CFM at 275 F, represents a small fraction of that required with more conventional hooding.

In another instance,[19] gases are withdrawn from a 25-ton arc furnace in sufficient quantity to maintain a negative pressure under the roof varying between 0.01 and 0.03 in. w.g. Quantitative data are lacking.

Fume collection equipment heretofore used has been mainly of the wet scrubber type. Many installations have been made of the furnace-top hood in conjunction with dynamic scrubbers and, more recently, orifice scrubbers of the type illustrated in Fig. 10. Figure 11 illustrates one such installation. Accord-

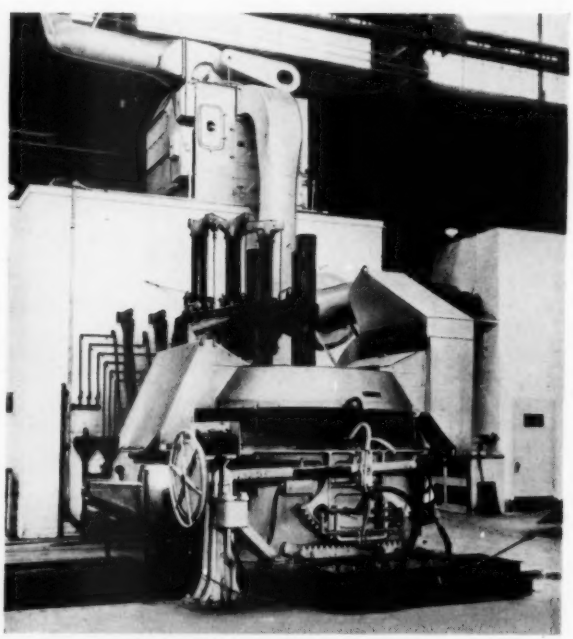


Fig. 11—Photo of arc furnace with roof type hood and orifice scrubber (background).

ing to Kane,[20] good conventional wet dust collectors can remove some 80 per cent of electric furnace fume particles and reduce stack concentrations of 1.0 grain down to 0.2 grain per cubic foot. This degree of cleaning will reduce but not eliminate visible solids in the stack discharge.

Zang[21] reports that orifice scrubbers have reduced fume particles from 0.35-0.57 grains to 0.15-0.18 grain per cubic foot, a weight efficiency of about 74 per cent. This recovery meets emission rate provisions of the city code; however, the small quantity of solids leaving the stacks is still conspicuous to the eye.

Tests of a tubular cloth bag filter (Fig. 12) against arc furnace gases containing metallic fume plus smoke from oil-soaked borings showed an entering grain loading of about 2.0 and a collection efficiency of plus 99.9 per cent by weight.[18] No visible solids could be detected in the filter effluent gas. The tubular-type filter bag with mechanical shaking mechanism at the top of the unit proved advantageous as demonstrated by the back pressure range, varying from a maximum of 1.8 in. w.g. just before shaking to 0.2 in. just after cleaning the bags. The equipment cost f.o.b. vendor's plant ranges from $0.60 to $1.30 per CFM exclusive of the furnace hood (s), depending on type of cloth and accessories selected.

Equally efficient are tubular cloth bag filters employing reverse-jet bag cleaning, schematically illustrated in Fig. 13. Dirty gas enters at the top (A) depositing dust on the inside of the felt filter bag (B), and the cleaned gas passes through the filter to atmosphere. An annular slotted ring (C) traverses the bag, dislodging collected particles from the filter by means of a jet of clean air supplied by an auxiliary blower (D). The dislodged solid material drops to a screw conveyor (E) or hopper from which it is removed for disposal. Where wool felt is used as the

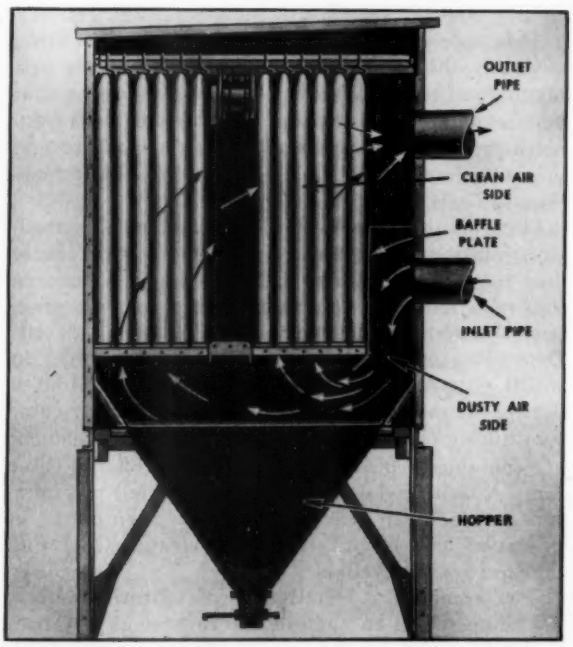


Fig. 12—Cutaway view of bag-type cloth filter of type used for cupola and electric furnace gas cleaning.

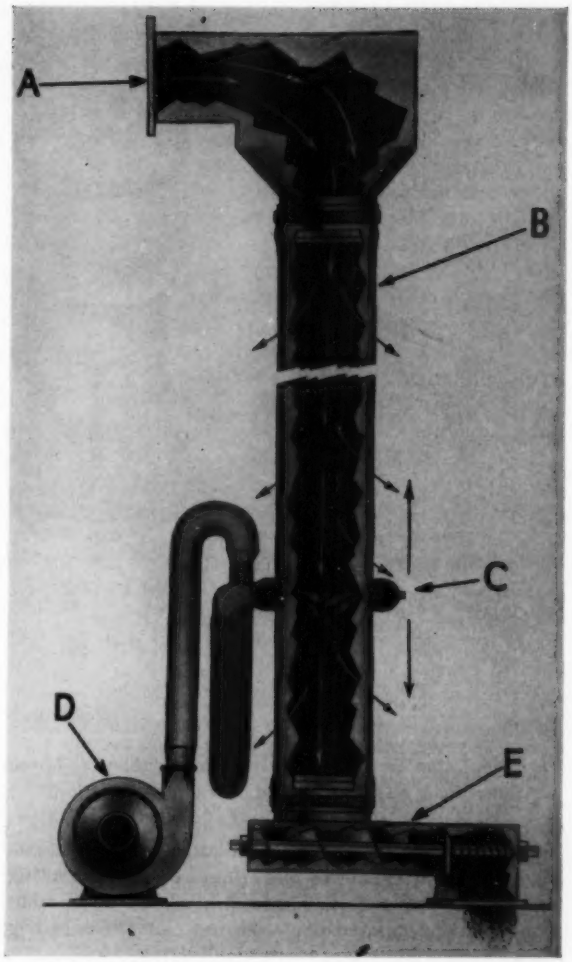


Fig. 13—Schematic drawing of reverse jet filter illustrating gas travel and method of cleaning bag.

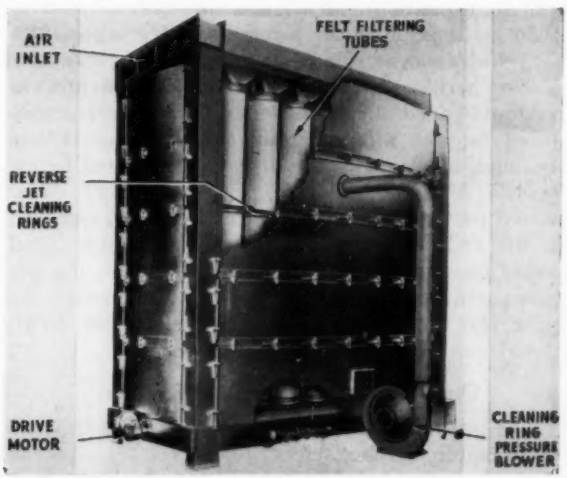


Fig. 14—Cutaway view of bag filter using reverse jet cleaning principle.

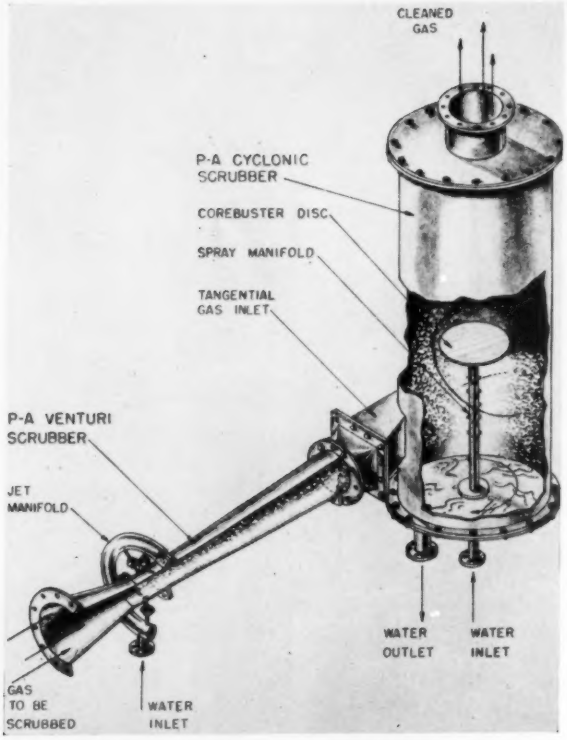


Fig. 15—Cutaway diagram of venturi scrubber followed by cyclonic scrubber as used for fume elimination.

filter medium, high air-to-cloth ratios are often employed with continuous blow-back by the traveling ring. With ratios of 12.0:1 and higher, the equipment cost would be competitive with the conventional bag filters. One commercial model is illustrated in Fig. 14.

At least one installation of Cottrell precipitators has been made cleaning ventilation gases from large arc furnaces pouring ingots. No performance data or costs are available; however, it may be presumed that satisfactory collection efficiencies are attained.

A pilot scale test of a venturi scrubber showed that 0.21 grain per SCF entering the unit was reduced to 0.035 grain per SCF, or a removal efficiency of 83.5 per cent by weight. Figure 15 is a schematic view of a venturi scrubber-cyclonic scrubber combination as commonly used for fume removal. Water is introduced through nipples at the venturi throat where thorough mixing with the dirty gas is effected by extremely high gas velocities and turbulence. The wetted particles and entrained moisture are removed in the cyclonic scrubber. Operating pressure (and power consumption) is high, depending on the efficiency required. Equipment of approximately the right size for a 1½-ton arc furnace (10,000 CFM) would cost about $1.00 per CFM delivered.

Open Hearth (Reverberatory) Furnaces

The control of air pollution from open hearth and reverberatory furnace stacks presents certain problems to the designer of air cleaning equipment. Grain loadings are not high, ranging up to 2.0 per SCF, and perhaps averaging around 0.43[22] depending, of course, on the type of charge, the fuel burned, the draft maintained, and whether waste heat boilers are installed. Because of the small particle size of the contaminant (one authority states 55 per cent minus 5 microns; 50 per cent minus 1.0 micron), the stack emissions are easily visible because of the large surface area of the dust available to refract light. Even when the solids emission rate is reduced below existing code requirements, visible stack discharge continues. A reduction of 95 to 98 per cent in the solids content of the stack gas is required to eliminate visible discharge, which exists above 0.05 grains per cubic foot. From the public relations viewpoint, a "clean stack" is highly desirable, especially where the casual bystander is unable to differentiate between high and low grain loadings in the visible range.

Vajda and Dreher[23] have published results of their extensive pilot-scale tests of various commercial dust collectors. Their results are summarized in Table 4.

TABLE 4—EFFICIENCY OF VARIOUS DUST COLLECTORS TREATING OPEN HEARTH FURNACE GAS

Primary Collector	Secondary Collector	Conditioner	Efficiency % by Wgt.
Sonic Agglomerator	———	———	20
"	———	Steam or water	55
"	Dry dynamic	———	20
"	"	Steam or water	55
"	Wet dynamic	———	90
———	"	———	45
———	Small dia. cyclones	———	43
———	Low voltage electrostatic	———	80
Orifice Scrubber	———	———	50
"	Wet filter	———	57
"	Gravel Filter	———	66
———	Traveling bed filter	———	78

This table should be interpreted with caution since, obviously, the details of the tests cannot be given in a summary of this nature. It is apparent, however, that none of the units tested could be expected to consistently produce a "clean stack," even though several showed promise of being able to meet Allegheny County Code requirements.

Pilot plant testing[24] of a high voltage (Cottrell) electrostatic precipitator against open hearth furnace dust having a median particle size under 0.5 micron showed a reduction in concentrations to 0.006 grain per cubic foot average over an entire operating cycle. Depending on the phase of the operating cycle, in which entering dust loadings varied from 0.18-0.44 during the melting period to 1.2-1.55 grains per cubic foot during tapping and lancing, the Cottrell effluent gas contained 0.005 to 0.12 grains of solids per cubic foot. A commercial installation of Cottrell precipitators on nine open hearth furnaces has been placed in operation at a recently built steel plant. Operating data are not yet available.

Two commercial installations of venturi scrubbers are in operation on open hearth furnace gas.[25] Mean particle size is in the range 0.25 to 0.33 microns (weight basis). Reported collection efficiencies run from 97.0 to 99.7 per cent by weight. Data on cost

and power consumption are not available.

Other types of high efficiency wet scrubbers have not been tested. Stream pollution regulations are such that the industry is reluctant to employ wet collectors, other than those in which the water can be recirculated, because of the large volumes of water requiring treatment and disposal.

Bag filters will produce "clean stacks" and their eventual adoption will become a matter of economic comparison.

Non-Ferrous Melting

Generalization on the subject of dust and fume collection equipment in the non-ferrous foundry industry is difficult. Countless alloys are poured from a wide variety of melting furnaces, ranging from pots or crucibles to reverberatory and induction furnaces of elaborate design. Again, we rely on Los Angeles County for the majority of our data.[13]

Light Metals—In the production of aluminum and magnesium alloy castings, various chlorides and cryolite are used in fluxing, which results in the presence of both gaseous and solid chlorides and fluorides in the stack gases. Recent emphasis on the role of fluorides in atmospheric pollution suggests the desirability of trapping as much as possible of this toxic material. Gaseous fluorine compounds are mainly hydrofluoric acid and silicon tetrafluoride.

One pilot installation of a wet electrostatic precipitator proved highly efficient against both solids and gases. From 87 to 99 per cent of the solids was recovered and nearly all the gaseous chlorides and 90 to 98 per cent of the gaseous fluorides were also trapped.

Very probably many of the more efficient scrubbers may prove effective because of the high solubility of the chlorides and fluorides. Pilot tests of the venturi scrubber indicated that satisfactory recovery of solid particles and nearly complete recovery of gaseous chlorides and fluorides could be expected.

Because of the need for removal of gaseous fluorides from stack gases, dry methods of collection should be approached with caution where these compounds are known to be present.

Copper Alloys—The non-ferrous smelting industry has, for years, turned to bag filters for the recovery of values in the form of zinc, lead and cadmium oxides. In copper smelters, acid concentrations and gas temperatures were well beyond the range considered suitable for natural fiber filter fabrics, and electrostatic precipitators have wide use in that industry.

In the production of copper alloy castings, the fume evolved from the melt comprises mainly oxides of zinc and lead and lends itself readily to recovery by filtration. In some cases, the collected material has some value as a paint pigment; in others, it is worthless and must be disposed of. A high zinc oxide content of the fume suggests possible use as a pigment and makes dry collection mandatory.

One mid-western foundry pouring brass castings has operated a bag filter for several years in connection with a rotating electric furnace.[18] The cold air infiltration at the furnace hood is sufficient to cool the gases below 180 F, the maximum operating temperature for cotton filter bags. Cost data are obsolete. Many installations of filters using treated glass fiber bags have been made in west coast non-ferrous metals plants and foundries. These are of the same general design previously illustrated in connection with gray iron cupolas and have met L.A.A.P.C.D. approval.

High cost has prevented widespread use of Cottrell precipitators in connection with non-ferrous melting operations. However, from the standpoint of collection efficiency, electrostatic precipitation can be expected to rank below cloth filters in collection efficiency. Their use may be justified where extremely corrosive gases are handled at temperatures close to the dew point, requiring special construction materials to withstand corrision. They may also be preferable where gases must be handled without cooling at temperatures above 500 F.

Static and dynamic scrubbers were tested in Los Angeles on copper alloy furnace gases with indifferent results so far as meeting A.P.C.D. Code requirements is concerned.[13] This does not mean that the collection efficiency of well-designed scrubbers will not meet air pollution requirements elsewhere. In fact, many installations of wet collectors have been made in localities having less stringent standards. One pilot scale test of a venturi scrubber reportedly yielded a collection efficiency of 65 per cent. Another test conducted at a smelter in England showed collection efficiencies ranging from 92.1 to 99.1 per cent by weight against lead and zinc fume.

Because of the wide variations in equipment and melting practice in the copper alloy casting industry, these comments should not be applied to the individual job. Every reputable manufacturer of dust and fume collection equipment will have specific operating data, applicable to the individual situation, which he will be more than willing to make available.

Acknowledgments

The writer has drawn heavily from previously published material—particularly papers by Allen, Viets, and McCabe, Radcliffe and Delhey, and Vajda and Dreher. In addition, the following manufacturers of equipment have generously contributed illustrations and data: American Air Filter Co., American Wheelabrator & Equipment Corp., Chemical Construction Corp., Day Company, Grindle Corp., Claude B. Schneible Co., and Whiting Corp.

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CORRELATION OF AIR FURNACE BOTTOM TEMPERATURE TO REFRACTORY AND OPERATING PRACTICE IN A CUPOLA-AIR FURNACE DUPLEX SYSTEM

By

F. W. Jacobs* and J. S. Lawrence, Jr.**

ABSTRACT

This paper represents a practical approach to control of refractory bottom life in an oil-fired air furnace by means of measuring temperatures of the furnace bottom.

Experimental work was done during a 10-month period of regular production practice using the air furnace of a cupola-air furnace duplex system in a southwestern malleable production foundry.

Method of furnace construction is shown including the method of placing the thermocouples under the service bottom for temperature measurement.

Graphs are used to compare results and show differences in retained bottom temperatures as related to amount of ventilation on the furnace bottom, size and type brick, variable construction and operation methods, slag treatment and effect of intermittent operation of furnace.

Costs expressed in man-hours per ton and 9-in. equivalents of brick per ton are presented for comparison of the nine campaigns covered in the paper.

Results indicate that measurement of bottom temperature is a definite aid in predicting bottom life and should be a useful control tool notably in those foundries which are limited to the use of one air furnace and where bottom failure in the middle of the week would seriously handicap production.

Introduction

One of the important problems facing air furnace operators today is that of furnace repair. Through many years of experience operators have learned to judge the condition of the bottom of an air furnace by such visual checks as noting the degree of joint erosion, spalled bricks, contraction cracks, the ease with which a furnace drains, and the amount of metal slabs remaining when drained. In most cases they have been able to get sizeable tonnage over a given bottom before it becomes unserviceable or before it actually comes up. However, the last week or two of service on a bottom usually becomes a matter which weighs heavily on the operator's mind and he wonders whether he is operating safely or if his judgment was sound and then when the bottom is torn out, he asks himself if he could have gotten another week out of it.

Knowing that brick has the capacity for absorbing and storing heat and assuming that this capacity might well be dependent on the thickness of the lining and numerous other factors which are discussed later in this paper, it was decided at the authors' plant to adopt some method of measuring this retained heat and to correlate the findings with bottom practice over a period of several furnace campaigns.

Melting and Refractory Practice

The duplex cupola-air furnace melting equipment at this plant consists of a 90-in. cupola lined to a 54-in. melting zone which feeds continuously a 34-ton capacity oil-fired air furnace (Fig. 1). The cupola is charged by skip hoist. Blast is supplied by a centrifugal blower and is dehumidified by means of a Kathabar system. The monolithic lining of the cupola is repaired each night by means of a blower gun. The slag is removed from the metal by a front slagging arrangement and is quenched in water for easy examination and disposal.

The air furnace is fired with No. 5 fuel oil using two low pressure burners.

The burners are sighted against a given point on the back bridge wall and set slightly toed to center line of furnace so that the flame hits the bath at the tap hole area.

A burner bridge 38½ in. long is maintained directly in front of the burner wall to prevent excess oxidation of the metal directly below the burners, Fig. 2.

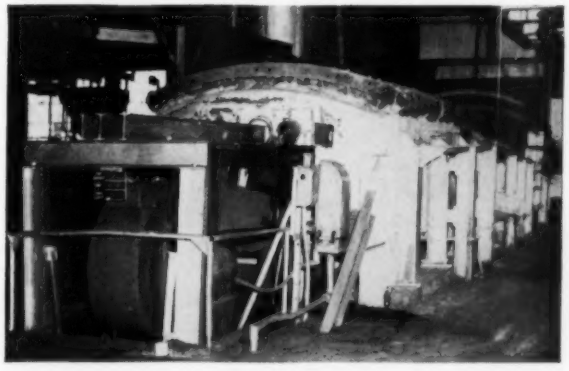


Fig. 1—View of cupola-air furnace duplex system.

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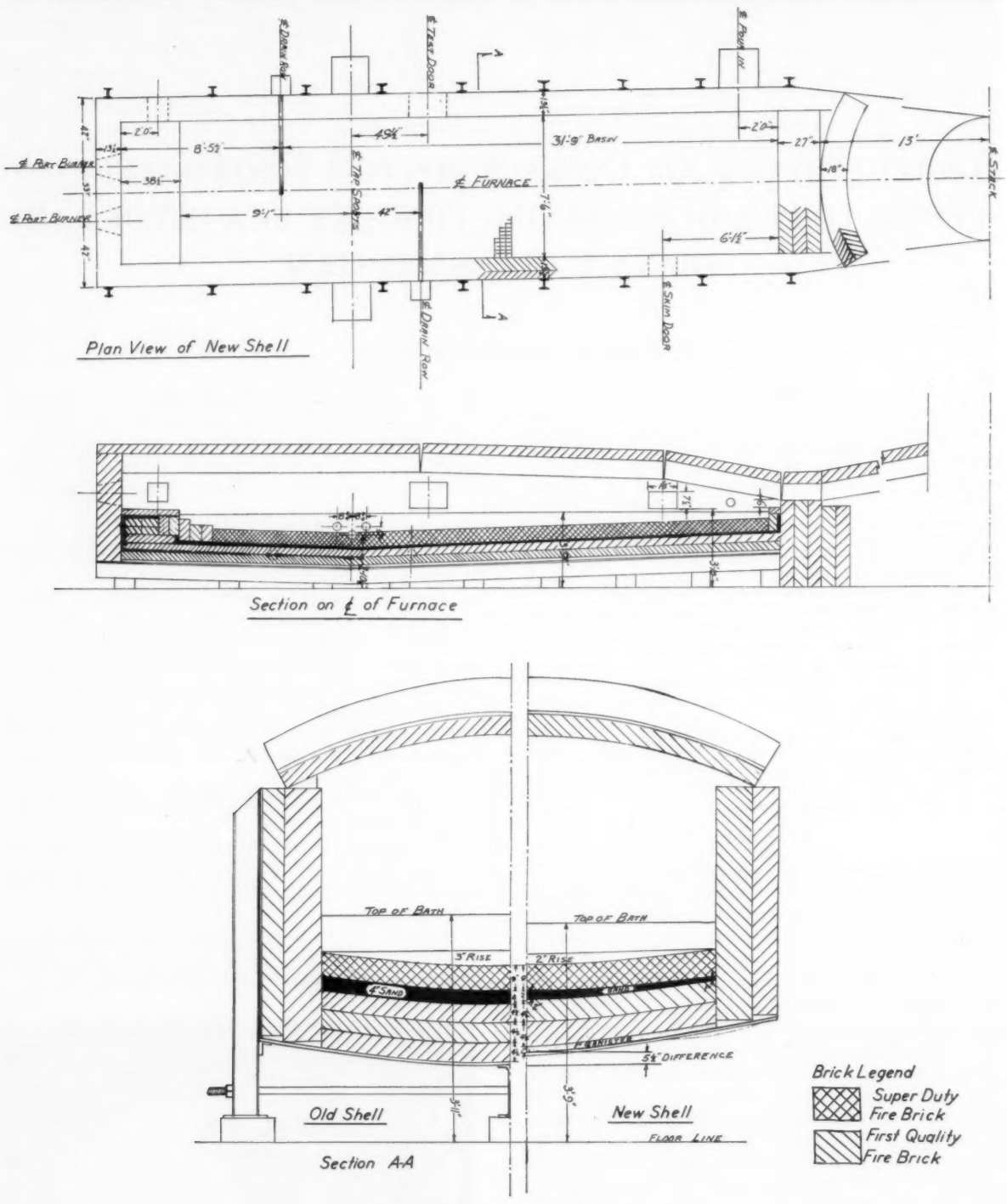


Fig. 2—Layout of duplexing furnace.

The bottom slopes ¼ in. per ft from burner bridge to center between tap holes for a distance of 9 ft, 1 in. The slope from the tap holes to the back bridge walls runs ⅛ in. per ft. The rise from the center of the inverted arch of the bottom to the side walls is 2 in. and the drop from centerline to the lower tap holes is 2 in. The skim door sill is 9½ in. and the back burner bridge is 14½ in. above center line of furnace at lower tap hole. Bungs are set so that center of bung arch measures 10 in. over bridge wall.

The refractory bottom consists of 1 in. of ganister laid directly on the furnace bottom plate, two courses of high duty (40 pct alumina) side wall brick laid in rowlock courses making a 9 in. sub-bottom, 2 in. sand

between sub-bottom and service bottom and 9 in. of service bottom making a total of 21 in.

Side walls, 13½ in. thick, are laid directly on the bottom plate. The sub-bottom and service bottom are wedged between the side walls. The service bottom is normally laid with large 9-in. straight, 70 pct alumina brick using a high alumina heat setting mortar for a dip joint. When the bottom is completed, it is swept with this same mortar.

Drain rows are constructed in the sub-bottom as a means of draining metal from the furnace in case of bottom failure. Each row leads from the center of the furnace to a tap block which is located in a frame attached to the side of the furnace. The forward drain row is located 46 in. ahead of the bottom tap hole at a point 8 ft, 5½ in. from the burner wall and, facing the burners, drains to the right side of the furnace. The rear drain row is located 42 in. behind the bottom tap hole and drains to the opposite side of the furnace.

The tap holes are arranged triangularly in a metal frame attached to side plate of furnace. This permits easy replacement of tap blocks without disturbing the side wall. The center of upper tap holes measures 5 in. above center of lower tap hole.

The metal bath measures 7 ft, 6 in. in width by 31 ft, 9 in. length. Depth of the bath at tap holes is 8½ in.

The melting system supplies metal for a one shift operation. Because the air furnace is oil-fired, it is necessary to maintain enough slag on the bath to prevent excess reduction of carbon in the metal. At the end of the cupola run it has been standard practice to cover the entire bath with slag drained from the cupola. Additions are made to the bath through a test door located on the right side of the furnace just back of the tap blocks. The average temperature of the cupola metal is 2760 F (1516 C) and the average furnace tap temperature is 2850 F (1566 C). The furnace is lighted off normally at 1:30 each morning, except mornings following a week end shut down, and the heat is out between 2:30 and 3:30 each afternoon. As much slag is drained from the furnace each day as possible.

Method of Measuring Bottom Temperature

Initial work on temperature measurement of the bottom was confined to measuring temperatures directly below the service bottom in the center of the furnace just forward of the tap hole area. This position was chosen first because of the expediency in positioning the thermocouple tube in the drain row and secondly because it represented to us the hot spot of the bottom.

A closed end nickel-chromium alloy case protecting tube, 1-15/16 in. O.D. x 13/16 in. I.D. with ¾ in. thread connection was set in the forward drain row with the tip centered between sidewalls in sand ½ in. below the service bottom, the threaded open end being set just inside the hole of the drain tap block permitting easy access of the thermocouple element. A No. 8 gauge bare wire chromel-alumel thermocouple element with double core insulators was then inserted through the drain block into the protecting tube so that the hot junction rested at the tip of the tube. This permitted easy removal of the thermocouple for checking against standard couples.

Finally and commencing with Bottom No. 3 a similar arrangement was made for checking temperatures below the service bottom behind the tap holes by placing a tube in the rear drain row and similarly inserting thermocouple through the rear drain tap block (See Figs. 3, 4 and 5.)

The protecting tubes are inspected and reset on each bottom repair prior to laying the bottom and remain in the furnace for the duration of the campaign. Figures 6 and 7 show views of the completed furnace prior to placing the bungs.

Temperatures are read and recorded twice daily using a portable potentiometer. The first temperature is recorded at light off time and the final temperature recorded immediately after the heat is out. For practical purposes these temperatures represent the condition of the bottom although it has been found by test that an average downward lag of 70 F occurs 3 to 4 hr after light off time before the bottom at the point of measurement starts to pick up heat. Similarly after a days run an average upward lag of 30 F occurs 2 to 3 hours after official measure-

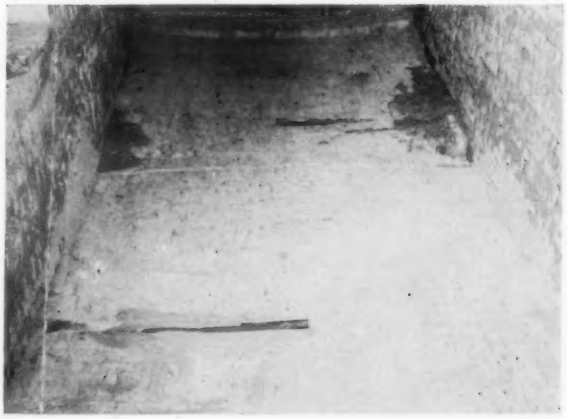


Fig. 3—Relationship of front and back thermocouple protecting tubes in position directly above sub-bottom.

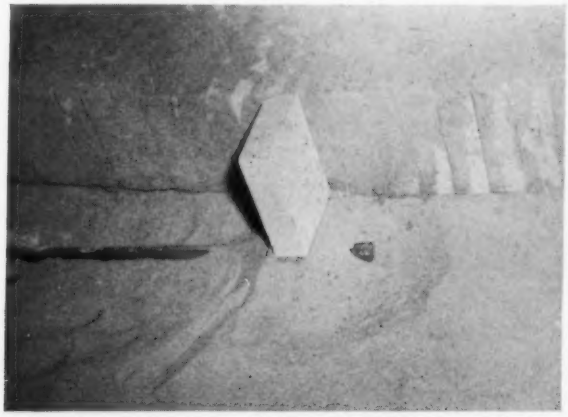


Fig. 4—Relationship of back thermocouple protecting tube to service bottom.

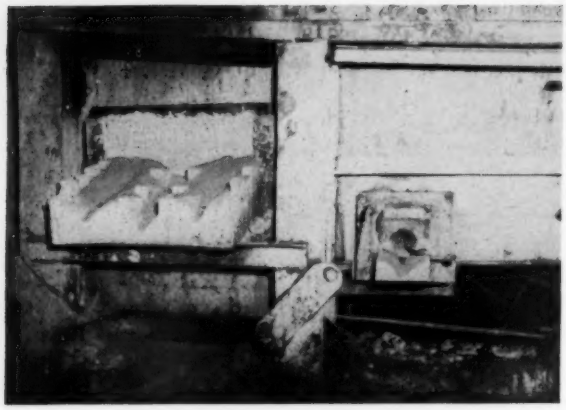


Fig. 5—Placement of thermocouple through drain tap block into protecting tube laying in sub-bottom drain row.

ment before the bottom starts to cool down.

The bottom temperatures are plotted on a daily control chart to show maximum and minimum temperatures on both front and back thermocouples as may be seen in Fig. 8. Here can be seen the cyclic nature of the graphs. The sharp dips in the graphs of the early morning temperatures indicate the extent to which a bottom cools down over a week end shut down. It is noticed that the front and back thermocouple readings follow very closely. The maximum temperature graphs are more indicative of bottom behavior and serve to point out the area of most heat concentration whether this be caused by firing methods or weaknesses in the bottom itself due to spalling action, metal penetration, or thinness of lining. Thus the front thermocouple may show a higher reading than the back thermocouple or vice versa dependent on the above mentioned conditions.

Test Procedure

When it was ascertained from Bottom No. 0 that bottom behavior might be closely allied with retained heat of the bottom brick, a program was set up to study the effect on bottom temperatures of bottom ventilation, two sizes and types of bottom brick, different methods of laying these brick, and finally the effect of cupola sag on bottom brick.

Bottoms No. 1 and No. 2 were laid with materials and methods identical to Bottom No. 0 in order to study effect of ventilation on the bottom temperature.

The program starting with Bottom No. 3 and ending with Bottom No. 7 called for one change at a time on each successive bottom. It was planned, as seen in Table 1, to make a gradual change-over from using 70 pct alumina large 9-in. straight bottom brick with high alumina heat setting mortar for a dip joint to using 60 pct alumina regular 9-in. straights laid dry on Bottom No. 5. This was then followed by the same type of bottom on Bottom No. 6 where a fillet of cupola slag was placed between sidewalls and bottom on all four sides of furnace. Bottom No. 7 was planned to be identical to Bottom No. 2 as a check back.

Since these experiments were carried out on a production unit, variables other than those originally planned were introduced during the entire program some of which were bound to affect the final results. These variables, namely the length of burner bridge, the depth of bath, the average outside temperature and the tap temperature, are listed in Table 1 and will be referred to from time to time in this paper.

Effect of Ventilated Bottoms

Facts listed after Bottom No. 0 on Table 1 represent operating conditions which prevailed at the start of the experiment. The first change came about with a decision to raise the bottom plate of the furnace in order to reduce the thickness of refractory in the sub-bottom, thereby hoping to better ventilate the bottom and lower the retained heat in the service bottom. As shown in Fig. 2, the refractory bottom in the old shell consisted of three 4½ in. courses of high duty side wall brick laid row lock directly on the bottom plate, 4 in. sand, and the 9-in. 70 pct alumina service bottom brick making a total of 26½ in. of refractory. The refractory in the new shell as also shown in Fig. 2 and referred to previously in the paper consisted of 21 in. total refractory thereby reducing the insulating effect by 5½ in.

By noting Fig. 9 which is a plotting of maximum



Fig. 6—View of completed bottom looking toward burners.

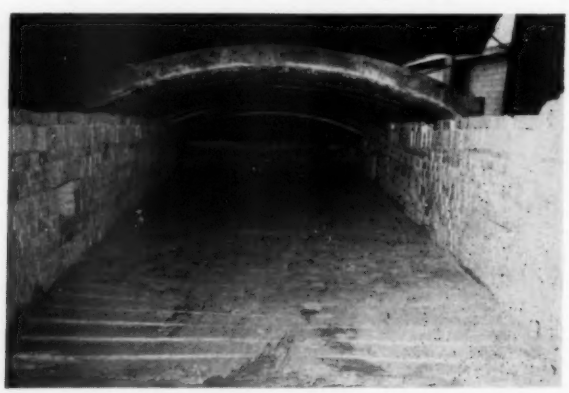


Fig. 7—View of completed bottom looking toward back bridge wall.

TABLE 1—BOTTOM RECORD

Bottom No.	Date In	Date Out	Type Brick	Mortar	Length Burner Rise	Depth Bridge,	Depth Bath,	Depth Room	Temperature °F Tap	Temperature °F Bottom	Total Tons on Bottom	Total Heats on Bottom	Tons Per Heat	Remarks
0	11-19-51	12-21-52	70% Al_2O_3 Large 9" Strt.	Hi-Alumina Heat Setting	3	18	10½	50	2810	2025	23	88		Old Shell. See note 1.
1	1-2-52	2-22-52	Same	Same	3	18	10½	55	2810	3548	35	101		New Shell. See note 2. Front half bottom replaced after 18 heats.
2	2-25-52	3-21-52	Same	Same	2	18	10½	50	2810	1654	20	83		Installed 6½" burned bridge 3-8-52. Tap 2850 F, 3-20-52.
3	3-24-52	4-18-52	60% Al_2O_3 Large 9" Strt.	Same	2	52	9½	60	2850	1684	20	84		Started using Thermocouples in 2 locations. See note 3.
4	4-21-52	5-16-52	60% Al_2O_3 9" Strts.	Same	2	38½	8½	70	2850	2015	21	96		Bottom up 12:00 N. Heat 21. From right side over thermocouple.
5	5-19-52	6-6-52	Same	None	2	38½	8½	75	2850	1457	15	97		Bad joint between bottom and side wall back of left side tap holes.
6	6-9-52	6-25-52	Same	None	2	38½	8½	85	2850	1049	11	95		Bottom up 2:19 p.m. Heat 11. Bottom bowed up across furnace at tap hole are slag fillet.
7	6-30-52	7-24-52	70% Al_2O_3 Large 9" Strt.	Hi-Alumina Heat Setting	2	38½	8½	85	2850	1732	18	96		Slag reaction noticed.
8	7-28-52	9-11-52	Same	Same	2	38½	8½	80	2850	2347	30	78		Slag treatment. Short heats and work weeks.

Notes: 1. Old Shell—Bottom composed of 3 courses sub-bottom each 4½" thick, 4" sand, 9" service bottom. Total 26½".
2. New Shell—Bottom composed of 1" Ganister on Shell, 2 courses sub-bottom each 4½" thick, 2" sand, 9" service bottom. Total 21".
3. Chromel-Alumel Thermocouples set in Chromel T sealed tubes. Front couple set in drain row. Tip centered between side walls in sand ½" below service bottom, 46" forward of bottom tap hole and 8'5½" from burner well. Back couple set in drain row similar to front couple but 42" back of bottom tap hole.

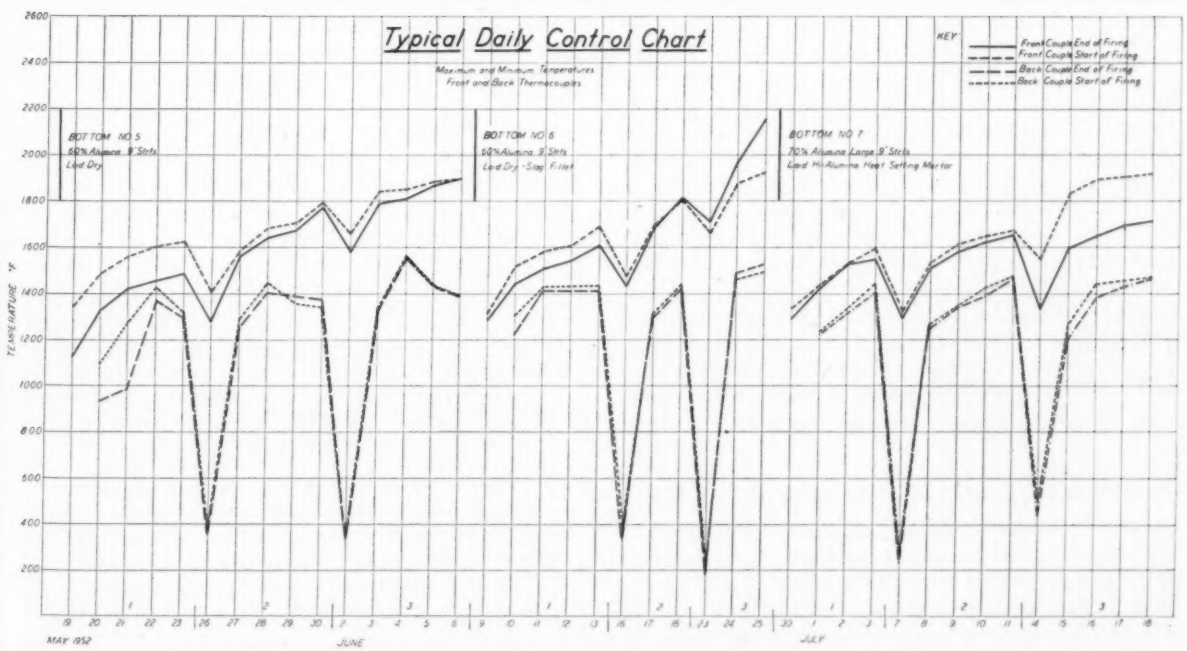


Fig. 8—Typical daily control chart showing maximum and minimum temperatures recorded on front and back thermocouples.

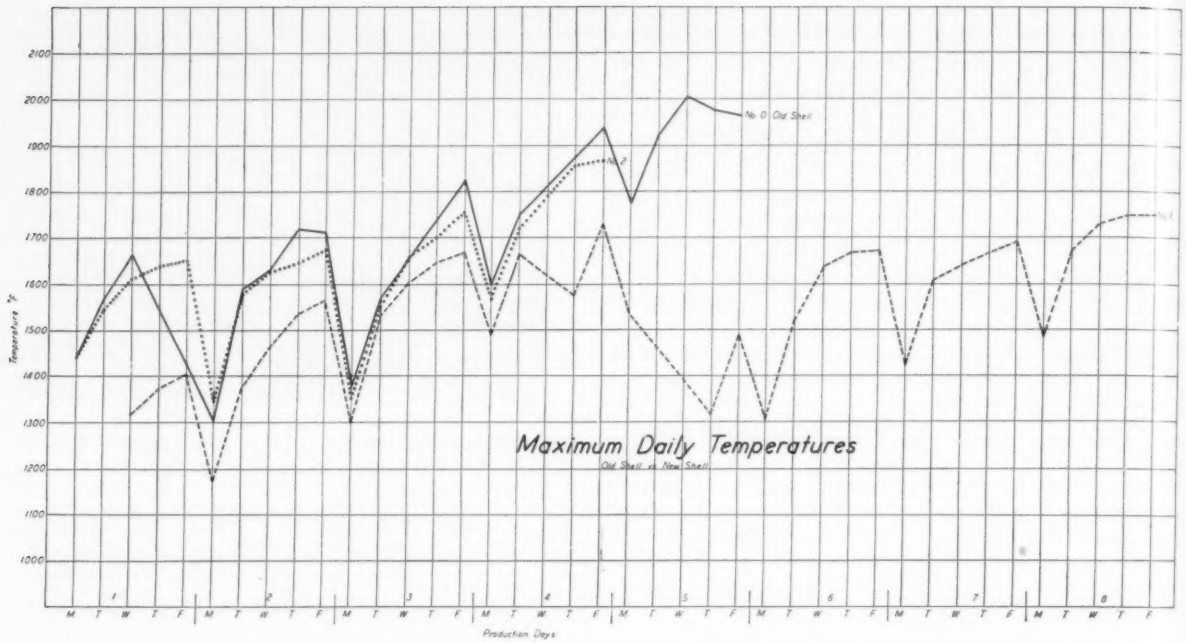


Fig. 9—Graph of maximum daily temperatures comparing "old shell" and "new shell" construction.

daily temperatures it can be seen that Bottom No. 1 ran about 150 F cooler the first three weeks of operation than did No. 0. This was felt to be partly due to the increased ventilation and partly due to the fact that the entire refractory in the furnace was new and at room temperature at the start of the campaign. The break in the curve during the fourth week of operation was due to a one day shut-down. After one heat in the fifth week, the furnace was shut down to replace the front half of the bottom. By doing so, it can be seen that the furnace bottom lasted 35 heats and reached a maximum temperature of 1750 F (954 C). The graph of No. 1 shows the effect on temperature of frequent interruption of the five day week. Decision to end this particular campaign was based on visual observations alone.

Bottom No. 2 more closely indicates the real effect of better bottom ventilation and shows an average

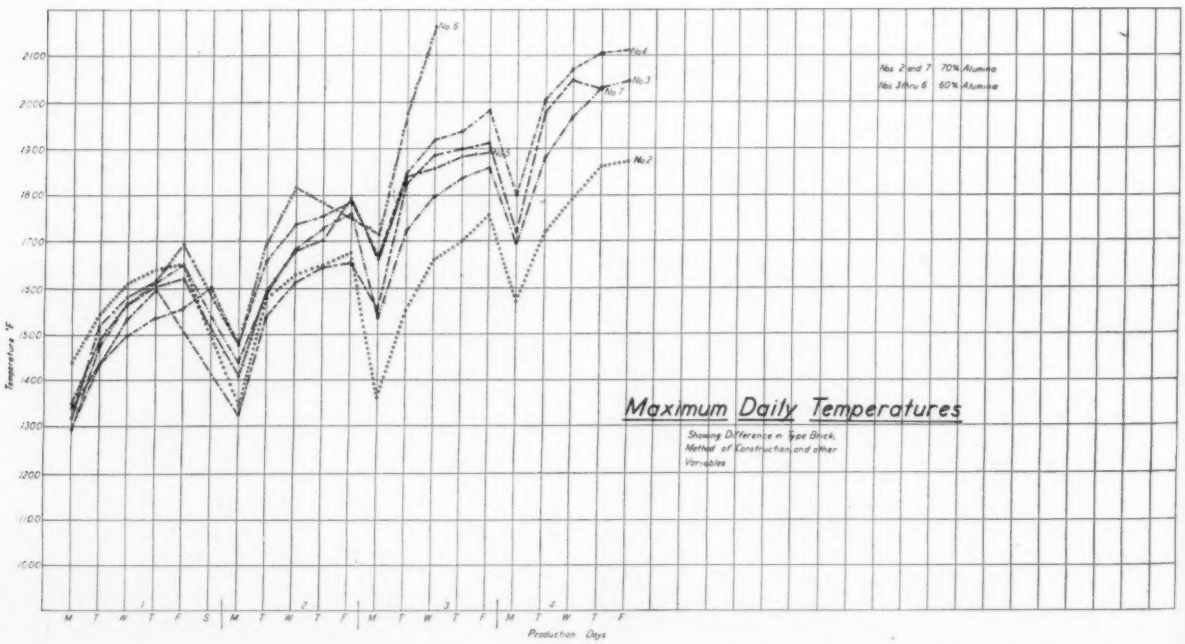


Fig. 10—Graph of maximum daily temperatures showing effect of type and size bottom brick, method of construction, and other variables as listed in Table 1.

weekly maximum temperature approximately 50 F less than similar runs on No. 0. The sharp upswing of temperature during the third and fourth week of operation was no doubt due to the installation of a longer burner bridge after the second week of operation. The new bridge was installed to find out if the absence of a burner bridge caused excess oxidation of the bath near the burner wall. This was disproven and the length of the bridge was reduced on successive bottoms. The new burner bridge came within 40 in. of the thermocouple and resulted in faster wear of the bottom brick in the front half of the furnace, thus increasing the retained heat at the point of measurement. The change of the rise from the center of the bottom to the sidewall from 3 in. to 2 in. was felt to have no effect on recorded temperatures. This flattening of the bottom was done by using less sand between the bottom and sub-bottom adjacent to the sidewalls, and the purpose being to prevent excess erosion of the bottom near the sidewalls.

Comparison of Type Brick

Figure 10 illustrates in graphic form the effect of the above mentioned variables in the test program. The graph for Bottom No. 2 serves as the master pattern for comparison of results on successive bottoms. As mentioned before, starting with Bottom No. 3, two thermocouples, front and back of the tap holes were used to measure temperatures. Temperatures plotted in Fig. 10 represent the maximum for the day whether they be front or back thermocouple readings.

The graph for No. 3 bottom as compared to No. 2 shows the comparison of using 60 per cent alumina large 9 in. brick against using 70 pct alumina large 9-in. brick. Each campaign lasted 20 heats and Bottom No. 3 reached a maximum temperature of 2050 F (1121 C), approximately 190 F hotter than maximum on No. 2. Although No. 3 reached about the same maximum temperature as No. 2 after the first weeks run, it failed to cool down as much over the week end and each subsequent week indicated an increasingly wide variation in maximum temperature. This variation is felt to be caused by four factors: first, the difference in type brick; second, the increase of burner bridge length; third, the slight increase of outside average temperatures; and fourth, the increase of furnace tap temperature. The only factor in favor of lower retained heat on this bottom would be the decrease of depth of metal bath. Bottom No. 3 was torn out because of a bad cracking and spalling condition of the brick.

Comparison of Size Brick

Comparing Bottom No. 4 to Bottom No. 3, the principle change made was to use regular 9-in. bottom brick in place of the large 9-in. brick as formerly used. As noted by the graph, Bottom No. 4 ran almost 60 F hotter than No. 3 and did so for the last 3 weeks of the campaign. Significant is the fact that even though the maximum temperature of No. 4 reached at the end of the first week was less than that of No. 3 the 6 day operation on No. 4 prevented the bottom from cooling to as low a temperature over the week end as did No. 3. Of the variables, the length of burner bridge and depth of bath was less, the outside temperature slightly higher, and the tap temperature the same, which collectively should have made for a cooler bottom. Thus the main reason for No. 4 running higher in temperature than No. 3 lies in the size brick itself. The bottom laid with the smaller brick having more joints to erode, more chance for metal penetration, should have and did reach a higher temperature. Bottom No. 4 had a bad spalling and cracking condition as did No. 3 and came up at 12:00 noon on the 21st heat at a point directly over the front thermocouple. Temperature recorded on this thermocouple a few minutes before the bottom floated was 2111 F (1154 C).

Comparison of Method of Laying Bottom Brick

Bottom No. 5 was different from No. 4 only in that this bottom was laid dry and swept with sand. Temperature wise it ran a similar course to No. 4 except that the hot spot developed at the back thermocouple area. Checking against operation of the furnace it was felt that the length of flame on this run was greater than that on No. 4 as verified by sidewall burnout and was sufficient to move the zone of greatest heat to the rear of the tap hole area. Although the temperature of Bottom No. 5 was less at the end of 3 weeks than that recorded on No. 4, the bottom had a bad cobblestone effect, more so than did No. 4 as well as a bad joint between the bottom and side walls back of the left tap holes. It was therefore decided to be safe and remove it.

Effect of Cupola Slag on Bottom

Bottom No. 6 was laid identical to No. 5 except that a fillet of granulated cupola slag was placed at the junction of bottom and side walls on the theory that the slag would melt upon firing and flow down over the bottom thus sealing the joints. As seen in Fig. 10, this bottom deteriorated fast and by the third week had developed such a bad cobblestone condition and had started to crack and spall so bad that it came up during the 11th heat at 2:19. The temperature reached a maximum of 2160 F (1182 C). This happened in spite of the fact that the second week on this bottom had only three production days which would normally allow for a big drop in bottom temperature. The temperature recorded on Monday of the third week on this bottom was actually higher than any previously recorded, this fact signalling serious trouble ahead as substantiated by the very sharp rise in maximum temperatures the next two days.

Check Comparison on 70 Pct Alumina Bottom Brick

Bottom No. 7 was laid identical to No. 2 with exception of the length of burner bridge which was greater, and the depth of bath which was less. The outside temperature was higher as was the tap temperature. At a glance it would be expected that this bottom should run at a slightly higher temperature than did No. 2. Even though No. 7 ran only 3 days

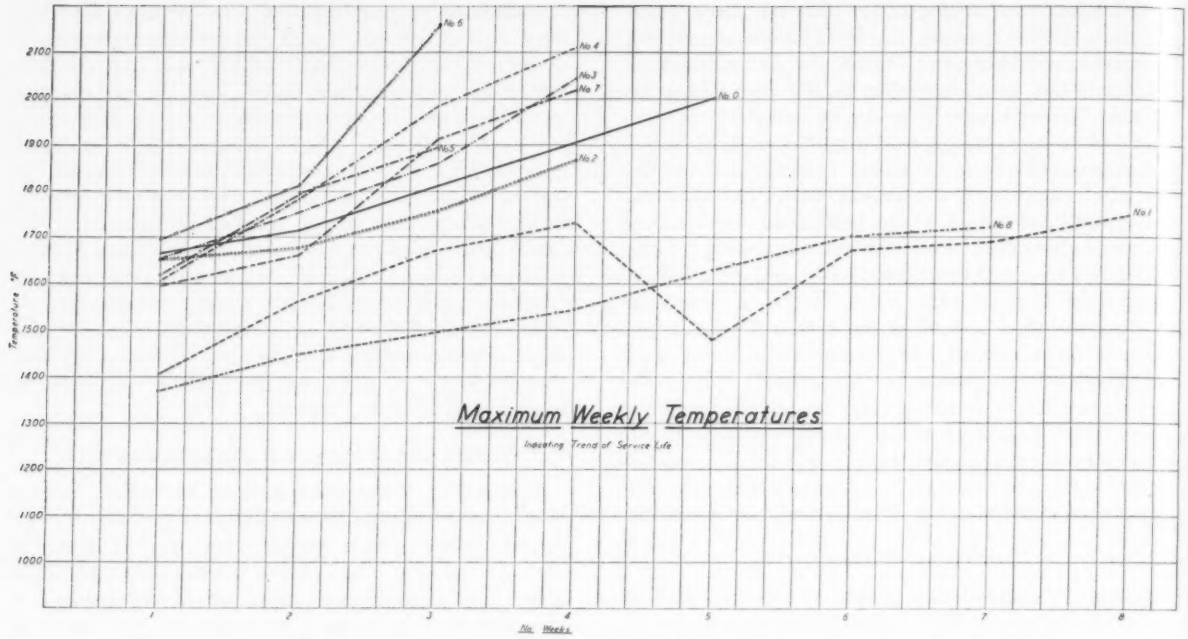


Fig. 11—Graph of maximum weekly temperatures indicating trend of service life on bottoms.

the first week while No. 2 ran 5 days, the temperatures recorded on both No. 7 and No. 2 bottoms were nearly the same after one heat on the second week, and remained nearly the same the balance of the second week. The sharp departure in temperature of No. 7 from No. 2 occurred early the third week and remained such for the balance of the campaign. Bottom No. 7 developed a large crack which ran lengthwise in the center of the back half of the furnace. This was accompanied by a bad spalling condition. The high temperature, this time being recorded on the back thermocouple, was no doubt due to this condition.

Summary of Initial Test Results

Significant in the above described series of tests is the fact that the maximum temperatures attained on both Bottom No. 4 and No. 6, each of which came up during a heat, were over 2100 F (1149 C). This represents a danger point in bottom temperature op-

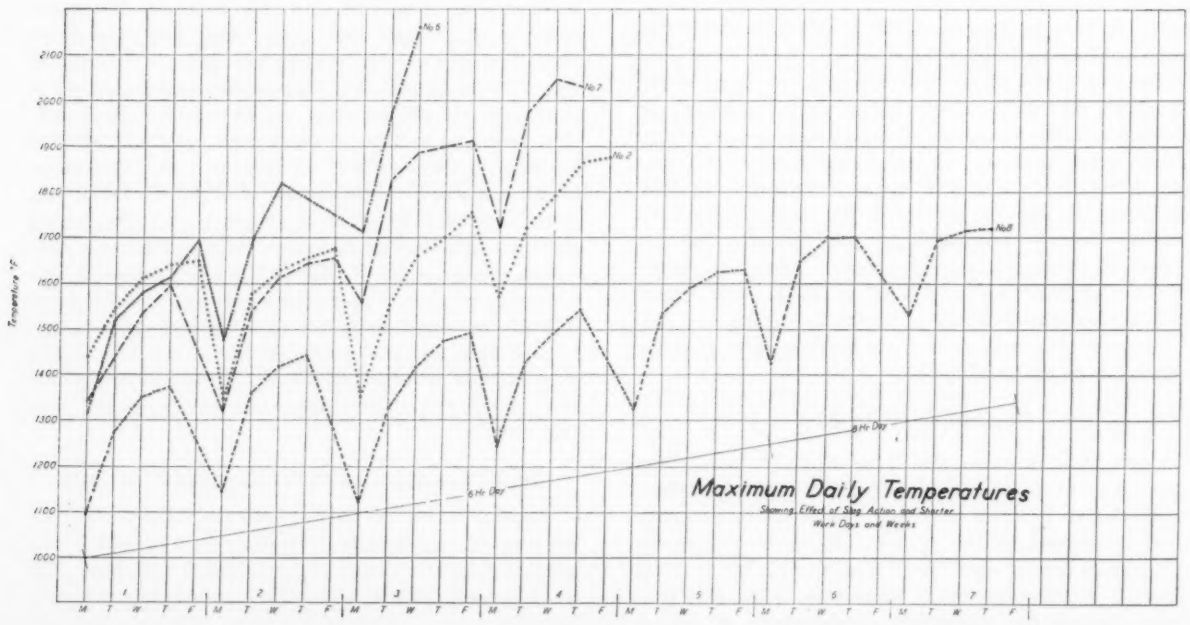


Fig. 12—Graph of maximum daily temperatures showing effect of slag action and short work days and weeks on bottom life.

eration and has led the authors to set a temperature of 2000 F (1093 C) as the maximum to be reached before a bottom is taken out provided that other prevailing bottom conditions appear normal by visual check.

A useful illustration for indicating the trend of service life on a bottom is shown in Fig. 11 where maximum weekly temperatures of all bottoms covered in this paper are plotted against the number of weeks of operation. This graph clearly indicates that 60 pct alumina bottom brick in general as represented by lines for No. 3 through No. 6 bottoms attains a higher maximum weekly temperature than does 70 pct alumina brick. Also indicated is the fact that regular 9-in. straight bottom brick attain a higher temperature than do large 9-in. straight bricks as represented by lines for No. 4, 5 and 6 Bottoms compared to line for No. 3 Bottom.

Disregarding the graph for No. 1 Bottom which does not represent true repetitive operation, and disregarding graph for No. 8 Bottom which will be described later in this paper, it is seen that all the bottoms attained a maximum temperature after the first week of operation somewhere between 1600 F (871 C) and 1700 F (927 C). Generally after the second week of operation and definitely after the third week of operation, the life of the bottom can be forecast to a reasonable degree by observing the nature of the trend and by watching for any sharp departure upward from the normal weekly rise in temperature experienced to date on any particular bottom. Thus, for example, Bottom No. 4 after the third week of operation indicated a sharp rise in temperature over Bottom No. 3. Bottom No. 6 indicated after the second week that it was headed for trouble. After the third week, Bottom No. 7 was seen to take a sharp departure from No. 2.

Advantages of Slag Control on Furnace Bath

All experimentation from Bottom No. 1 through No. 7 seems to have led to adverse conditions, each bottom progressively becoming worse as far as maximum temperature attained and operation procedures were concerned. Observing the behavior of Bottom No. 6 where cupola slag was deliberately placed as a fillet on the bottom, it was apparent that the cupola slag actively attacked the bottom bricks at their joints. Bottom No. 7 had a decided departure from the behavior of Bottom No. 2 due to the large lengthwise crack which developed in the back half of Bottom No. 7 during the last two weeks of operation. As mentioned early in this paper, the standard practice in the melting department had been to cover the furnace bath at the end of the cupola operation with molten cupola slag direct from the cupola. This procedure was now felt to be responsible for the heavy erosion occuring in the back half of the bottom in that during the drain period of the furnace, this cupola slag came to rest directly on the bottom and was largely concentrated along the centerline of the furnace. This conclusion was further justified because the cupola slag produced during the period of No. 7 Bottom was much higher in corrosive CaO content than the cupola slag which was produced during the period of No. 2 Bottom, this in turn being due to a change in cupola practice during the interval between Bottom No. 2 and Bottom No. 7.

As a result of these conclusions the practice of adding molten cupola slag to the furnace both was stopped and beginning with Bottom No. 8 a synthetic slag practice was adopted resulting in a remarkable change of bottom behavior as may be seen in Fig. 12. It is not intended to bring the manipulations of synthetic slag control on the furnace bath within the

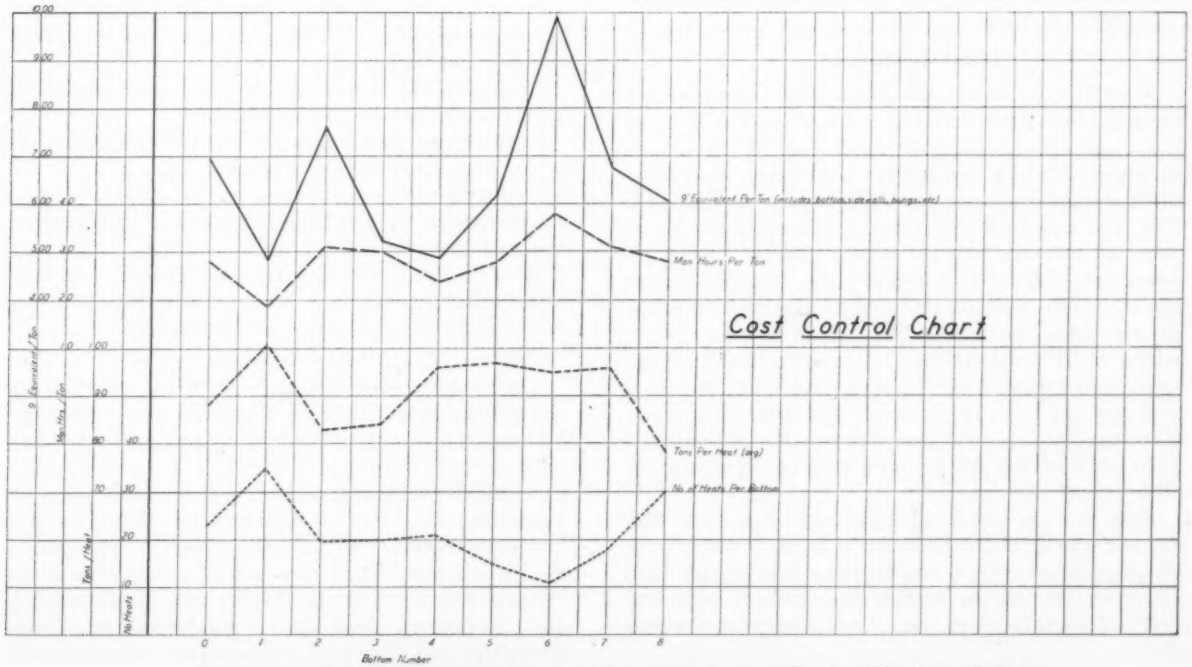


Fig. 13—Graph comparing cost figures with average tons per heat and heats per bottom.

scope of this paper other than to point out that broad possibilities do exist in this field and to mention that the authors are now working along these lines in relation to bottom temperatures. Existing data and future findings will be presented in a future paper.

Advantages of Intermittent Operation

The graph of Bottom No. 8 in Fig. 12 brings to light another important factor. Five of the 7 operating weeks were 4 day weeks and 5 of the 7 weeks were limited to 6 hr days. This has definitely been a factor in keeping the maximum daily temperatures throughout the entire run of No. 8 at a low level. Other examples of this have already been pointed out in discussion on previous bottoms. After a 3 or 4-day shut down a bottom was seen to drop to a much lower temperature than usual. In the case of Bottom No. 4 after a one day shut down over Sunday, the bottom lost comparatively little heat. This suggests the value in maintaining two furnaces in the melting department to be operated alternately thereby keeping maximum bottom temperatures low on each furnace with the benefit of longer bottom life.

TABLE 2—COST CONTROL DATA

Bottom No.	No. Heats	Total Tons	Tons Per Heat	Man-Hrs. Per Ton	9-in. Equivalents Per Ton
0	23	2025	88	.28	6.96
1	35	3548	101	.19	4.86
2	20	1654	83	.31	7.65
3	20	1684	84	.30	5.21
4	21	2015	96	.24	4.88
5	15	1457	97	.29	6.23
6	11	1049	95	.38	9.92
7	18	1732	96	.31	6.76
8	30	2347	78	.28	6.04

Note: Figures in last 2 columns represent labor and material for service bottoms, sidewalls, bungs, tap block assembly and replacement during life of bottom of any of these component parts.

Cost Comparison

A careful record of costs throughout the entire experiment was kept and is presented in Table 2. It is evident at once by referring to Fig. 13 that the 9-in. equivalent per ton followed the same pattern as did the man-hours per ton. This was to be expected. The low cost of Bottom No. 1 reflects the replacement of only one half the bottom during 7 weeks of operation. The cost of Bottom No. 4 is also low as the side walls were not replaced for the entire 4 weeks of operation. Such was the case of Bottom No. 3 which can be compared to Bottom No. 2 where side walls were replaced after 2 weeks operation, the total tonnage and heats being nearly the same for the two bottoms. Savings made on Bottom No. 8 were not fully realized due to the low tons per heat during that period.

Costs per ton including labor and refractories during the course of the experiment from Bottom No. 1 through Bottom No. 8 averaged at a lower figure than did costs per ton on Bottom No. 0 which itself represented slightly lower costs than average for the year 1951.

A comparison was made in Fig. 13 of tons per heat versus number of heats per bottom and indicates no positive correlation between the two. The tons per heat and 9-in. equivalents per ton do not always compare inversely as might be expected, this probably being due to factors such as excessive side wall or bung replacement.

Conclusions

The work presented in this paper must be considered as a preliminary study in the field of bottom temperature measurement and results obtained must necessarily be checked and rechecked for clarification of many points which in a study of this sort is time consuming. Nevertheless the authors wish to present the following conclusions based on comparisons made thus far.

1. Measurement of bottom temperatures during the course of a campaign is a definite aid in predicting bottom life.
2. The ability to predict bottom life through use of bottom temperatures will aid foundries having only one furnace in getting maximum life from a furnace bottom without fear of the bottom coming up during the middle of a production week.
3. Ventilated air furnace bottoms promote longer bottom life by reason of less retained heat in the bottom brick resulting in slower vitrification and wear of the brick.
4. 70 pct alumina bottom brick are superior to 60 pct alumina brick in that vitrification and wear is slower as proven by resultant lower bottom temperatures when using 70 pct alumina brick.
5. The large 9-in. bottom brick is superior to the regular 9-in. bottom brick because of less joint exposure and wear which results in lower bottom temperatures.
6. Bottoms laid with high alumina heat setting cements are superior to bottoms laid dry and swept with sand by reason of less chance for slag and metal attack in the joints resulting in lower bottom temperatures and longer life.
7. Cupola slag definitely attacks bottom brick and lowers life expectancy of the bottom.
8. When the bath must be covered with slag, synthetic slags are superior to cupola slags.
9. Intermittent operation of an air furnace lengthens the life of the bottom by reason of lower bottom temperatures.
10. The use of ventilated air furnaces together with bottom temperature measurements and slag control will allow appreciable saving in cost.

Acknowledgment

The authors wish to express their appreciation to the management of Texas Foundries, Inc. for their permission to carry out this program; to R. A. Witschey and C. J. Yeager of the A. P. Green Firebrick Co. for their technical advice; to Earl Ashley, Texas Foundries, Inc., for his assistance in obtaining tests and data required; to the Photographic Department of this company for its assistance in preparing illustrations presented in this paper; and to all melting and laboratory department employees at Texas Foundries, Inc. for their patience and wholehearted support.

DISCUSSION

Chairman: C. H. WYMAN, Burnside Steel Foundry Co., Chicago.

Co-Chairman: R. A. WITSCHEY, A. P. Green Fire Brick Co., Chicago.

Recorders: R. A. WITSCHEY and W. A. JAESCHKE.

ERIC WELANDER: [1] Is it not true that different types of brick would have different physical properties, particularly thermal conductivity, so that the curve characteristics would differ between different kinds of brick?

CO-CHAIRMAN WITSCHEY: It is true that density, thermal conductivity, etc. will vary appreciably between different types of brick. The differences, however, between the 60 and the 70 per cent alumina fire clay types as referred to specifically in this paper are not large and certainly within the range of other variables that are recognized in this paper.

MEMBER: What type brick do you consider best?

MR. JACOBS: As to size, we feel that the large 9-in. series bricks are best for our purpose. As to type, we have found 70 per cent alumina best suited. We believe that a combination bottom may be the answer. In other words, on a portion of the bottom, probably the back third, where the degree of wear is less, a large 9-in. 60 per cent alumina brick could be used together with a large 9-in. 70 per cent alumina brick in the front two thirds. Wear down would be more even with the same total life and would allow savings in cost of the bottom brick.

W. R. JAESCHKE: [2] This paper is evidence of the fine painstaking work Mr. Jacobs and his associates have accomplished. However, the problems and their solutions were special and peculiar to their oil-fired furnace. Before any other operators attempt slag control in their air furnaces, I would like to caution them that oil firing causes a much higher rate of carbon reduction and part of the problem in this furnace practice was to avoid excessive carbon loss, whereas in the average pulverized coal-fired duplexing air furnace, the problem is to maintain maximum carbon reduction and that is possible only by maintaining a clean bath of iron, free from a slag blanket.

L. R. JENKINS: [3] We have two air furnaces which are enclosed to the ground. There is no circulation of air under the bottom. The furnace is fired 22½ to 23 hr per day. At the end of the third consecutive day of firing, the heel of service bottom brick near the center of the furnace will reach 2095 F. The heel will cool only to 1850 F before it is fired again the next day. We have to deliver 2860-2880 F iron at the spout. To accomplish this the furnace is fired with a high flame temperature. New bottoms were installed after every five consecutive days of operation. Extreme joint penetration was encountered.

We now fire the furnaces on alternate days. A furnace is fired one day and allowed to cool the second day, then it is fired again the third day. The maximum temperature at the heel of the service bottom brick reaches 1800-1900 F.

Bottoms have been removed after 27 days of operation and still appear to be in good condition. They were fired about 530 hr. We do not make a practice of running a bottom till it comes up, we would rather replace it too early than too late.

We have experienced good bottom life with both 40 per cent and 60 per cent alumina brick. At the present time we are trying a 70 per cent alumina brick. It is felt that we do not have sufficient information to correlate bottom life with the various grades of refractories.

It appears that the joint penetration may not be due to thermal expansion and contraction as much as the reheat contraction of the refractory material. The reheat contraction would be greater if the refractory is held at high temperature for prolonged periods of time. Some high alumina refractories have a reheat expansion rather than contraction.

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[1] Metallurgist, Union Malleable Iron Works, East Moline, Ill.
[2] Consulting Engineer, The Whiting Corp., Harvey, Ill.
[3] Metallurgist, Wagner Malleable Iron Co., Decatur, Ill.

RAMMING OF MOLDING SANDS

By

Harry W. Dietert* and Alex L. Graham**

ABSTRACT

The authors made a survey of the effect of squeeze ramming pressures on the physical properties of four production molding sands.

The molding sands were squeeze rammed with pressures ranging from 1 to 400 psi on 2-in. diam specimens and 1 to 1200 psi on 1⅛-in. diam specimens. The green, dry, air-set and high temperature properties were determined on specimens prepared under various known squeeze pressures and the data are presented as a guide for further research in high pressure molding and as a reference for foundrymen using conventional ramming methods.

Introduction

Ramming of sands in the foundry under present day practice is essentially an art as there are not enough data available for a truly scientific approach to the problem. Foundrymen at large appreciate the importance of correct mold ramming and some thought has already been directed to high pressure ramming or molding. Thus, information on the behavior of sands under different ramming loads is pertinent in furthering our scientific knowledge about sands and molding procedures.

To cover the present day fields of practice and thinking, the authors tested a group of four sands using specimens prepared by squeeze ramming under pressures ranging from 0 to 400 psi. As an index, one may consider a squeeze pressure of 50 to 60 psi as average foundry practice.

The data demonstrate how each sand property was affected as ramming load was increased, and indicate the extent to which each property of a sand is developed by ramming. One must remember that many of the sand properties are developed by ramming and are not necessarily inherent in the sand.

Method of Test—In this study, the specimens were prepared by squeeze ramming, except for a limited number of butt rammed specimens prepared in accordance with A.F.S. specifications.

The squeeze method of ramming was secured with a Dietert sand press as illustrated in Fig. 1. The sand press uses compressed air for power and will accurately apply any predetermined squeeze ramming pressure up to 1500 psi on a 1⅛-in. diam specimen and up to 450 psi on a 2-in. diam specimen. A secondary spring assembly was added to the unit in order to secure the desired accuracy for load applications of less than 10 lb.

The specimen tube floats on the stripping post during the ramming operation with the sand press, producing double-end ramming of the sand specimen.

The physical property tests on the sand specimens were made in accordance with A.F.S. specifications when available.

Sands Under Investigation—The selection of a representative group of sands that would be most illustrative to the largest group of foundrymen received considerable study. The final selection consisted of sands in actual production in an aluminum foundry, two iron foundries and one steel foundry. The aluminum and one of the iron foundry sands were naturally bonded while the other iron foundry sand and the steel foundry sand were synthetic.

The grain distribution of these sands is illustrated in Fig. 2. The distribution of the sand grains in all of these sands is of normal expectancy.

The test data of the green properties of the sands under investigation are tabulated in Tables 1, 2, 3 and 4.

Green Hardness vs Ramming Pressure — Many foundrymen wholly gauge the degree of ramming by green (mold) hardness. This is an excellent practical gauge for the degree of ramming.

The green hardness of a sand increases as the squeeze pressure is increased up to 1000 psi. This is illustrated in Figs. 3 and 4. The green hardness-squeeze pressure graph, shown in Fig. 3 is for the 2-in. × 2-in. specimen while Fig. 4 is for the 1⅛-in. × 2-in. specimen.

The squeeze pressure required to reach a predetermined green hardness is not the same for various sands. Sands reaching the predetermined green hardness with the lower squeeze pressure may be classed as better ramming sand in respect to rammability.

The ease or order of rammability expressed in psi pressure required to reach 75 green hardness on 2-in. and 1⅛-in. specimens for the selected group of

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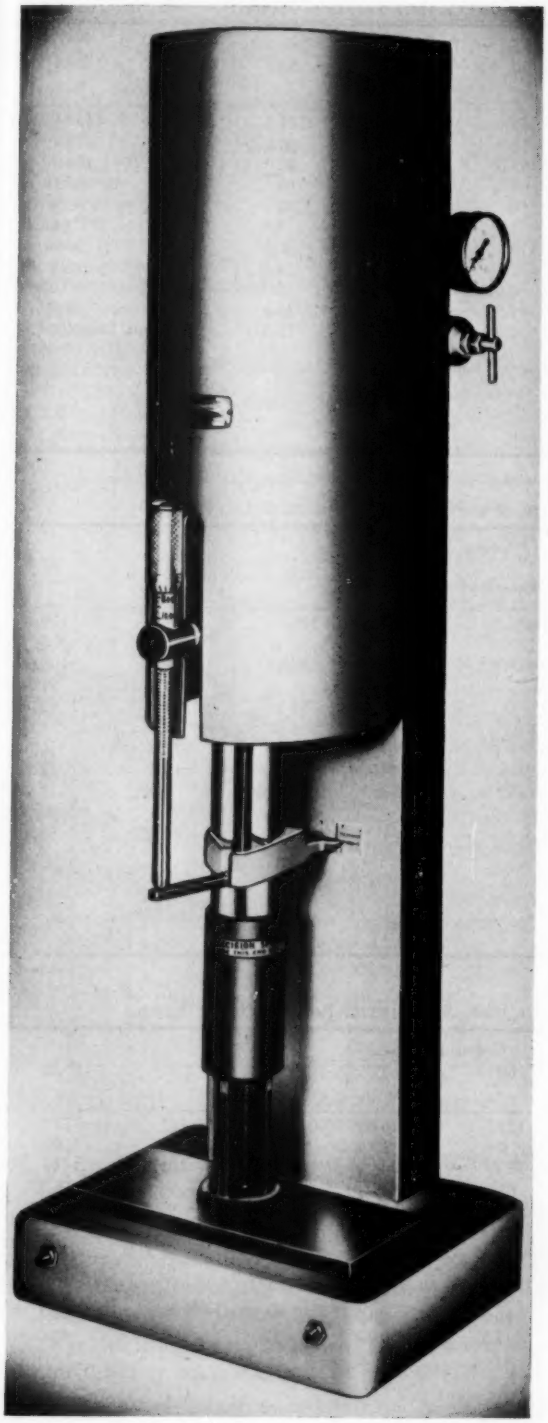


Fig. 1—Sand Press Used to Ram Sand Samples with Squeeze Pressure, Ranging up to 1500 psi.

sands is shown in Table 5.

The order of rammability is not materially different for the 2-in. or the 1⅛-in. specimen. However, the degree of rammability shows a wider spread in the case of the 1⅛-in. specimen.

The data tabulated in Table 5 shows that the

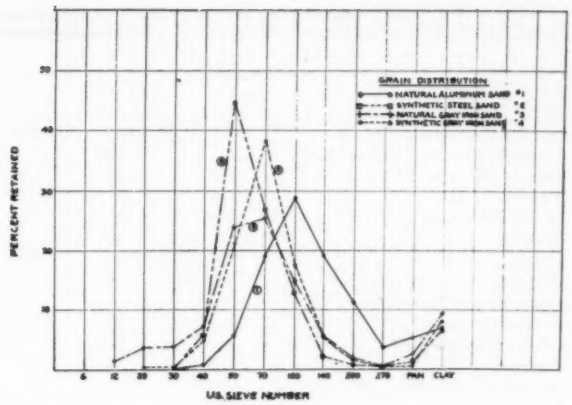


Fig. 2—Fineness Graphs of Four Sands Used in the Investigation.

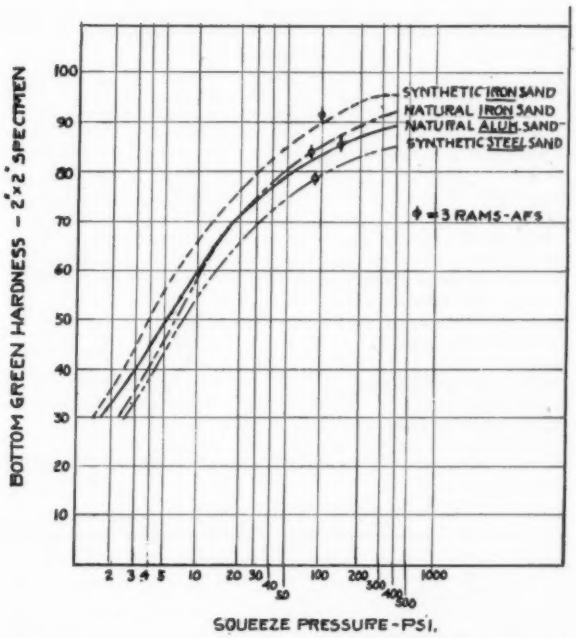


Fig. 3—Green Hardness as Affected by Squeeze Ramming Pressure, 2-in. x 2-in. Specimen.

squeeze pressure required to obtain a predetermined green hardness in the 1⅛-in. specimen is materially higher for all sands tested except the synthetic iron sand. The synthetic steel required a squeeze load of 53.8 psi to obtain a 75 green hardness in the 2-in. × 2-in. specimen and a 120 psi squeeze load to obtain a 75 green hardness in the 1⅛-in. × 2-in. specimen. Thus, in the order of decreasing rammability we have: synthetic iron foundry sand, natural iron foundry sand, natural aluminum foundry sand and a very low rammability for the synthetic steel foundry sand.

Rammability may be defined as ease of reaching a good green hardness in a restricted area of a mold. Flowability may be defined as absence of void spaces larger than that compatable with the grain size of the sand. Thus, rammability and flowability are not

TABLE 1—TEST DATA FOR GREEN PROPERTIES OF THE NATURALLY-BONDED ALUMINUM SAND.

	Green Hardness							
	3 Rams A.F.S.	30	45	60	75	80	87.5	89.5
Wt. for 2-in. specimen, gm	158	116.3	125	135	146.5	151.8	160	166
Pressure for 2-in. specimen, psi		1.59	4.2	10.9	29.7	56.8	191	376
Density, lb/cu ft	95.7	70.4	75.7	83.6	88.8	92.0	96.9	100.5
Top Hardness	86.5	30	46	61	75	80	87.5	89.5
Bottom Hardness	86.5	30	45	60	75	80	87.5	89.5
Moisture, %	3.8	3.8	3.8	3.8	3.8	3.8	3.8	3.8
Permeability	64.3	411	287	173	107	85.2	49.9	38.9
Green Compression, psi	9.1	0.51	1.0	2.45	4.6	6.4	10.0	12.1
Green Deformation, in./in.	0.013		0.018	0.0155	0.0155	0.0135	0.0125	0.0155
Dry Compression, psi	62.5	2.13	5.5	14.9	35.4	45.8	63.7	66.0
Air-Set Strength, psi	24.9	1.43	3.1	7.3	12.9	17.9	25.1	30.0
Flowability, %	75.5							
Hoffman Flowability, gm	4.55							
Combustibles, %	2.2							
A.F.S. Grain Fineness No.	96.0							
A.F.S. Clay Content, %	7.0							

TABLE 2—TEST DATA FOR GREEN PROPERTIES OF THE NATURALLY-BONDED IRON SAND.

	Green Hardness						
	3 Rams A.F.S.	30	45	60	75	90	92
Wt. of 2-in. specimen, gm	163.6	125.2	132.5	141	153.5	173.5	179.4
Pressure for 2-in. specimen, psi		2.26	5.1	10.9	30	221	376
Density, lb/cu ft	99.1	75.8	80.3	85.4	93.0	105.1	108.6
Top Hardness	85.0	31	46	61	75	90.5	91.5
Bottom Hardness	84.5	30	45	60	75	90	92
Moisture, %	5.6	5.6	5.6	5.6	5.6	5.6	5.6
Permeability	118	650	503	355	204	72.4	55.8
Green Compression, psi	9.1	0.48	1.2	2.33	4.8	15.2	17.5
Green Deformation, in./in.	0.0193		0.0182	0.0183	0.0205	0.022	0.0245
Dry Compression, psi	47.7	1.91	5.9	12.6	18.85	73.5	99.0
Air-Set Strength, psi	25.8	2.65	4.6	10.0	14.0	42.95	52.2
Flowability, %	67						
Hoffman Flowability, gm	4.55						
Combustibles, %	5.7						
A.F.S. Grain Fineness No.	51.0						
A.F.S. Clay Content, %	9.2						

TABLE 3—TEST DATA FOR GREEN PROPERTIES OF THE SYNTHETIC BONDED IRON SAND.

	Green Hardness							
	3 Rams A.F.S.	30	45	60	75	90	95	95.25
Wt. for 2-in. specimen, gm	157.0	116	122	129.5	137.5	156	167	171.5
Pressure for 2-in. specimen, psi		1.4	3.18	6.8	19.5	93	252	376
Density, lb/cu ft	95.1	70.3	73.9	78.4	83.2	94.4	101.1	104
Top Hardness	91.5	31	47.5	61	76	91	95	95.25
Bottom Hardness	90.5	30	45	60	75	90	95	95.25
Moisture, %	3.2	3.2	3.2	3.2	3.2	3.2	3.2	3.2
Permeability	147	306	645	467	313	144	87.2	71.2
Green Compression, psi	18.7	0.44	0.9	2.5	6.1	18.6	27.9	31.1
Green Deformation, in./in.	0.0178		0.018	0.015	0.0145	0.018	0.017	0.016
Dry Compression, psi	65.0	0.88	2.9	10.0	20.9	74.6	84.5	106.1
Air-Set Strength, psi	36.75	0.6	2.4	7.3	10.8	32.1	42.9	47.0
Flowability, %	70.8							
Hoffman Flowability, gm	3.4							
Combustibles, %	4.1							
A.F.S. Grain Fineness No.	58.0							
A.F.S. Clay Content, %	8.2							

identical. In Table 5 it may be noted that both the Dietert[1] and Hoffman flowability show some agreement. Both show that the order of flowability is substantially opposite to the order of rammability.

Density vs Squeeze Pressure — It was found that green hardness increased rapidly with an increase of squeeze pressure. The graph in Fig. 5 shows that density (lb per cu ft) increases with an increase of green hardness. The density increases at a faster rate at high green hardness. Thus, a 10 point increase of green hardness in the upper range will cause a heavier sand than 10 points increase in the lower range of hardness. The synthetic iron foundry sand is the lightest sand of the group and it also has the

TABLE 4—TEST DATA FOR GREEN PROPERTIES OF THE SYNTHETIC BONDED STEEL SAND

	Green Hardness							
	3 Rams A.F.S.	30	45	54.5	60	75	85	86
Wt. of 2-in. specimen, gm	165	123.5	136.5	142.6	146	163.8	174.6	178.5
Pressure for 2-in. specimen, psi		2.39	5.6	9.2	13.2	53.8	301.5	376
Density, lb/cu ft	100.0	74.8	82.7	86.4	88.4	99.2	105.8	108.1
Top Hardness	80	31	46	56	61	75	85	86.5
Bottom Hardness	78.5	30	45	54.5	60	75	85	86
Moisture, %	4.6	4.6	4.6	4.6	4.6	4.6	4.6	4.6
Permeability	154	911	586	435	362	164	85	78
Green Compression, psi	5.4	0.73	1.4	2.2	2.6	4.7	8.4	8.9
Green Deformation, in./in.	0.0465		0.042	0.0375	0.035	0.046	0.044	0.0465
Dry Compression, psi	243.0	44.7	83.7	124.3	145.8	254.4	353	375
Air-Set Strength, psi	31.5	3.0	9.9	14	16.9	22.3	45.4	48
Flowability, %	75.2							
Hoffman Flowability, gm	9.2							
Combustibles, %	3.3							
A.F.S. Grain Fineness No.	52.7							
A.F.S. Clay Content, %	6.6							

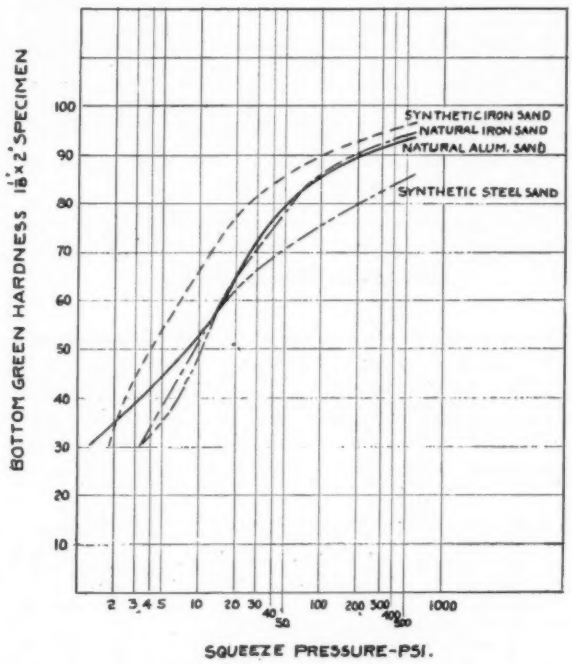


Fig. 4—Green Hardness as Affected by Squeeze Ramming Pressures, 1⅛-in. x 2-in. Specimen.

TABLE 5—RAMMABILITY AND FLOWABILITY DATA OF THE ALUMINUM, IRON AND STEEL SANDS.

Sands	Green Hardness	Squeeze Pressure 2-in. x 2-in. Specimen	Squeeze Pressure 1⅛-in. x 2-in. Specimen	Grams, Hoffman Flowability	Percentage, Dietert Flowability
Synthetic Gray Iron	75	19.5	16	3.4	70.8
Natural Gray Iron	75	30	36.5	4.55	67.0
Natural Aluminum	75	29.7	35.5	4.55	75.5
Synthetic Steel	75	53.8	120	9.2	75.2

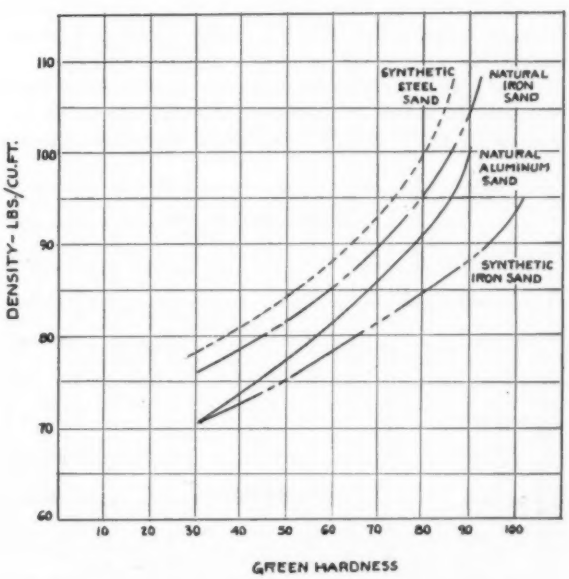


Fig. 5—Density as Affected by Squeeze Ramming Pressure.

best rammability, while the heaviest sand in the group is the synthetic steel foundry sand possessing the poorest rammability on squeeze ramming.

The relationship between density and squeeze pressure for the four sands is shown in Figs. 6, 7, 8 and 9.

The density of a sand increases rapidly at the beginning of a squeeze ram. Thus a sand compacts very rapidly with the first 40-psi squeeze load and thereafter the rate of density increase is moderate. Three A.F.S. rams compact the sands well past the range of rapid rise in density.

The synthetic iron foundry sand with the better rammability reached a maximum density at 320 psi squeeze pressure. All of the other sands showed increasing densities at 360 psi squeeze pressure.

Green Properties vs Squeeze Pressure—The permeability of a sand drops rapidly as ramming pressure is first applied. Three A.F.S. rams compact the sands just past the range of the rapid permeability drop. In plant practice, using for example, 50 psi squeeze ramming pressure, the permeability of all four sands is still dropping rapidly. See Fig. 6, 7, 8 and 9. A

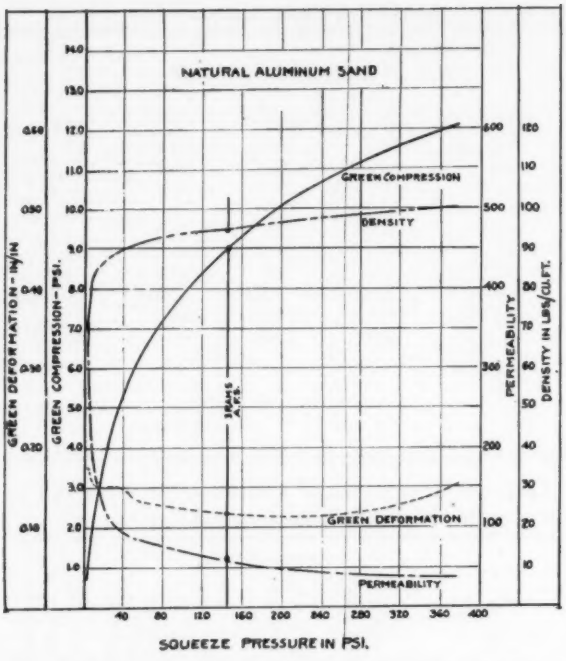


Fig. 6—Effect of Squeeze Ramming Pressures on Green Properties of Naturally-Bonded Aluminum Foundry Sand.

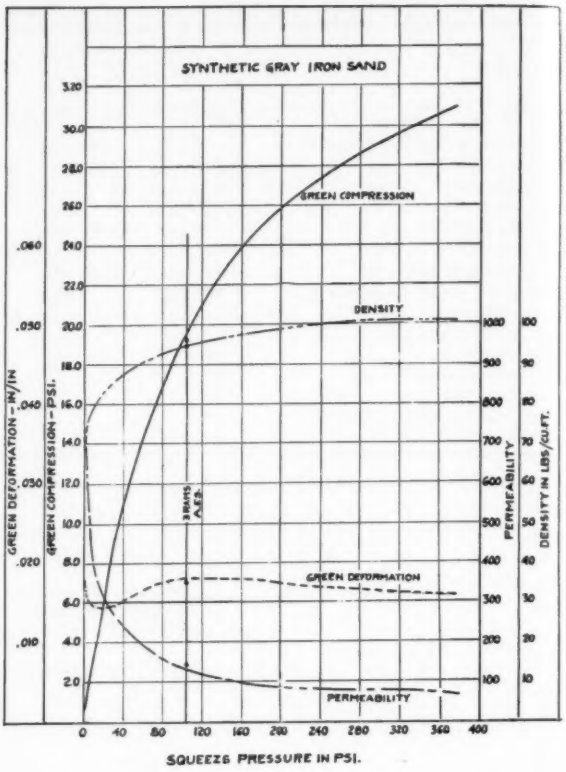


Fig. 8 (Right)—Effect of Squeeze Ramming Pressures on Green Properties of Synthetic Iron Foundry Sand.

small change in ramming force will mean a great change in permeability. At higher pressure of ramming the rate of permeability drop decreases.

The green compressive strength of a molding sand steadily increases as squeeze ramming pressures are increased up to the 376 psi where tests were discontinued. See Figs. 6, 7, 8 and 9. High pressure molding offers a means of obtaining a workable strength with a low percentage of clay substance. The higher the pressure the lower the clay substance content may be for a predetermined green compressive strength. When green sand is rammed with a very

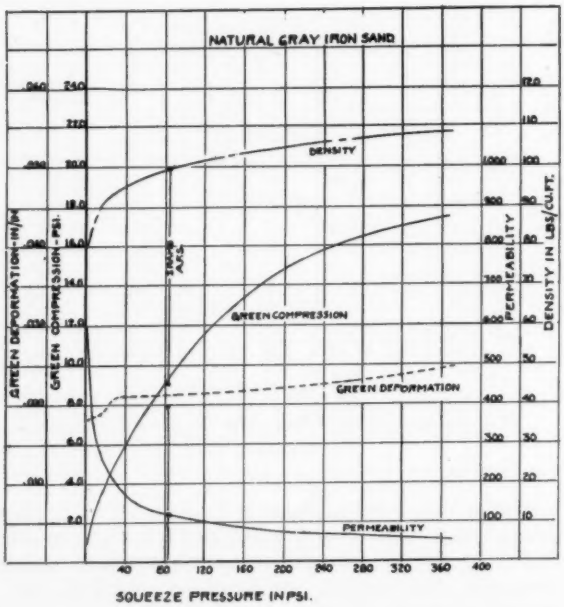


Fig. 7—Effect of Squeeze Ramming Pressures on Green Properties of Natural Iron Foundry Sand.

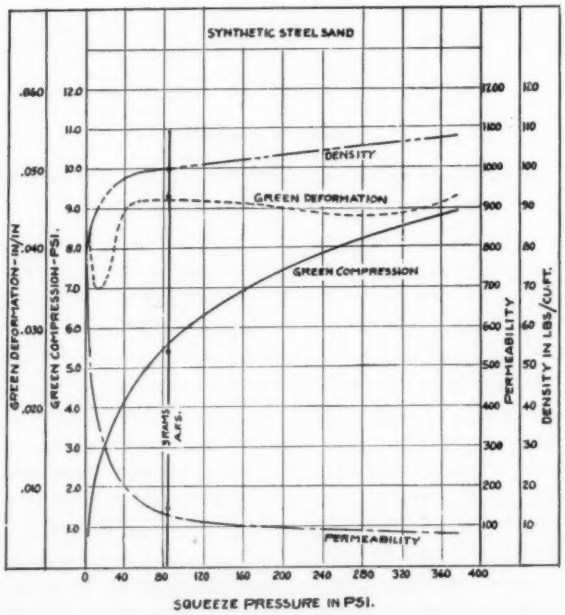


Fig. 9—Effect of Squeeze Ramming Pressures on Green Properties of Synthetic Steel Foundry Sand.

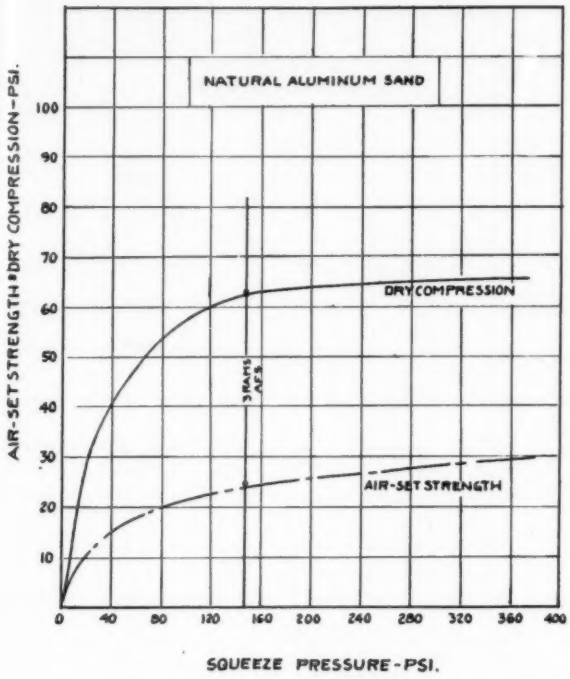


Fig. 10—Effect of Squeeze Ramming Pressures on Air-Set Strength and Dry Compressive Strength of Naturally-Bonded Aluminum Foundry Sand.

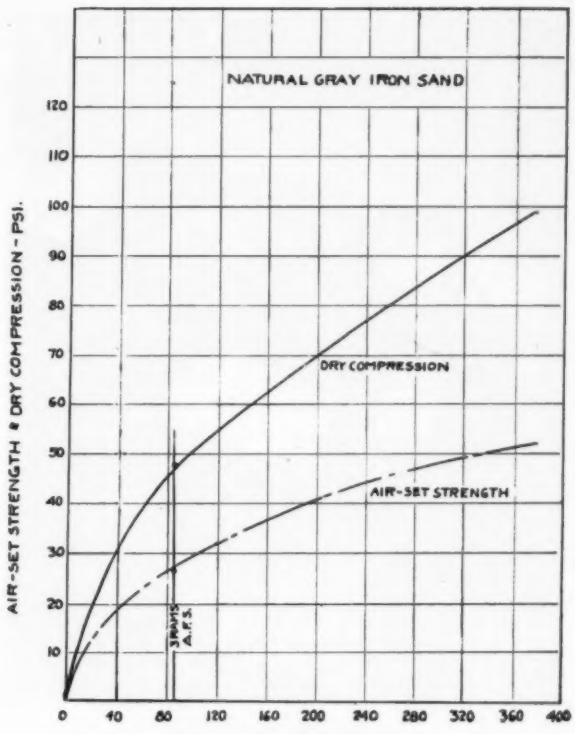


Fig. 11—Effect of Squeeze Ramming Pressures on Air-Set Strength and Dry Compressive Strength of Naturally-Bonded Iron Foundry Sand.

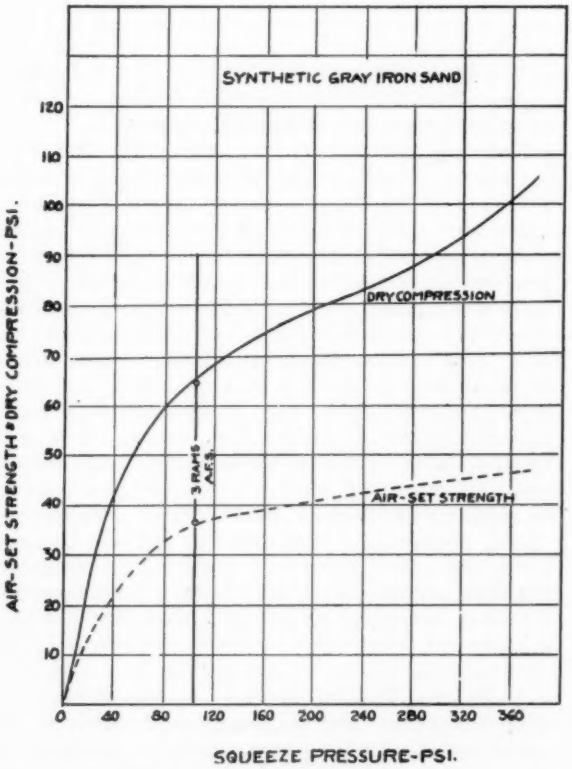


Fig. 12—Effect of Squeeze Ramming Pressures on Air-Set Strength and Dry Compressive Strength of Synthetic Iron Foundry Sand.

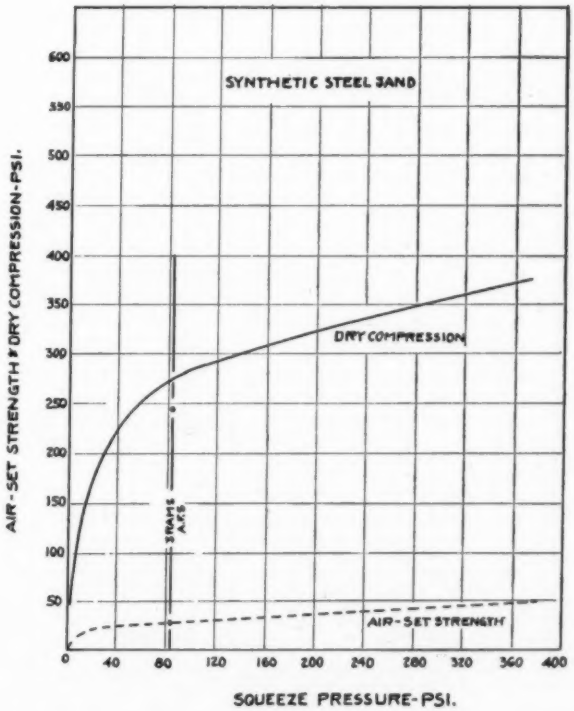


Fig. 13—Effect of Squeeze Ramming Pressures on Air-Set Strength and Dry Compressive Strength of Synthetic Steel Foundry Sand.

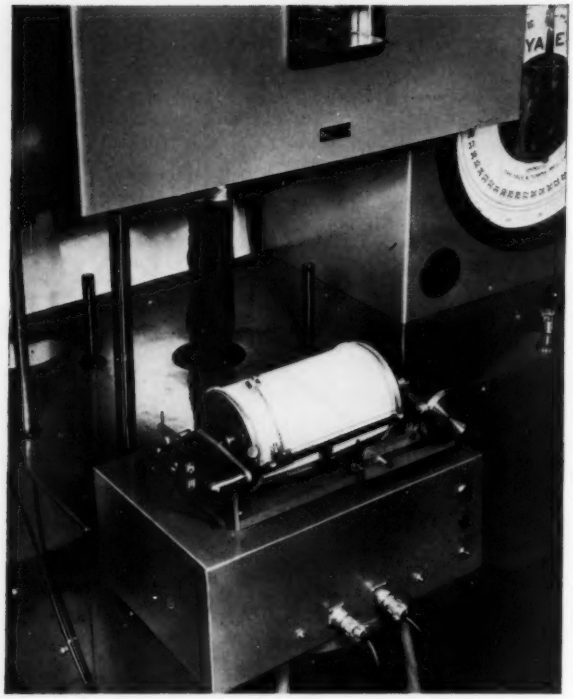


Fig. 14—Hot Strength-Deformation Recorder.

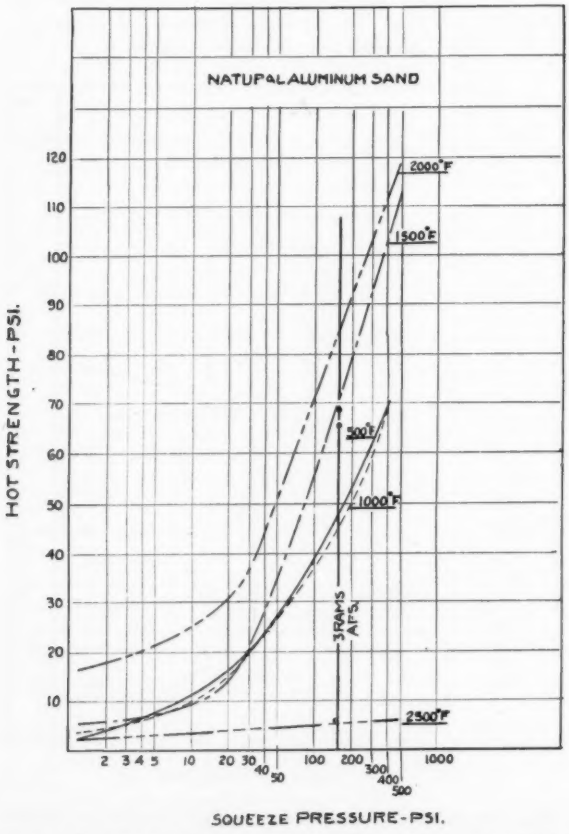


Fig. 15—Effect of Squeeze Ramming Pressures on Hot Strength of Naturally-Bonded Aluminum Foundry Sand.

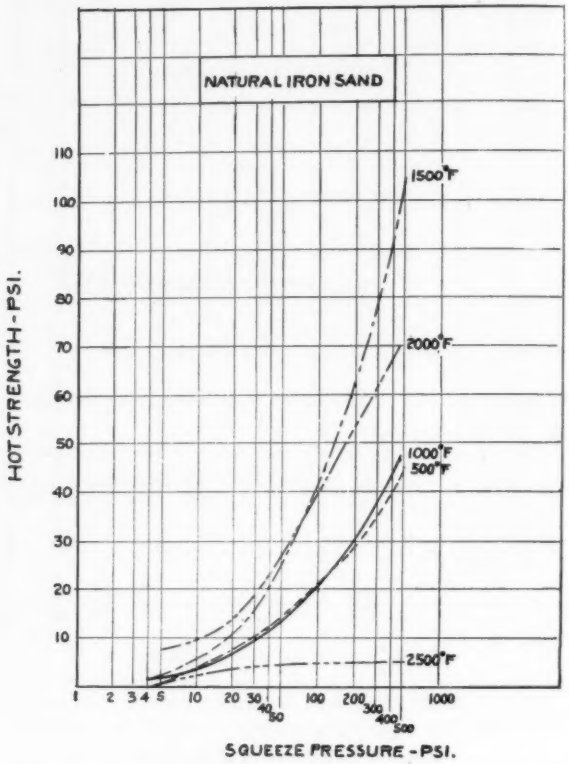


Fig. 16—Effect of Squeeze Ramming Pressures on Hot Strength of Naturally-Bonded Iron Foundry Sand.

high pressure, for example, 50,000 psi, the water will separate from the sand. Dry bonded sand rams better at such pressures than green sands.

The green deformation (plasticity) of a sand does not change greatly as the ramming pressure is increased providing the first stage of ramming is passed.

The first stage of ramming may generally be defined between 0 to 40 psi. In this range the green deformation changes markedly. The behavior of the green deformation during the first stage of ramming is not the same for all sands. The synthetic iron and steel sands have a low deformation or brittle range at 15 to 20 psi ramming pressure. Such low ramming pressures could easily be obtained in pockets of molds and cause trouble. The natural iron foundry sand also has a low deformation at low ramming pressures while the natural aluminum sand has a decreasing deformation, as ramming pressure increases until a very high ramming pressure is used. Green deformation does not respond to changes in ramming pressure as much as other green properties do. It is interesting to note that sand does not lose its plasticity when high ramming pressures are employed.

Air-Set and Dry Properties vs Squeeze Pressure — Both the air-set and dry compressive strengths increase rapidly as the squeeze pressure is increased in the range from 0 to 80 psi. The A.F.S. ram is just out of the rapid change section, see Fig. 10, 11, 12 and 13. Since standard A.F.S. ramming is greater than the usual foundry practice, most molds in a

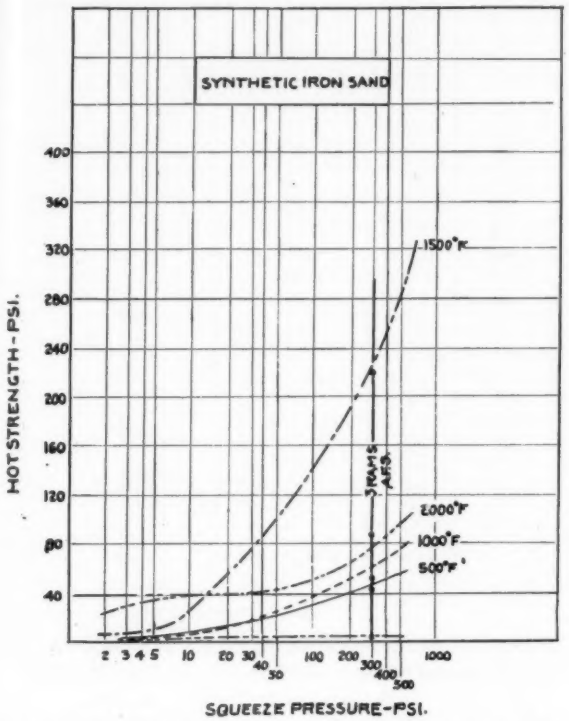


Fig. 17—Effect of Squeeze Ramming Pressures on Hot Strength of Synthetic Iron Foundry Sand.

foundry are rammed within this rapid changing section. This causes wide fluctuations in air-set and dry strength of the sand in a mold, for relatively small changes in ramming energy or green hardness.

Hot Strength vs Squeeze Pressure—The hot properties of the sands were determined with a dilatometer and a hot strength-hot deformation recorder as illustrated in Fig. 14.

The hot strength of sands, conversely to many other sand properties, increases slowly during the first stage of ramming. The hot strength increases rapidly when squeeze pressures are increased to higher pressures. This is probably caused by more sand grains making contact with each other.

In conventional foundry practice, using 50 psi or better squeeze pressure, the molds are rammed on the steep gradient of the hot strength curve, see Figs. 15, 16, 17 and 18. This means that great fluctuations in the hot strength, 500 to 2000 F inclusive, of mold faces may be expected with relative small change in green hardness. The hot strength at 2500 F is not materially affected by a change in ramming pressure on green hardness, except for the steel sand the hot strength increases rapidly for the first 50 lb of squeeze load.

The importance of devising ways and means of uniformly distributing ramming pressures to all parts of a mold is of great importance. More thought should be given to the problem of actually delivering uniform ramming pressure to all faces of mold cavity.

Hot Deformation vs Squeeze Pressure — Foundry sands when heated to elevated temperatures possess a softness or plasticity termed "hot deformation." The hot sand grains can actually move without fracturing the bond since the bond becomes plastic to a limited or to a great degree depending upon the temperature and nature of the bond.

The amount of hot deformation that a sand actually possesses may be measured at different temperatures and loads in a dilatometer equipped with a hot deformation recorder, as illustrated in Fig. 14. Both the compressive load and the hot deformation are recorded on a chart from which may be read the load, hot deformation per inch of specimen length and also, the rate of hot deformation.

Soft rammed molds are brittle since they possess low hot deformation. This is illustrated in Fig. 19, 20, 21 and 22. Note that for ramming pressure 10 psi and below the hot deformation drops to low values, except for the steel foundry sand which shows a high hot deformation at low green hardness or low squeeze pressures. The natural iron sand actually requires 30 psi ramming pressure to reach a normal value. See Tables 6, 7, 8 and 9.

Low hot deformation means that the sand surface does not possess plasticity to accommodate sand movement as caused by the expansion of the sand. This is conducive to mold wall fracture.

After normal ramming pressures have been reached hot deformation of a sand does not change markedly.

For those interested in high pressure molding, it

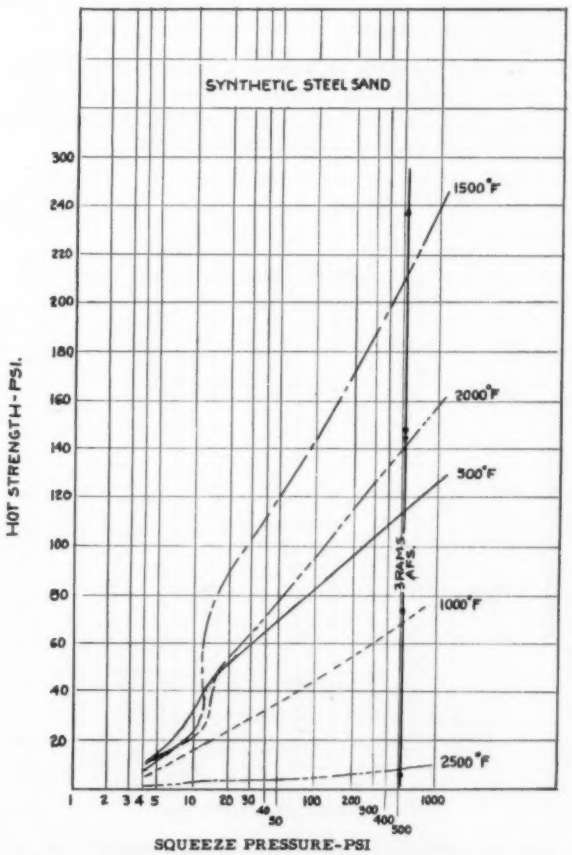


Fig. 18—Effect of Squeeze Ramming Pressures on Hot Strength of Synthetic Steel Foundry Sand.

TABLE 6—TEST DATA FOR HOT PROPERTIES OF THE NATURALLY-BONDED ALUMINUM SAND.

	3 Rams A.F.S.	Green Hardness 30	45	60	75	92.5
Wt. of 1⅛-in. diam specimen, gm	51.9	38.0	40.5	43.3	47.5	53.0
Pressure for 1⅛-in. specimen, psi		1.2	5.2	15	35.5	430
Top hardness	89	30	46	60	75	92
Bottom hardness	90	30	46	60	75	92.5
Retained strength, psi	30.0	1.08	2.0	5.0	10.0	36.6
Hot strength @ 500 F, psi	67.6	2.3	7.6	13.5	24.2	70.0
Hot Deformation @ 500 F, in./in.	.0069	.0023	.0035	.0037	.0048	.0058
Hot strength @ 1000 F, psi	70	3.5	7.7	13.7	21	70
Hot deformation @ 1000 F, in./in.	.0092	.0027	.0053	.0065	.0093	.0093
Hot strength @ 1500 F, psi	114	5.8	7.6	11.0	25	112
Hot deformation @ 1500 F, in./in.	.0138	.0065	.0065	.011	.010	.0132
Hot strength @ 2000 F, psi	109	15.6	22.0	27.0	40	119
Hot deformation @ 2000 F, in./in.	0.435	.0295	.0317	.035	.0425	.0457
Hot strength @ 2500 F, psi	5.75	2.4	3.2	3.8	4.2	6.0
Hot deformation @ 2500 F, in./in.			Plastic			

seems that no trouble is to be expected from the hot deformation catagory.

Retained Strength vs Squeeze Pressure—The energy required to shake out a mold is directly proportional to the retained strength. The higher the retained strength the greater the shake-out effort.[2]

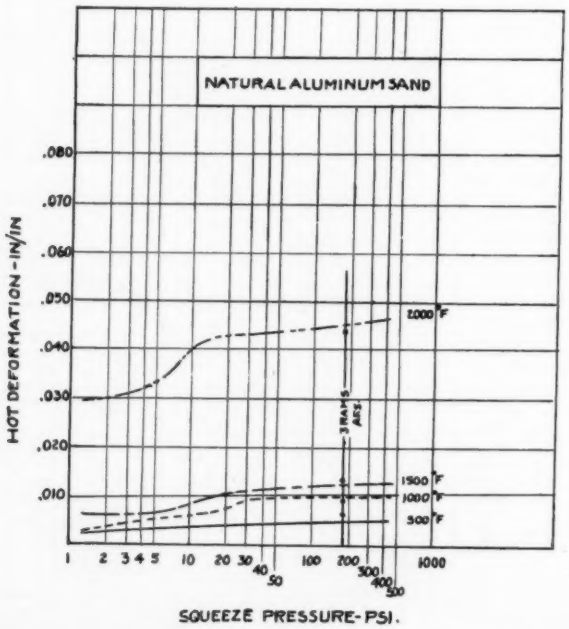


Fig. 19—Effect of Squeeze Ramming Pressures on Hot Deformation of Naturally-Bonded Aluminum Foundry Sand.

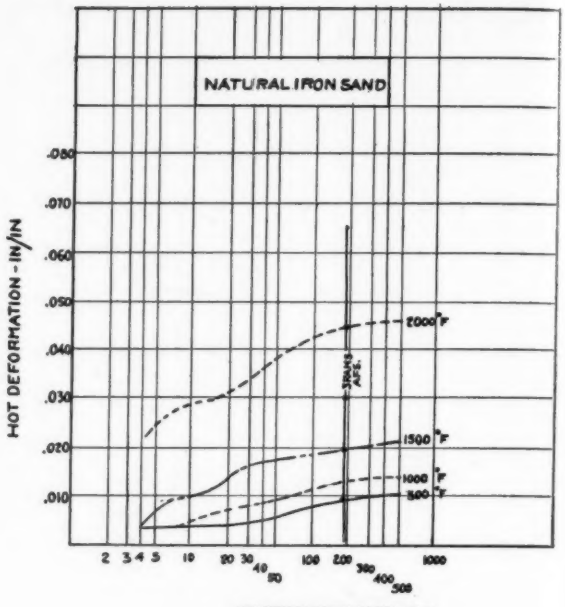


Fig. 20—Effect of Squeeze Ramming Pressures on Hot Deformation of Naturally-Bonded Iron Foundry Sand.

The graphs in Fig. 23 illustrate the effect of squeeze ramming pressure on the retained strength. It may be noted that the retained strength of all four sands are low for the first stage of ramming and increase rapidly as ramming pressure enters the normal ramming pressure. The retained strength of all the sands increases with an increase of ramming pressure up to 400 psi where tests were discontinued. Indica-

TABLE 7—TEST DATA FOR HOT PROPERTIES OF THE NATURALLY-BONDED IRON SAND.

	3 Rams A.F.S.	Green Hardness 30	45	60	75	93.5
Wt. of 1⅛-in. Diam Specimen, gm	54.7	39.8	42.2	44.9	48.2	57.0
Pressure for 1⅛-in. specimen, psi		4.2	7.5	15	36.5	515
Top Hardness	88	33	49	61	76	93.5
Bottom Hardness	90	30	45	60	75	93.5
Retained Strength, psi	26.3	0.0	0.0	2.2	5.2	32.3
Hot Strength @ 500 F, psi	40	2.3	4.4	6.5	12	45.3
Hot Deformation @ 500 F, in./in.	.0097	.0033	.0034	.0042	.005	.0103
Hot Strength @ 1000 F, psi	34	0.0	1.7	4.5	10.2	47.5
Hot Deformation @ 1000 F, in./in.	.0136		.0034	.007	.0085	.0139
Hot Strength @ 1500 F, psi	63	2.0	4.5	8.5	19.8	104
Hot Deformation @ 1500 F, in./in.	.0198	.0034	.0091	.011	.0169	.021
Hot Strength @ 2000 F, psi	55.0	6.3	9.0	11.5	23.5	73
Hot Deformation @ 2000 F, in./in.	.0451	.0222	.0284	.0291	.0359	.046
Hot Strength @ 2500 F, psi	5.5	0.0	2.0	4.0	4.8	5.8
Hot Deformation @ 2500 F, in./in.			Plastic			

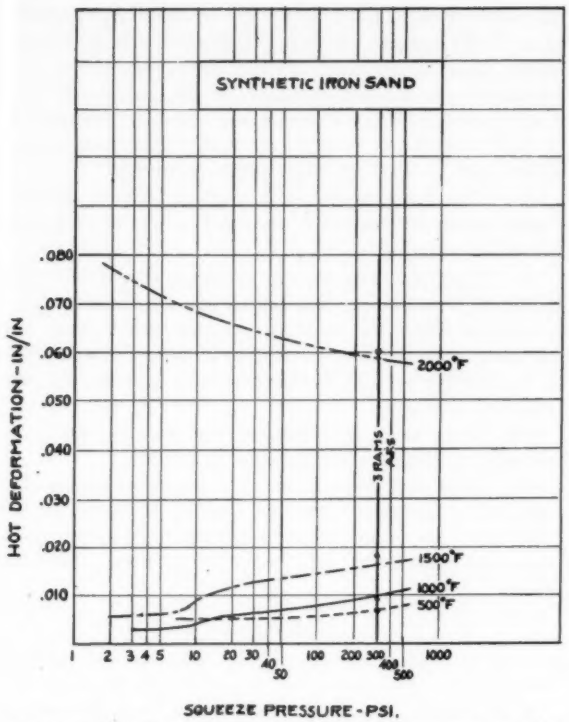


Fig. 21—Effect of Squeeze Ramming Pressures on Hot Deformation of Synthetic Iron Foundry Sand.

tions are that the retained strength is reaching its maximum at 400 psi and greater pressure would not materially alter the retained strength.

Summary and Conclusion

The material presented in this paper may be of practical value to foundrymen using the conventional molding methods and to those who are doing research in the high pressure molding field. Knowing the effect of a wide range of ramming pressures on the mechanical properties of sands shows whether any alterations in sands are required should high pressure molding be employed. The importance of controlling the green hardness of mold within a narrow range to obtain mold surfaces possessing con-

Table 8—Test Data for Hot Properties of the Synthetic Bonded Iron Sand.

	3 Rams A.F.S.	Green Hardness 30	45	60	75	90	96
Wt. of 1⅛-in. Diam specimen, gm	51.7	36.5	38.0	39.8	44.0	49.0	54.7
Pressure for 1⅛-in. specimen, psi		1.8	3.2	7.1	16	88.5	612
Top Hardness	94.5	33	43.5	61	75	90.5	97.0
Bottom Hardness	94.5	30	45	60	75	90	96.0
Retained Strength, psi	38	0.47	1.04	1.51	8.5	27	40.0
Hot Strength @ 500 F, psi	55	0.0	3.1	5.4	10.9	32	69
Hot Deformation @ 500 F, in./in.	.0093		.0030	.0034	.0055	.0077	.0111
Hot Strength @ 1000 F, psi	59.7	0.0	0.5	3.9	9	36	88
Hot Deformation @ 1000 F, in./in.	.011			.0049	.0049	.0106	.0122
Hot Strength @ 1500 F, psi	225	4.75	8.6	12.5	46.3	131	325
Hot Deformation @ 1500 F, in./in.	.0179	.0059	.006	.0069	.0116	.0136	.017
Hot Strength @ 2000 F, psi	88.7	25	32	37.5	40	50.3	104
Hot Deformation @ 2000 F, in./in.	.0604	.0782	.074	.0711	.0659	.0618	.0575
Hot Strength @ 2500 F, psi	5.7	2.2	2.8	3.13	4.6	5.0	6.0
Hot Deformation @ 2500 F, in./in.			Plastic				

Table 9—Test Data for Hot Properties of the Synthetic Bonded Steel Sand.

	3 Rams A.F.S.	Green Hardness 30	45	60	75	85.5
Wt. of 1⅛-in. diam specimen, gm	55.0	42.0	45.0	49.0	52.5	57.8
Pressure for 1⅛-in. specimen, psi		4.1	9.6	14.3	120	1195
Top Hardness	82	31	46.5	60	75.5	86.5
Bottom Hardness	82	30	46	59	75.5	85.5
Retained Strength, psi	63.6	8	16.7	29.7	59.3	63.3
Hot Strength @ 500 F, psi	143	11	28	46	84	130
Hot Deformation @ 500 F, in./in.	.0041	.0025	.0027	.0038	.0049	.0064
Hot Strength @ 1000 F, psi	69	7.7	16	20	45	74
Hot Deformation @ 1000 F, in./in.	.0068	.0037	.0047	.0042	.0055	.0063
Hot Strength @ 1500 F, psi	233	10	22	79	149	243
Hot Deformation @ 1500 F, in./in.	.0133	.008	.0088	.0106	.0115	.0101
Hot Strength @ 2000 F, psi	144.5	11	19.7	45	99	163
Hot Deformation @ 2000 F, in./in.	.0351	.023	.025	.0335	.0303	.0349
Hot Strength @ 2500 F, in./in.	5.4	0.8	1.8	3.5	5.7	10
Hot Deformation @ 2500 F, in./in.			Plastic			

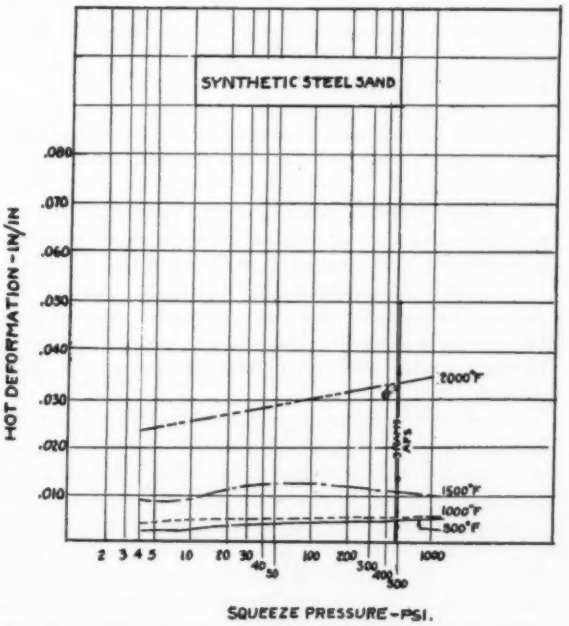


Fig. 22—Effect of Squeeze Ramming Pressures on Hot Deformation of Synthetic Steel Foundry Sand.

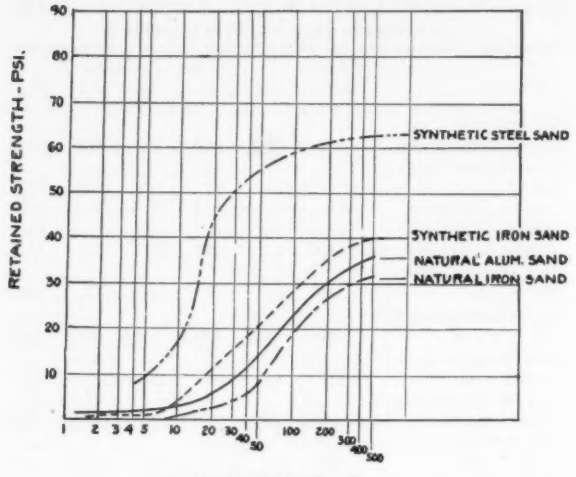


Fig. 23—Effect of Squeeze Ramming Pressures on Retained Strength.

sistent mechanical properties is well illustrated.

Summarizing:

1. Green hardness steadily increases as squeeze ramming pressure increases.

2. The density (lb/cu ft) of molding sand increases rapidly with an increase of green hardness.

3. The rammability of a sand may be measured by determining the squeeze pressure required to obtain a predetermined green hardness value.

4. The density of sands increase at a rapid rate at the first 40 psi of ramming pressures and changes more slowly at high ramming pressures.

5. The flowability of the four sands under study is in the opposite order of the rammability.

6. The permeability of molding sand decreases rapidly for the first stage of ramming in the range from 0 to 40 psi and then decreases slowly in the high pressure ramming range. High pressure molding in the ranges above 160 psi pressure will reduce permeability only moderately.

7. The green compressive strength of a sand continues on an increase throughout the ramming pressure ranges up to the limit of this test namely, 400 psi.

8. High pressure molding offers a means of materially reducing the required amount of bond to secure a predetermined compressive strength in a mold.

9. Green deformation of the mold face at soft rammed sections is very low or brittle. Any increase in ramming pressure above that usually practiced in the foundry will not materially change the green deformation.

10. The air-set strength increases rapidly at the beginning of a ram and then continues to increase as ramming pressure increases. The rate of air-set strength increase varies with different sands.

11. The first stage of ramming for example, from 0 to 40 psi, causes a rapid rise in dry compression. The rate of increase in the normal ramming pressure, such as 50 psi, and in the high pressure range varies with the different sands. Some increase slowly, others increase rapidly.

12. The hot strength of molding sands is low and increases slowly in the first stage of ramming, namely, 0 to 10 psi squeeze pressure. This gives rise to a weak mold face at elevated temperatures whenever soft rammed sections are present in the mold. In the high pressure range of ramming the hot strength of a sand increased at a rapid rate. This may cause difficulties in case of high pressure molds.

13. The hot strength of a sand at 2500 F., for the sands under investigation changed slowly as ramming pressure changed.

14. The hot deformation of the four sands under study is low causing hot brittleness at elevated temperatures when the mold face was rammed with low pressure in the range from 0 to 20 psi pressure. This is conducive to mold fracture in soft rammed sections of a mold.

15. The hot deformation increased slowly as squeeze ramming pressure was increased.

16. The shake-out time of a mold increases rapidly with ramming pressure from 0 to some 50 psi and then increases more moderately. Higher pressures of ramming will require greater shake-out effort.

References

1. H. W. Dietert and F. Valtier, "Flowability of Molding Sand," A.F.A. TRANSACTIONS, vol. 42, pp. 199-210 (1934).

2. "Evaluation of Core Knockout," A.F.A. TRANSACTIONS, vol. 55, pp. 320-324 (1947).

DISCUSSION

Chairman: J. B. CAINE, Foundry Consultant, Cincinnati.

Co-Chairman: W. S. PELLINI, Naval Research Laboratory, Washington, D. C.

Recorder: P. E. KYLE, Cornell University, Ithaca, N. Y.

D. C. WILLIAMS *(Written Discussion):*[1] It is admitted that the term ramming has existed in the foundry industry for many years to describe a process of compacting a granular mass around a pattern. Ramming indicates that impacting forces are used. The authors refer to squeezing as a ramming procedure. Can we justify the naming of a steady pressure, such as squeezing, as a ramming procedure? Should we not speak of consolidation brought about by either the ramming or squeezing procedures?

The following statement in the paper "One must remember that many of the sand properties are developed by ramming and are not necessarily inherent in the sand.", deserves further explanation. The word develop indicates an increase in the property or subject under consideration. It is thought that permeability is inherent in a sand mixture. Granular mixtures contain void spaces and permeability is related by the fact that in a compacted mass the number of through channels of connected void spaces confer permeability. Deformation is thought to be inherent in sand mixtures by virtue of the water content, and ramming apparently does not develop this property. Would the authors further describe what they consider to be the properties inherent and those properties that are developed?

This discussor is not sure what is meant by the term rammability and would like an explanation of this term. Would the rammability be the same under a circular rammer such as used in compacting the 2-in. x 2-in. test specimen as in the compacting of the 1-in. x 1-in. x 8-in. transverse test specimen under a rectangular footing? Along with the above question is the following: how are grain movement and rammability related?

The authors define rammability as "the ease of reaching a good green hardness in a restricted area of the mold." The word good can describe many conditions and this discussor wonders what is a good green hardness. Is measuring the green hardness on the surface of a test specimen an indication

[1] Associate Professor and Head of Foundry-Option, Department of Industrial Engineering, Ohio State University, Columbus Ohio.

of the flowability that occurs within the molded mass, and will such mass have a uniform density and hardness?

The terms softness and plasticity are considered by the authors to be synonymous for they state "softness or plasticity." It is difficult to follow the authors on this point and I would like them to present sound reasons why this should be so.

Flowability apparently is related to the absence of void spaces. This concept for this discussor does not seem to explain very much and it will be appreciated if the authors would provide a thorough description of their concept of the flowability.

The authors claim that the higher the pressure used for consolidation purposes the lower the clay content required to obtain a predetermined green compressive strength. This being so, then how does clay-mineral enter into the mechanism for providing green strength?

Apparently at high pressures about 50,000 psi, water will separate from the test specimen. What sand mixtures were tested and why will the water separate and where will it go?

This discussion necessarily has taken the form of a series of questions because it is thought that the authors' reply will clear up many questions in the minds of others as well as that of the discussor.

J. B. STEVENS: [2] What is the effect of squeeze pressure on expansion?

MR. DIETERT: Expansion does not increase rapidly when the high pressure range is reached.

MEMBER: How do the moistures used for high pressure molding compare with those normally used?

MR. DIETERT: They are essentially the same.

MEMBER: Are synthetic and natural sands the same?

MR. DIETERT: The hot deformation properties change with the bond used.

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[2] Bethlehem Steel Co., Bethlehem, Pa.

MAGNESIUM ALLOY PERMANENT MOLD AND SEMI-PERMANENT MOLD CASTINGS

By

M. E. Gantz, Jr., E. M. Gingerich and R. T. Woods*

Introduction

The purpose of this paper is to outline the current status of the art of casting magnesium alloys in permanent and semi-permanent molds. The past ten years have witnessed marked advances in the art of magnesium alloy casting by this process. They are now available, on a competitive basis, for the many uses in which the characteristics of high strength-weight ratio, excellent machinability and comparatively low cost are particularly desirable. While much has been learned about mold design and mold operation in recent years, the present satisfactory state of the art of founding magnesium alloys in permanent molds rests primarily on two metallurgical developments. The first was the discovery that very small amounts of beryllium added to the alloys[1] so reduced their tendency to burn and to react with the mold wash that the molten metal could be handled and cast almost as readily as aluminum. The second was the discovery that grain refinement of the metal could be accomplished by treating the molten metal with solid organic chlorides.[2] This eliminated the costly and time-consuming superheating operation previously used in our foundries to refine the grain of the metal.

Magnesium Alloys Suitable for Permanent Mold Work

The currently known magnesium alloys that have the characteristics required for the economical production of permanent mold and semi-permanent mold castings are few in number because this type of work requires the special combination of characteristics listed below:

1. Low coefficient of expansion, particularly in the range between the solidus temperature and 500 F.
2. High equi-cohesive temperature or resistance to hot cracking.
3. Adequate mechanical properties either in the as-cast condition or in a heat-treated temper.

Assuming that the alloy to be cast has satisfactory mechanical properties, the most important of the above characteristics is low coefficient of expansion or, more exactly, low coefficient of contraction. Experience has shown that alloys with very high equi-cohesive temperatures are poor for permanent mold work if, at the same time, they have high coefficients of expansion. On the other hand, if two alloys have approximately the same coefficients of expansion, the one with the lesser tendency toward hot cracking is preferable because it is not as likely to crack under stresses applied to the casting while being removed from the mold.

Of all the alloying elements which have been added to magnesium and on which coefficient of expansion data are available, aluminum is the most potent in reducing the rate at which magnesium expands or contracts on heating and cooling. Figure 1 from data determined by P. Bastien[3] shows how the linear contraction, expressed in per cent, decreases as the aluminum content of the binary magnesium-aluminum alloy system is increased from zero to about 11 per cent when cast in sand molds. Bastien reported that the linear contraction of the alloys cast in chill molds is somewhat less than that of those cast in sand. The addition of zinc to magnesium also tends to decrease linear contraction on cooling but to a smaller degree than aluminum. Also, binary magnesium-zinc alloys and ternary magnesium-aluminum-zinc alloys have a tendency toward hot cracking unless the ratio of aluminum to zinc is high and carefully controlled.

The addition of aluminum to magnesium brings

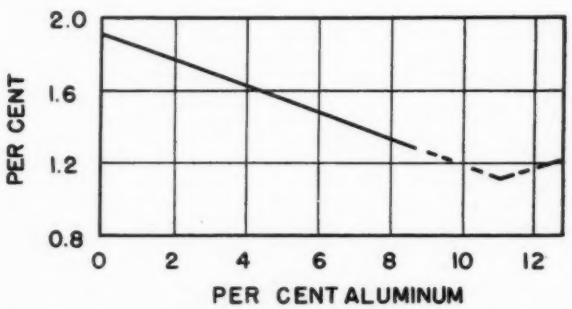


Fig. 1—Linear contraction of magnesium-aluminum casting alloy.

* Aluminum Company of America, Cleveland.

about a marked change in all the mechanical properties in the as-cast condition and the change is even more marked if the alloys are heat treated. The manner in which the mechanical properties of magnesium-aluminum alloys vary with aluminum content in both the as-cast condition and after solution heat treatment is shown in Figs. 2 and 3.[4] Other combinations of mechanical properties may be obtained by the artificial aging of the alloys containing about 8 per cent or more aluminum.

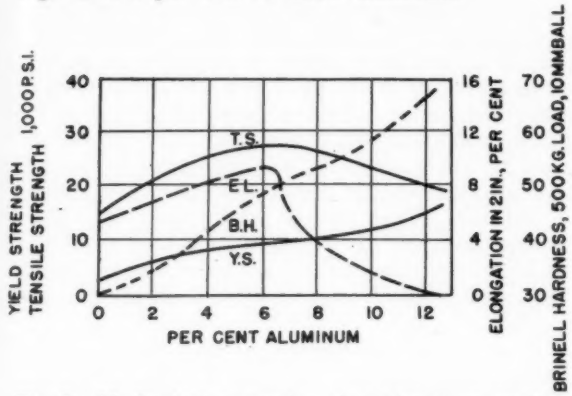


Fig. 2—Mechanical properties of sand-cast magnesium-aluminum alloys in as-cast condition.

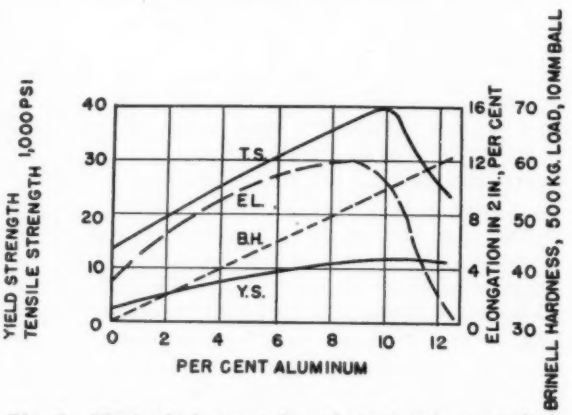


Fig. 3—Mechanical properties of sand-cast magnesium-aluminum alloys after solution heat treatment.

The addition of zinc to form ternary magnesium-aluminum-zinc alloys increases the response of the alloys to artificial aging. Greater hardness and higher yield strength after artificial aging can be obtained in the ternary alloys.

In view of the factors affecting the casting characteristics and mechanical properties outlined above, it appears quite logical that the most widely used magnesium alloys for permanent mold work are those bearing the ASTM designations AZ92A and AM100A. The alloy AZ91C is also suitable for permanent mold work but up to the present, our magnesium permanent mold foundry has not been presented with an application wherein the requirements could not be satisfied as readily with the standard AM100A as with the alloy AZ91C.

Permanent and semi-permanent mold castings in AZ92A and AM100A are covered by Federal Specification QQ-M-55. The guaranteed tensile properties are listed in this specification and need not be repeated here. The typical properties of magnesium alloy permanent mold castings, however, will be of interest to designers and are shown in Table 1. The alloy AM262 shown in Table 1 is an alloy of the AZ92A type for which there is no ASTM designation. The heat treat temper designations shown in Table 1 are those most commonly used for each alloy. It is, of course, possible to get property combinations other than those in Table 1. The chemical compositions of the various alloys discussed are shown in Table 2.

Table 1—Typical Mechanical Properties of Permanent Mold Cast AZ92A, AM100A and AM262

	AZ92A-T6	AM100A-T4	AM262-T4
Ultimate Strength, psi	40,000	40,000	39,000
Tensile Yield Strength, psi[1]	24,000	16,000	16,000
Elongation, per cent in 2 in.	2.0	10.0	10.0
Brinell Hardness[2]	81	52	63
Rockwell E Hardness	88	62	75
Shear Strength, psi[3]	22,000	20,000	20,000
Compressive Yield Strength, psi[1]	22,000	13,000	14,000
Endurance Limit, psi[4]	12,000	11,000	12,000
Charpy impact value, ft-lb[5]	0.8	2.0	—

[1] Stress at 0.2% deviation from modulus line.
[2] 500-kg Load—10 mm ball.
[3] Double shear tests converted to single shear values.
[4] 500 million reversals.
[5] Keyhole notch.

Table 2—Nominal Compositions of Alloys Suitable for Permanent Mold Work

Alloy Designation		Nominal Composition—Per cent			
ASTM	ALCOA	Al	Zn	Mn	Mg
AZ92A [1]	AM260	9	2	0.1 min	remainder [3]
AM100A [2]	AM240	10	—	0.1 min	remainder [4]
—	AM262	9	2	0.1 min	remainder [5]
AZ91C	AMA263	8.7	0.7	0.13 min	remainder [6]

Notes:
[1] Same as Composition AZ92 in Federal Spec. QQ-M-55
[2] Same as Composition A10 in Federal Spec. QQ-M-55
[3] Maximum copper—0.25 per cent
[4] Maximum copper—0.10 per cent
[5] Maximum copper—0.25 per cent; max. silicon—0.5 per cent; max. nickel—0.025 per cent; total other impurities—0.8 per cent.
[6] Maximum copper—0.10 per cent.

The alloys AZ92A and AM262 are general purpose alloys and the major portion of the magnesium permanent mold castings made today is in these two compositions. However, AM100A is being used to an increasing extent, particularly for aircraft wheels and other aircraft components which are subject to radiographic inspection. Alloy AM100A castings are more free from microshrinkage than AZ92A castings and experience has shown that difficult castings can be produced in AM100A more easily than in AZ92A, especially if the difficulty to overcome is cracking. In addition to the lesser tendency of AM100A castings to cracking in the mold or during removal from the mold, it has been repeatedly demonstrated in our foundry that when AM100A and AZ92A castings are made in the same mold and with equivalent mold

conditions, the AM100A castings will prove to be sounder on radiographic examination.

The microstructure of magnesium alloy permanent mold castings is finer than that of similar castings made in sand molds. The rapid rate of freezing causes a much greater dispersion of the Mg_4Al_3 compound in the microstructure than is normally observed in the microstructure of sand castings. The finer microstructure of permanent mold castings increases their response to both solution heat treatment and artificial aging and results in somewhat higher tensile properties compared to the same alloys cast in sand. The improved tensile properties obtained by casting in permanent molds instead of sand molds is particularly noticeable in AM100A. It has been found that AM100A test bars cast in permanent molds and given special experimental thermal treatments have properties such as those shown below, which were considered beyond the capabilities of AM100A until quite recently.

	Treatment A	Treatment B
Tensile Strength, psi	46,000	41,000
Yield Strength, psi	20,000	23,500
Elongation, % in 2 in.	9.0	2.0

It has been stated in the Introduction that the addition of beryllium to alloys used for permanent mold work and development of the chlorinated carbon compound method of grain refinement are important factors in the present rather satisfactory state of the art of casting magnesium alloys in permanent molds. Aside from affording marked commercial advantage over former practices, the beryllium addition and the grain refinement method have technological interest.

It is well known[5] that additions of beryllium to magnesium alloys coarsen the grain of the alloys if too much beryllium is added, but if the methods of Stroup and Sager[1] are followed, enough beryllium can be used to reduce oxidation of the molten metal and prevent it from reacting with the mold wash, and a grain size fine enough to result in excellent mechanical properties is still retained. In our magnesium permanent mold foundry, a nominal 0.001 per cent Be is added to the alloys.

In addition to the effect that fine grain size has on mechanical properties and in enhancing the response of the alloys to thermal treatments, experience in the foundry has shown that fine grain is essential if high scrap loss due to cracked castings is to be avoided.

Mold Design and Mold Operation

Mold design is such an involved subject that it can be considered here only in general terms. There are at least two general concepts of mold design and mold operation in use today. When the investigators of the Combined Intelligence Objectives Sub-Committee[6] examined magnesium permanent mold operations in Germany after World War II, one of the German experts stated that in his opinion, hot molds and low metal pouring temperature are the best combination for producing structurally sound magnesium alloy castings in permanent molds and that the semi-permanent mold process will produce castings of higher quality than can be produced in full permanent molds. Following this concept, it was German practice to maintain the molds at 660-750 F and to pour at 1260-1330 F. The mold walls were usually about ½ in. thick but the mold wall thickness was sometimes varied to match the section thickness of the adjacent portion of the casting. That is, the wall of the mold would be thick where the casting was heavy and thin where the casting was light in section. The German foundry engineers had also devised a rather clever method of feeding heavy sections of castings not accessible to risers placed at the parting lines of the mold. This was accomplished by placing a riser sleeve about ¼ in. thick of suitable shape and draft in the mold over the location where additional feed was desired. The riser sleeve had a handle and could be rotated and lifted out of the mold by the handle. In operation, the casting was poured in the regular manner and, just as the metal in the riser sleeve became solid enough to be removed, the riser sleeve was rotated and lifted out with the riser inside it. Thus removed, risers of this type would not interfere with the opening of the mold.

In our magnesium permanent mold foundry, pouring temperatures are usually in the range 1380-1420 F and there is no general rule regarding mold temperatures. Rather, each casting is carefully studied so that the mold may be designed in conformance with well-known principles of progressive solidification without the use of risers not on the parting line and without the very large risers that give the German permanent mold castings the appearance of sand castings. When the mold is placed in operation, careful thermal studies are made to determine where heat must be supplied or removed to achieve progressive solidification and sound castings. These thermal data are carefully recorded so that proper operating conditions may be duplicated each time the mold is placed in production. The mold design takes into consideration the relation between mold wall thickness and casting section thickness, but the principal concern is to design a mold that will not warp and one that will maintain the required dimensional accuracy under operating conditions.

For best results, it has been found that permanent molds for magnesium alloy castings must be operated rapidly. That is, the time interval between the pouring of successive castings should be as short as possible. This requirement of fast mold operation is, in part, the result of the low volume specific heat of magnesium alloys and it has its advantages as well as disadvantages. Per unit of weight, a pound of magnesium alloy contains about as many Btu's at a given temperature as an aluminum alloy, but per unit volume, the magnesium alloy contains much less heat than the aluminum alloy. For two typical alloys, magnesium base alloy AZ92A and aluminum base alloy B195, the specific heat values are 0.45 and 0.64 cal/cc/°C. Thus, the amount of heat a given mold must extract to freeze a magnesium casting is only about 70 per cent of that required to freeze a similar aluminum casting. This means that the time cycle for operating a given mold on magnesium can be appreciably shorter than when operating the same mold

on aluminum and that an operator can produce more magnesium castings than aluminum castings per shift. Since labor cost is a major factor in the cost of producing permanent mold castings, it is this higher productivity per man-hour that enables magnesium permanent mold castings to compete, on a cost per piece basis, with aluminum permanent mold castings.

The principal disadvantage of the low volume specific heat of magnesium alloys is that there is less leeway in mold conditions and, to maintain quality, the mold wash, mold temperatures, pouring temperatures, etc., must be very carefully controlled.

Fabricating Limitations

(A) **Size and Weight**—It is rather meaningless to attempt to establish an upper limit on the size and weight of magnesium alloy castings produced by either the permanent or semi-permanent mold process. Year by year the foundry undertakes and produces larger and more difficult jobs than it was willing to attempt the previous year. At the present time, castings having a trimmed weight of about 100 lb are the heaviest made in our magnesium permanent mold foundry. The production of much larger castings, however, would be entertained if the casting design is such that it lends itself to the process.

Overall casting size is probably a more serious limitation than casting weight. As the dimensions of a mold increase, the problems of compensating for expansion and preventing mold distortion multiply. As an example, the mold for a casting 45 in. long and about 14 in. wide, heavily ribbed on one side, presented some rather serious distortion problems. There were 20 transverse ribs approximately 2 in. deep, one longitudinal center rib and two side flanges. Because of the length of the mold halves and the unequal heating resulting from the casting operation, considerable distortion is encountered which adversely affects the mold operating cycle. Problems such as the ones encountered on this mold must be anticipated and overcome as the art of making larger castings by the permanent mold process is developed.

(B) **Section Thickness**—A second consideration in determining the advisability of producing a part by the permanent mold process is the section thicknesses of the part involved. Thick sections offer no particular problem if they can be adequately fed, but there are definite limitations to how thin a section can be cast. Our foundry does not, as a general rule, undertake to cast sections thinner than $\frac{1}{8}$ in., although on occasion, castings with areas thinner than $\frac{1}{8}$ in. have been produced in commercial quantities.

As the area of a thin section increases, it is necessary to request an increase in thickness. If the area is over 9 sq in., it is recommended that the minimum wall thickness be $\frac{5}{32}$ in.

(C) **Corner Radii**—Sharp interior wall or section junctions should be avoided in the design of magnesium castings to be produced by the permanent or semi-permanent mold process. Such sharp corners frequently result in cracks in the casting induced by a combination of stress concentration and a localized hot spot in the mold.

(D) **Inserts**—In many instances, it is necessary to include in a magnesium part an insert or inserts fabricated from some other metal. It is frequently desirable to cast the insert in place at the time the casting is produced rather than to put it in place by means of a subsequent operation. There are, in fact, many inserts which, because of their shape and location, must be cast in place.

The permanent mold process is probably more suited to producing castings with inserts cast in place than other casting processes. It is generally desirable to preheat inserts just before casting to drive off water vapor and other volatiles which may be adsorbed on the insert surface. In the permanent mold process, the insert is placed in the mold and the casting poured while the insert is still hot. This insures that no water vapor can condense on the insert after preheating and before casting in place.

Inserts for magnesium permanent mold castings must be clean and accurately sized before use. They may or may not be coated depending on the type of insert and application. Typical insert materials are iron, steel, copper and brass. If the inserts are costly, they can generally be salvaged from scrap castings for re-use by carefully heating the magnesium casting above the equi-cohesive range and then breaking the magnesium away from the insert. Extreme care must, of course, be taken to prevent inserts containing copper or nickel from getting in to the melting furnaces, as this will result in contamination of the magnesium alloys. All salvaged inserts must be carefully inspected and gauged before re-use.

(E) **Surface Smoothness** — The average surface smoothness of magnesium castings is about 150 microinches, although somewhat greater smoothness can be achieved if desired. The smoothness is controlled by the mold wash application and may be varied over a wide range.

Sand Cores for Semi-permanent Mold Work

Sand cores are used for semi-permanent mold production. In general, core mixes are similar to those used for other metals in that they consist of silica sand, cereal, binder and water. One important difference is that sand mixes for cores used in magnesium production must contain an inhibiting material to prevent reaction between the molten magnesium and the sand core.

Common inhibiting materials are sulphur, boric acid, potassium fluoborate, ammonium acid fluoride, ammonium silico-fluoride and diethylene glycol. These may be used singly or in combination.

Core mixes are generally similar in composition for either sand or semi-permanent mold production except that less inhibitor is necessary for semi-permanent mold cores. This is true because the more rapid solidification of the metal in the semi-permanent mold process tends to retard the reaction mentioned earlier.

Bank or washed silica sand free from alkalis and organic matter is the base material for cores. Characteristics of the sand used will vary, depending upon availability and properties required in the core.

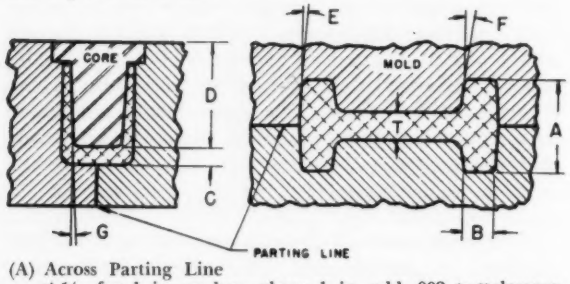
The binder used, in most instances, is one of the

numerous urea-formaldehyde compositions. This type of binder is preferred because of its good knockout characteristics and the fact that the baking temperature for such cores is lower than for cores using standard oil binders. A low baking temperature is desirable to keep "burn out" or volatilization of inhibitors to a minimum. Under certain conditions when a urea-formaldehyde binder is not satisfactory because of rapid generation of large volumes of gas, a standard oil type binder is used. Satisfactory cores can be made with oil binders although their knockout properties are somewhat inferior. It may also be necessary to increase the amount of inhibitor when oil binders are used.

A typical core mix used for semi-permanent mold sand cores is:

Ottawa silica sand, lb	—	1000
Cereal, lb	—	10
Urea-formaldehyde type binder, lb	—	12
Boric acid, lb	—	10
Water, qt	—	16
Kerosene, qt	—	3

Chills are used in cores where necessary to promote progressive solidification of the casting. It is important to apply a coating to the chills to prevent blows. A typical coating for this purpose consists of talc, rosin, and boric acid in an alcohol suspension. The coatings can be applied by brush or spray. Materials used to repair cores or close joints usually consist of various mixtures of fine sand, talc, cereal and boric acid moistened with alcohol to form a thick paste or mud.



(A) Across Parting Line
$\pm 1/64$ for 1 in. or less—above 1 in. add .002 to tolerance per inch of length.
(B) Between Points Produced by One Part of the Mold
$\pm 1/64$ for 1 in. or less—above 1 in. add .001 to tolerance per inch of length.
(C) Between Point Produced by the Core and Mold
$\pm 1/64$ for 1 in. or less—above 1 in. add .002 to tolerance per inch of length.
(D) Maximum Length of Core Supported at One End
diameter of core $\times$ 10.
(E) Outside Draft
1° minimum—3° desirable
(F) Draft in Recesses
2° minimum—5° desirable
(G) Draft on Cores
$1/2$° limited—2° desirable

Note:
(1) Minimum Diam of Cores—$1/4$
(2) Minimum Web Thicknesses "T" for Distance of:
3 in. or less—$1/8$
3 to 6 in.—$5/32$
over 6 in.—$3/16$
(3) Machine Tool Locating Points Should be Indicated
(4) Allowances for Machining:
Castings up to 10 in. long—$1/32$ minimum—$3/64$ desirable
Castings over 10 in. long—$3/64$ minimum—$1/16$ desirable
Surfaces formed by sand cores—$1/16$ minimum
(5) Minimum Radius of Fillet
Radius should equal average thickness of joining walls.

Fig. 4—Permanent mold tolerances.

Design Considerations

(A) **Dimensional tolerances**—The dimensional tolerances applicable to magnesium permanent and semi-permanent mold castings are shown in Fig. 4. These tolerances are generally acceptable to the foundry, provided close tolerances are specified on only a limited number of specific points on each part. Normal sand casting tolerances usually apply to those surfaces formed by sand cores in semi-permanent mold castings. Designers should not specify tolerances closer than necessary. Also, castings of a complex design or those made according to specifications which require unusual foundry practices should be submitted to the engineers of the foundry involved for special consideration.

In special cases, the foundry may agree to reduce the stated minimum dimensional and draft requirements, but these exceptions will usually increase the cost of the product and/or equipment, and may reduce the production rate of the mold.

Permanent mold castings are more accurate dimensionally than sand castings. They are not, however, as accurate as die castings.

(B) **Examples of poor casting design from permanent mold standpoint**—In establishing the design of a part to be produced by the permanent mold or semi-permanent mold process, attention to certain details will reduce the cost and improve the quality of the part. It should be noted, for example, that thin ribs or fins are difficult to cast and that the height of the fins should be reduced as much as possible. From a design standpoint alone, the substitution of a broad flat-edge rib for a high, thin rib almost always results in improved serviceability. An example of good rib design is shown in Fig. 5.

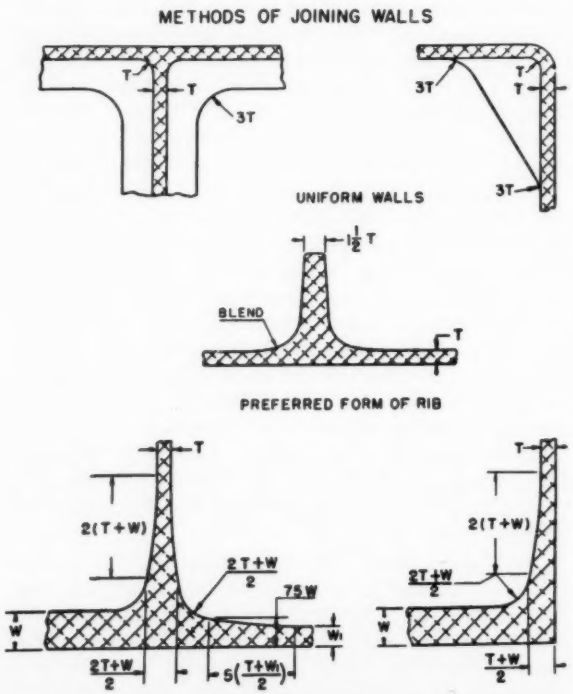


Fig. 5—Examples of good rib design.

The difficulties encountered with cracks in thin, high ribs can be readily understood if it is remembered that many magnesium castings are removed from molds while portions of the castings are still above the equi-cohesive temperature. With a casting in this fragile condition, the smallest drag or bump is usually sufficient to cause a crack or fracture. When ribs or other projections are present, therefore, extreme care must be used in moving mold segments to insure that, once parted, the segment is removed without touching the casting.

A second way in which the designer can aid the foundryman in their common objective of obtaining a good quality casting is in the blending of thick and thin sections. This is also illustrated in Fig. 5 depicting methods of joining walls of different thicknesses.

It will be noted that in the preferred method, blending is provided which results in a gradual change in section thickness as compared with the abrupt change commonly encountered.

In considering the construction of a permanent mold for producing magnesium castings, several points should be kept in mind. Compared with aluminum permanent mold practices, it has been found desirable to use closer tolerances in the fit of such mold segments as cores, shafts and pins in magnesium practice. This is, of course, due to the susceptibility of the magnesium castings to cracking when they are being removed from a mold.

At the time a mold is being designed, it is also important to avoid thin mold projections wherever possible. Judicious choice of casting parting lines will often be helpful in insuring adequate mold section thicknesses. Thin mold sections are undesirable from at least two standpoints. First, considerable cost will be involved in maintaining the sections; and, second, thin sections of a mold become overheated during mold operation and may result in poor quality castings in the area involved. Adequate chilling of such sections may be difficult or, in fact, impossible.

Another item which must be carefully considered in the construction of a mold is the venting of air from the mold cavity. The locations of the ingates into the casting cavity often have a profound effect on the venting of the cavity. When sand cores are used in molds, special precautions must be taken to provide means of escape for the core gases.

Economics and Applications

A discussion of magnesium alloy permanent and semi-permanent mold castings cannot be considered complete without some reference to the economics of using magnesium castings and an indication of the field of application. The experience of the past several years indicates that applications for magnesium permanent mold and semi-permanent mold castings are to be found in the following classifications:

1. As a replacement for magnesium sand castings in aircraft propulsion units and airframe and aircraft components when the quantities required are large enough to amortize the cost of the mold and show an over-all saving. Landing wheels, magneto housings, rocker box covers, front section crankcases and jet compressor cases are in this category (Fig. 6).



Figure 6

2. Applications where either aluminum or magnesium permanent mold castings are satisfactory but the part can be produced at lower cost in magnesium. Examples of this category are sideframes for duplicating machines, lawn mower housings, hoist bodies, movie projector housings, sewing machine parts (Fig. 7).

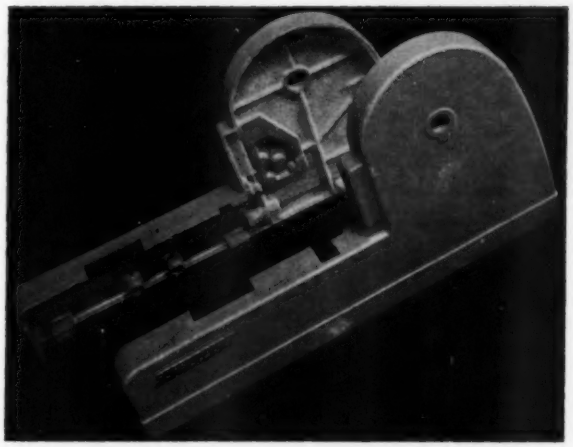


Figure 7

3. Applications where light weight may or may not be important but ultimate cost of the finished part is the controlling factor. In these cases, magnesium permanent mold castings can compete with gray iron castings because the magnesium castings can be cast closer to finished size and shape to eliminate or reduce machining operations and because the cost of machining magnesium is less than that of cast iron. Examples—Diesel inspection covers, Diesel scavenging valve covers, oil well cementing equipment, printing plate mounting bases (Fig. 8).

4. Applications where either aluminum or magnesium permanent mold castings would be suitable but where light weight is a desirable bonus if it can be obtained with little or no increase in cost. Ex-

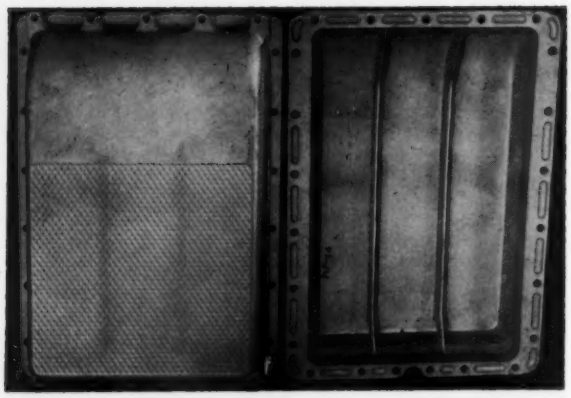


Figure 8

amples—Frames for change-makers, housings for portable tools, textile machinery parts, X-ray tube holders, cable reels (Fig. 9).

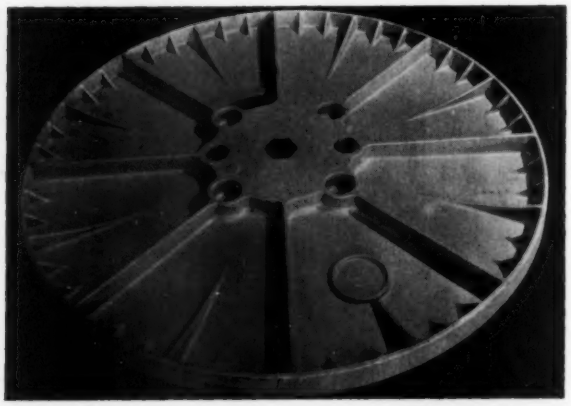


Figure 9

The use of magnesium castings of all types in such fields as aircraft where weight saving is extremely important is well known and easily understandable. If the magnesium foundry industry is to continue to expand, however, commercial outlets must be established and maintained against the day when the industry is no longer supported by defense orders.

The chief competitor of magnesium permanent mold castings in commercial applications is aluminum castings. There are many commercial applications where neither the generally superior corrosion resistance of aluminum nor the lighter weight of magnesium is important, and, in these instances, the customer will use the metal that provides him with the lowest cost product. It may be of interest to examine some of the costs that go into the price tag on a casting and to compare magnesium and aluminum permanent mold castings in light of these costs.

During the past several years, the metal cost per given casting has been less in magnesium than in aluminum. Today, with pure magnesium at 24.5 cents per lb as compared to aluminum 99 per cent ingot at 20 cents per lb, the metal cost per casting in magnesium should be as low as in aluminum. The cost per unit volume of melting magnesium is higher than the cost of melting aluminum because of the melt loss encountered with magnesium and the protective agents and fluxes used to prevent magnesium from oxidizing. We have found, however, that in most instances this is more than offset by the fact that it is possible to get more castings per hour out of a given mold in magnesium than in aluminum. It is hard to generalize because of the different parts involved, but it might be said that on the average, the increased production when magnesium is cast in the mold is about 20 per cent.

There are two chief reasons for this increased production. First, as mentioned before, the set-time for magnesium castings is less than for aluminum castings because less heat needs be extracted before the casting can be removed from the mold; and secondly, only two-thirds the weight of metal is handled when casting magnesium. In some instances, this results in being able to use one man on a mold to produce magnesium castings where two are required if aluminum castings are being made.

More scrap is encountered in magnesium than in aluminum, and this is one factor that tends to offset the lower labor cost when casting magnesium.

In regard to trimming operations, magnesium again has the advantage. The ease of sawing, grinding and machining magnesium is well known, and the lighter weight of magnesium castings makes them easier to handle. Offsetting some of the trimming gain is the fact that magnesium castings are normally supplied with chrome pickle surface treatment, whereas aluminum castings are usually shipped without surface treatment. A certain saving is realized in reduced shipping costs with magnesium because of its lighter weight.

The principal competitor of both aluminum and magnesium permanent mold castings are gray iron sand castings. In most instances, the deciding factor which determines the acceptance of the product is not the characteristics of the material, but rather the price of the casting. Some inroads have been made and are being made by magnesium in competition with gray iron. Occasionally, it is found that because thinner sections can be cast in magnesium permanent mold than can be sand cast in iron, the magnesium permanent mold foundry can supply a casting which weighs, not one-fourth the weight of the iron casting, but perhaps one-seventh the weight. In such instances as this, the magnesium foundry can frequently supply the customer with a casting which is not only much lighter and easier to handle, but also cheaper than the iron casting.

As stated in the beginning, it has been the purpose of this paper to outline the current status of the art of casting magnesium alloys in permanent and semi-permanent molds. To achieve this purpose, the characteristics that alloys for permanent mold work should possess have been discussed and certain points regarding casting design and economics have been set forth. It is hoped that the discussion and facts presented here have demonstrated that magnesium alloy castings made by the permanent mold process are available for a wide range of purposes at prices that

are competitive with aluminum and gray iron castings.

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Acknowledgment

The authors are grateful to Bendix Aircraft Corp., Speed-O-Print Corp., Cooper-Bessemer Corp. and General Cable Corp. for use of illustrations in this paper.

DISCUSSION

Chairman: W. A. MADER, Oberdorfer Foundries, Inc., Syracuse, N. Y.

Co-Chairman: D. L. LAVELLE, American Smelting & Refining Co., Barber, N. J.

Recorder: J. L. SCHMIEDER, JR., Oberdorfer Foundries, Inc., Syracuse, N. Y.

R. F. DALTON:[1] How is beryllium added to magnesium?

MR. GINGERICH: Beryllium is added after the magnesium is molten by methods believed to be patented.

CO-CHAIRMAN LAVELLE: How do you flush the molds? Are inhibitors used in the mold wash?

MR. GINGERICH: The molds are not ordinarily flushed. When done, however, SO_2 is used for this purpose. Standard mold washes are employed.

R. J. MAENNER:[2] Why was the range, 1380-1420 F, selected as the pouring temperature?

MR. GINGERICH: This range is typical of practice. Temperatures are kept low to prevent burning of the metal.

MEMBER: Are crucibles or some other type of pots used in melting? Is degassing of the metal required?

MR. GINGERICH: Fabricated steel pots are used. Degassing is done with chlorine.

MEMBER: Is grain refinement necessary?

MR. GINGERICH: Yes, it is standard practice.

MEMBER: Do you lose your grain refinement if the metal is held too long in the molten state?

MR. GINGERICH: Yes, but there is no appreciable change in 1½-2 hr.

CHAIRMAN MADER: Do you have trouble with precipitation in your melts?

MR. GINGERICH: Yes, occasionally, but we do not know why it happens.

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[1] Development Engineer, U. S. Gypsum Co., Chicago.
[2] Supervisor, Permanent Mold Foundry, The Dow Chemical Co., Bay City, Mich.

PREDICTION OF MECHANICAL PROPERTIES FROM CHEMICAL COMPOSITION FOR FULLY-ANNEALED DUCTILE CAST IRON

By

C. C. Reynolds,* C. M. Adams, and H. F. Taylor *****

ABSTRACT

A correlation is made in this paper, between mechanical properties and composition of ferritic ductile irons. The data used for calculating was obtained from carefully controlled laboratory experiments in which many elements (carbon, silicon, manganese, nickel, phosphorus, magnesium, also section size) were varied singly with all other variables held constant. The fact that mechanical properties could be accurately predicted for ductile irons as a function of composition (and vice versa) seemed a remarkable confirmation of the engineering potential of this material. Applications of such information are discussed and methods of calculation clearly described.

I. Introduction

Ductile cast iron consists of graphite spherulites dispersed in a metallic matrix. The matrix material may vary from pure ferrite to high carbon steel, and its microstructure depends heavily upon chemical composition and heat treatment. Possible microconstituents in the matrix include ferrite, pearlite, carbides, bainite, martensite, tempered martensite, and retained austenite.

To take full advantage of ductile cast iron as an engineering material, it will ultimately be necessary to obtain complete information on mechanical properties in a convenient form. To accomplish this, each general type of matrix should be studied with variations in chemistry and heat treatment, and the data correlated in a straightforward pattern. Given such a correlation, the user would be able to select the optimum composition and heat treatment for his needs, and the true potentialities of ductile cast iron might be realized.

The first phase of the compilation outlined above has been directed at a study of the completely ferritic matrix; reasons for this are:

1. Currently, low alloy ductile cast iron is most widely used in the annealed, ferritic condition. This is due largely to the relative ease of preparation and heat treatment, and the high ductility of the ferritic material.

2. A fact not widely appreciated is that the ferritic material can encompass a surprisingly extensive range of mechanical properties. Ductile cast iron is unique in that it can take full advantage of the properties obtainable by solid solution hardening of ferrite.

3. A simple, two phase, mechanical mixture of ferrite and graphite lends itself readily to the type of correlation used, whereas the presence of additional microconstituents would make the job more complex.

4. A large amount of data has already been ob-

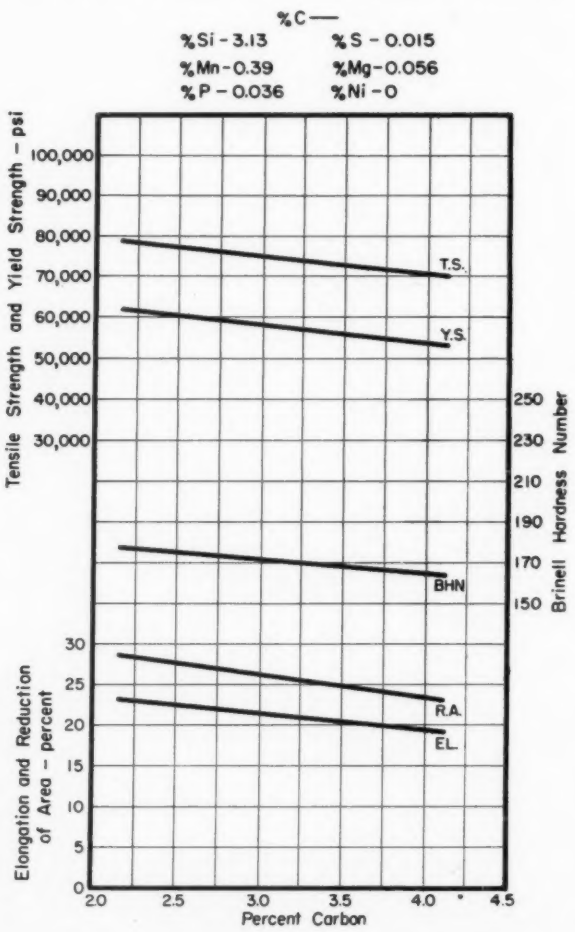


Fig. 1—Effect of carbon on mechanical properties.

*D.I.C. Staff, **Assistant Professor, and ***Professor, Dept. of Metallurgy, M.I.T., Cambridge, Mass.

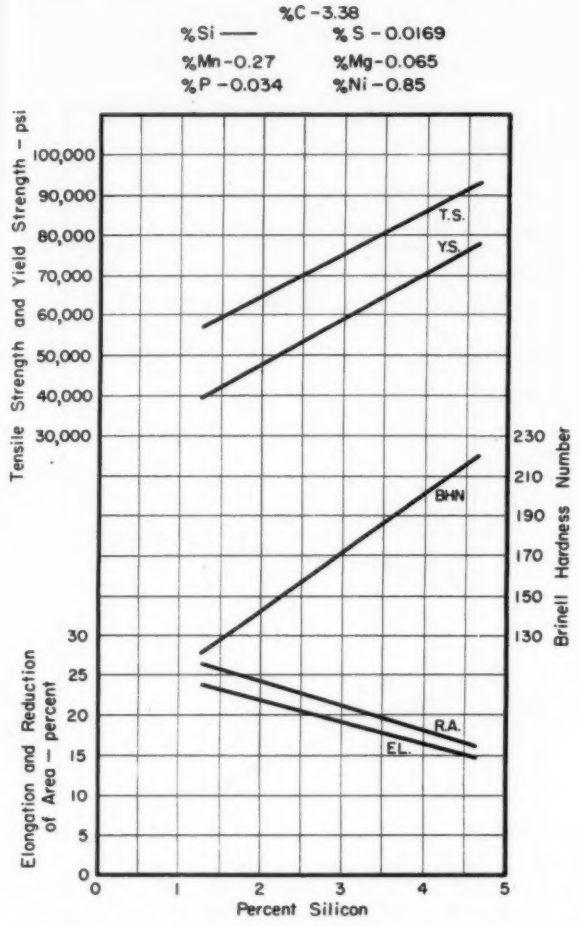


Fig. 2—Effect of silicon on mechanical properties.

tained under carefully controlled laboratory conditions;† these data are presented again herein as Figs. 1 through 6.

This correlation of mechanical properties with chemical composition applies to irons having analyses within the limits 0.75-4.5 pct Si, 2.25-4+ pct C, 0-3 pct Ni, 0.04-0.10 pct Mg, 0.003-1.0 pct Mn, 0.016-0.4 pct P in section sizes of 6 in. and under.

II. Analysis of Data on Mechanical Properties

Fully annealed ductile iron may be considered as pure ferrite, modified by the elements normally present in this type iron. Carbon modifies the ferrite by dispersing many small spherulites of graphite throughout the matrix. Since graphite is weak and soft, increasing the carbon will decrease all mechanical properties of the ferrite-graphite mixture. Examination of Fig. 1 reveals that the tensile strength decreases 4,400 psi for each per cent of carbon present. The ferrite is further modified by the other elements (silicon, nickel, manganese, phosphorus) and by section size. One per cent of silicon will increase the tensile strength 11,000 psi (Fig. 2), and 1 pct nickel 5,000 psi (Fig. 3). Similiarly, the other mechanical properties exhibit a straight line variation when the concentration of a single element is changed (with the exception of phosphorus). Adding a given element will have an effect upon a particular mechanical property which is obtained from measuring the slope of the pertinent line in Figs. 1 through 5. Composition factors obtained in this fashion are listed in Table 1, in which the *effect of adding 1 pct* of an element is shown for each property. Also included in Table 1 is the influence of a 1-in. increase in section size on properties.

The element phosphorus is the only element in the present paper which does not contribute linearly to the mechanical properties. Phosphorus contributes strongly at first, and to a lesser degree with each additional amount present. For convenience in graphical correlation, an "equivalent" per cent phosphorus ($\%P_e$) has been developed using the data of Fig. 5. In Fig. 7, curves are presented which relate $\%P_e$ to actual per cent phosphorus. The $\%P_e$ can then be applied in the manner as used for the other elements. The $\%P_e$ is used in assessing the effect of phosphorus on yield strength, tensile strength and hardness as shown in Table 1. Since elongation and reduction in area are linearly affected by phosphorus, the factors for these properties (in Table 1) apply to *actual per cent phosphorus.*

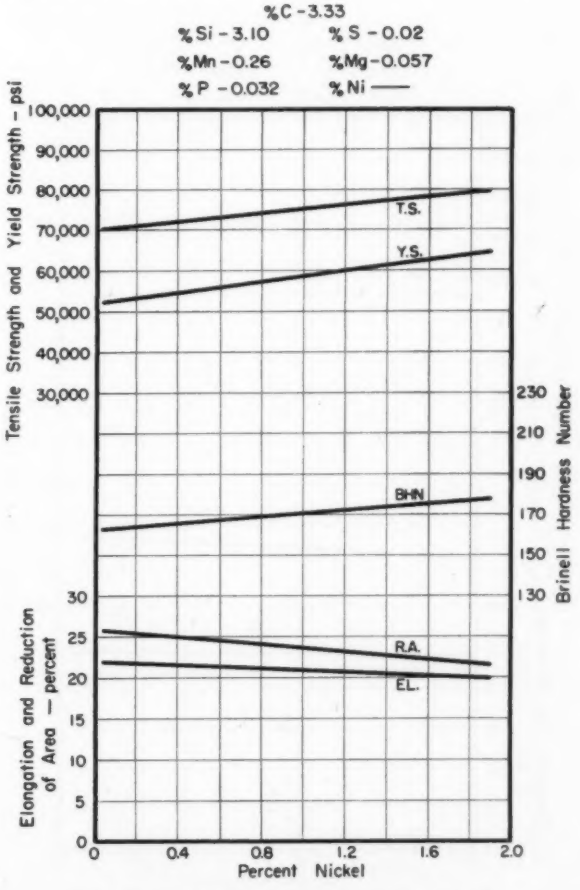


Fig. 3—Effect of nickel on mechanical properties.

†C. Reynolds and H. Taylor, "Mechanical Properties of Spherulitic Graphite Cast Iron," A.F.S. Transactions, vol. 60, pp. 687-713 (1952).

TABLE 1—CHANGE IN PROPERTY FOR EACH PER CENT OF ELEMENT

Increase in Element	T.S.	Y.S.	BHN	R.A.	El	(I)
1% Carbon	—4,400	—4,400	—7.2	—2.8	—2.1	
1% Silicon	+11,000	+11,200	+29	—2.8	—3.0	
1% Nickel	+5,000	+6,000	+7.0	—2.3	—1.0	
1% Manganese	0	0	+11	0	0	
1-in. Section Size	0	0	0	—2.8	—2.8	
1% Phosphorus Equivalent or	+5,000	+5,000	+3.6	——	——	(1A)
1% Phosphorus	——	——	——	—41	—38	

Residual magnesium (within reasonable and expected limits) has no effect upon the mechanical properties and for this reason is not considered.

The composition factors in Table 1 have been determined experimentally by varying the concentration of one element with all others held constant. Factors would not be very useful, however, unless applicable to a base iron of any composition. In other words, if the effects of the elements are independent of one another, and are completely additive, the process of correlation becomes much easier and of more practical importance. The elements may be shown to act independently by the following procedure:

Each series of experiments was designed to assess the contribution of a single element to the mechanical properties of a normal iron. When the individual contributions of all the major elements had been determined, it was possible to subtract the effect of each element from a given iron, and obtain a hypothetical set of mechanical properties for unalloyed ferrite. When the operation was repeated for all compositions tested, the properties of the ferrite thus obtained were in consistently good agreement. The results of this analysis are presented in Table 2. Data from each series of experiments led to a single independent determination of ferrite properties. For example, the first set of ferrite properties, shown in Table 2, was obtained from an analysis of the carbon

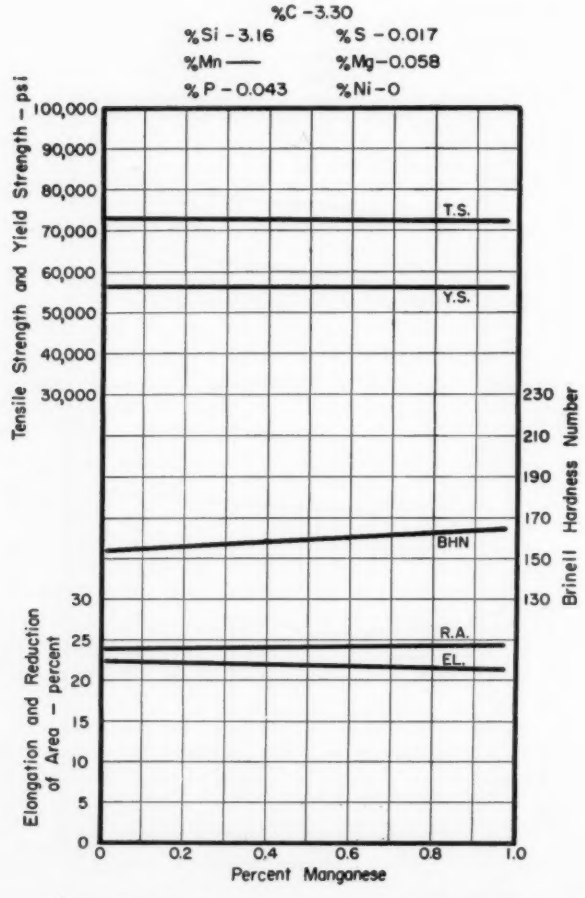


Fig. 4—Effect of manganese on mechanical properties.

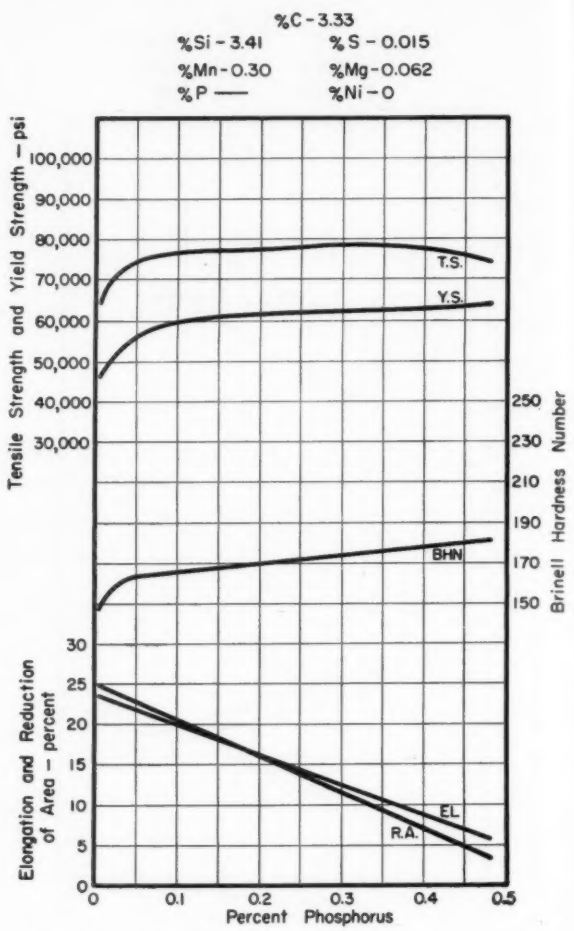


Fig. 5—Effect of phosphorus on mechanical properties.

TABLE 2—CALCULATED MECHANICAL PROPERTIES FOR UNALLOYED FERRITE

Series	$(T.S.)_o$	$(Y.S.)_o$	$(BHN)_o$	$(R.A.)_o$	$(El)_o$
Carbon	43,800	26,600	85	49	41
Silicon	42,100	25,900	86	46	39
Phosphorus	39,200	20,500	70	45	44
Nickel	44,300	20,800	81	47	42
Magnesium	40,300	21,200	82	48	42
Manganese	39,900	21,300	72	46	45
Section Size	39,500	19,400	80	45	41
Average Mechanical Property =	41,300	22,200	79	47	42

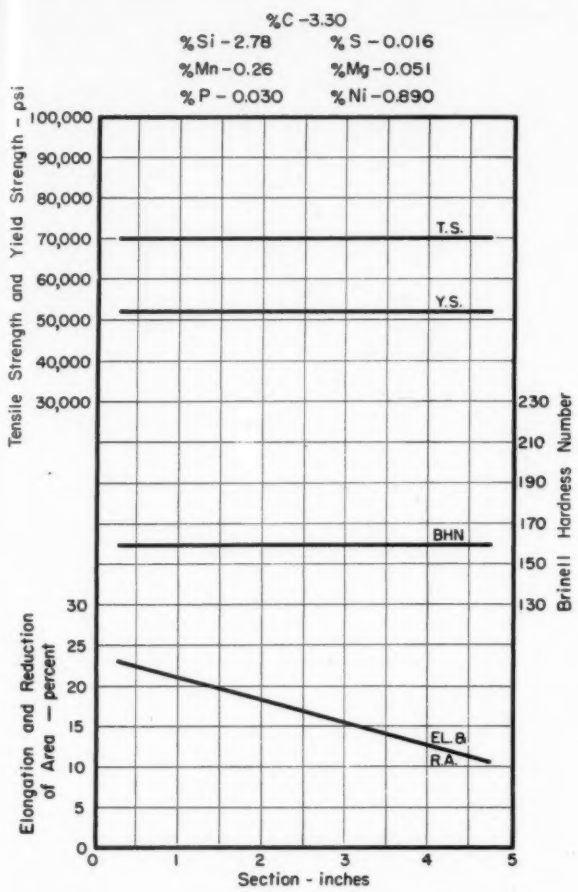


Fig. 6—Effect of section size on mechanical properties.

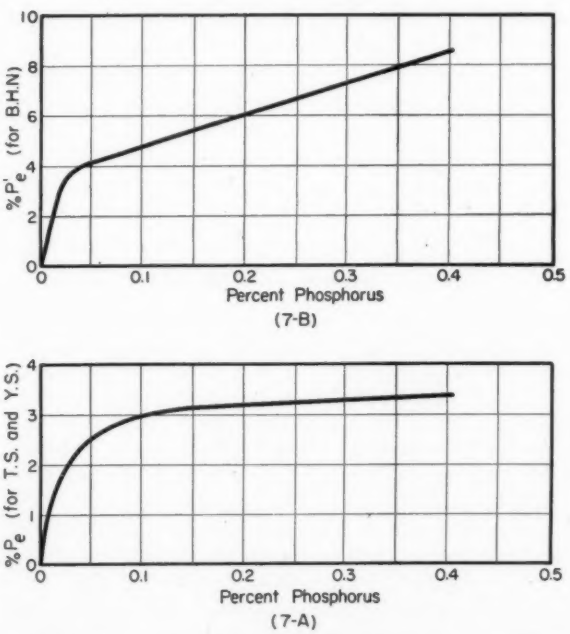


Fig. 7—Per cent phosphorus equivalent for tensile strength, yield strength, and Brinell hardness, from actual per cent phosphorus.

series (Fig. 1). The procedure was simply to extrapolate the straight lines in Fig. 1 to zero carbon, and then subtract the contributions of Si, Mn, P, and section size.

The properties of ferrite, which showed little deviation from series to series, were averaged; these average values will be used in subsequent paragraphs, and are denoted as $(T.S.)_o$, $(Y.S.)_o$, $(BHN)_o$, $(R.A.)_o$, and $(El)_o$.

Tables 1 and 2 may be combined and expressed analytically:

$$T.S. = 41{,}300 - 4{,}400\ (\%C) + 11{,}000\ (\%Si) + 5{,}000\ (\%Ni) + 5{,}000\ (\%P_e)$$

$$Y.S. = 22{,}200 - 4{,}400\ (\%C) + 11{,}200\ (\%Si) + 6{,}000\ (\%Ni) + 5{,}000\ (\%P_e)$$

$$BHN = 79 - 7.21\ (\%C) + 29\ (\%Si) + 7\ (\%Ni) + 11\ (\%Mn) + 3.6\ (\%P_e)$$

$$R.A. = 47 - 2.8\ (\%C) - 2.8\ (\%Si) - 2.3\ (\%Ni) - 2.8\ (\text{1-in. Section}) - 41\ (\%P)$$

$$El = 42 - 2.1\ (\%C) - 3.0\ (\%Si) - 1\ (\%Ni) - 2.8\ (\text{1-in. Section}) - 38\ (\%P)$$

The above equations are not in a very convenient form and the accuracy indicated is better than the data warrant. A few approximations lead to the following expressions:

$$T.S. = Y.S. + 20{,}000 = 41{,}000 + 5{,}000\ [2\ (\%Si) + (\%Ni) + (\%P_e) - (\%C)]$$

$$BHN = 79 + 3.6\ [8\ (\%Si) + 2\ (\%Ni) + 3\ (\%Mn) + (\%P_e) - 2\ (\%C)]$$

$$R.A. = 47 - [3\ (\%Si) + 3\ (\%C) + 2\ (\%Ni) + 3\ (\text{1-in. Section}) + 40\ (\%P)]$$

$$El = 42 - [3\ (\%Si) + 2\ (\%C) + (\%Ni) + 3\ (\text{1-in. Section}) + 40\ (\%P)]$$

These equations are presented graphically in Figs. 8 and 9, and summarize all of the information contained in Figs. 1 through 6. Figures 8 and 9 consti-

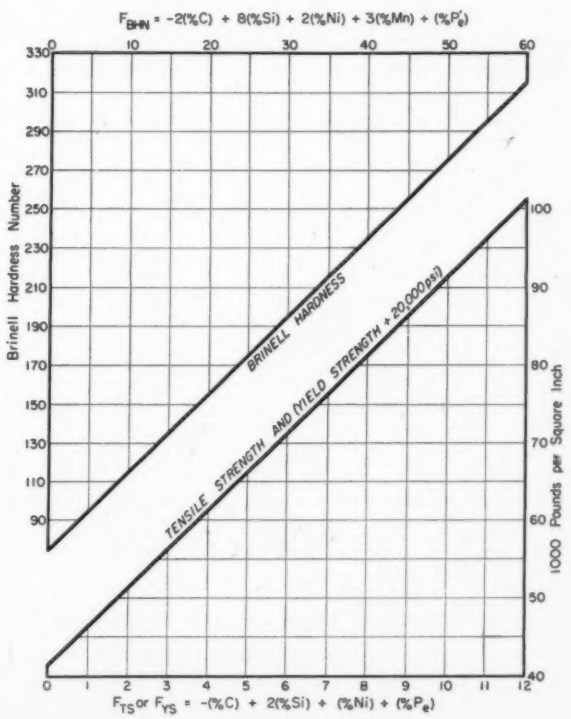


Fig. 8

tute the practical, useable end-products of the correlation. The charts may be directly interpreted to provide a suitable chemical composition for a required set of mechanical properties, using ferritic ductile iron.

III. Practical Aspects of the Analysis

The effect of each element on the mechanical properties of ductile cast iron with ferritic matrix has been separated and evaluated. The results can be useful in many ways:

1. The analysis developed herein provides an index of graphite shape. Knowing chemistry, a theoreical strength can be calculated and compared with the measured strength. Any difference would then be due to deviation of graphite shape from the spherulitic form.

2. From Figs. 8 and 9 the best direction in which to change chemistry of the iron to obtain desired mechanical properties can be determined.

3. The ratio of hardness to strength can be changed. From Fig. 10 it can be seen that, for a given increase in strength, silicon increases hardness twice as fast as does an equivalent increase in the amount of nickel or decrease in carbon. Manganese increases hardness without appreciably changing strength as measured by the normal tensile test. (Increasing manganese increases the tendency for the matrix to work harden. This, plus the fact that resistance to deformation is measured by Brinell hardness on an *increasing* area, and by the tensile test on a *decreasing* area, probably causes this abnormality.) Figure 10 graphically represents the relation between Brinell hardness and strength for each element.

4. Considering all equations, carbon should be kept as low as possible for optimum mechanical properties. The minimum carbon will be controlled by factors such as melting unit, inoculating technique, section size, pouring temperature, shrinkage and general practice.

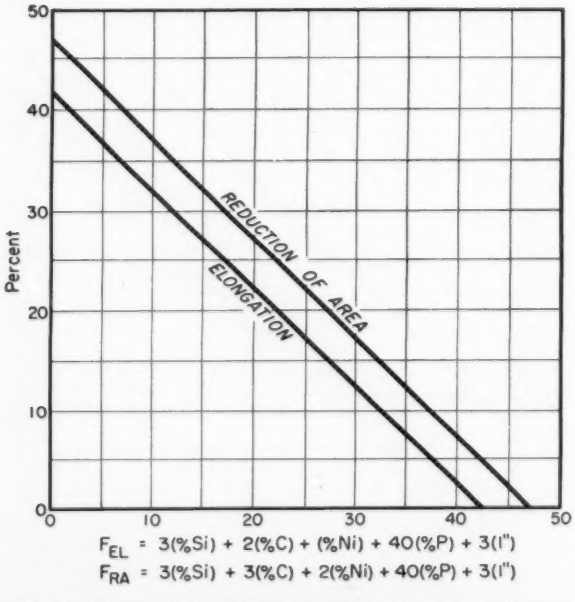


Fig. 9—Effect of chemistry on elongation and reduction of area of ferritic ductile iron.

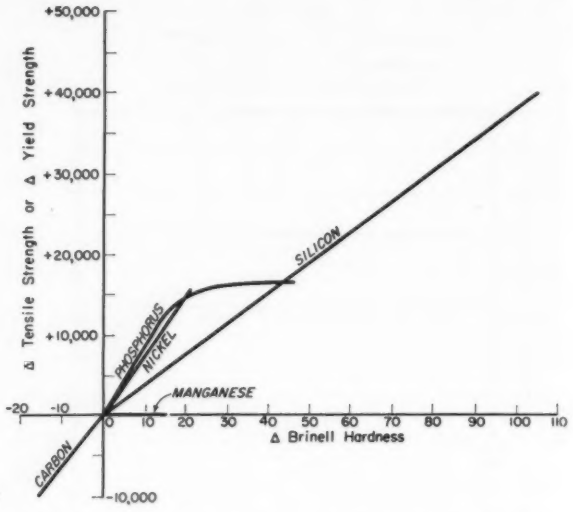


Fig. 10—Comparative effect of elements on change in tensile strength and yield strength for change in Brinell hardness.

5. The effect of the various elements in the relationship between hardness and ductility is shown in Fig. 11. Here it is very evident carbon should be kept to a minimum. Small amounts of phosphorus are necessary for good strength, but above about 0.05 pct the loss of elongation is high for the attendant increase in strength. Nickel and silicon are shown to be excellent alloys for ferrite.

6. It is possible to determine the greatest elongation that can reasonably be expected of ferrite. This might be produced by iron of the following composition: 2.25 pct C, 0.02 pct P, 0.75 pct Si.

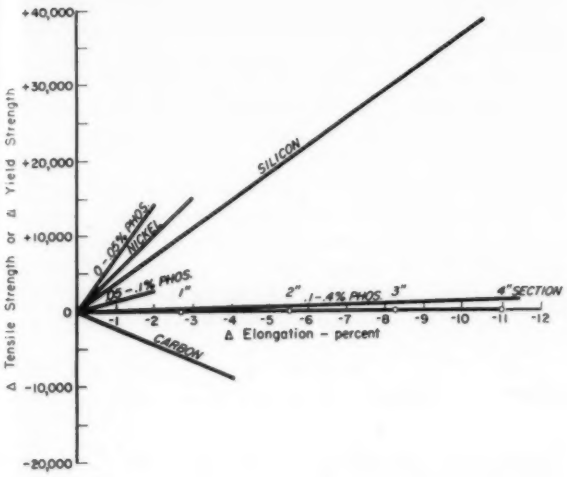
Element	F_{El}	F_{TS}
2.25% C	4.50	—2.25
0.02% P or 1.5% P_e	0.76	1.50
0.75% Si	2.25	1.50
Total $\bar{F}$ =	7.51	0.75

$\bar{F}_{El} = 7.51 =$ *35% elongation*

$\bar{F}_{TS} = 0.75 =$ 45,000 psi tensile strength

7. The above are examples of ways to use the analysis described in this paper. This building block of information will become increasingly important as the other blocks (studies of matrices of pearlite, bainite, etc.) become available, and are combined in similar form. It should be noted the analysis is not complete for all properties; fatigue, high temperature, and impact properties are not included.

8. In addition to the above ways in which the analysis will be useful, it is hoped it may serve to emphasize the industrial potential of ductile cast iron. That such a relationship as found exists between mechanical properties and composition attests to the reproducibility, as well as the generally desirable properties, of this remarkably utilitarian material. Much has been said about the undependability and inabil-



ity to produce this material commercially to reasonable standards. It seems only natural to the authors that such an infant material, yet new in commercial production, should still present problems in manufacture. These serious problems do not prevail in carefully controlled laboratory production and will diminish in shop practice as quality control standards and experience improve.

IV. Acknowledgment

The authors are indebted to George E. Schmidt, Jr., Research Assistant, Metallurgy Dept., M.I.T., for preparing the illustrations used in this paper.

Fig. 11—Comparative effect of elements on change in tensile strength and yield strength for change in elongation.

NOTES ON AIR FURNACE DESIGN AND OPERATION

By

J. E. Rehder*

ABSTRACT

A discussion is presented, based on operating experience, of various specific factors in the design of the air or reverberatory furnace, and its operation and maintenance. Reference is mostly to furnaces used in duplexing practice and fired with pulverized coal, used in the malleable iron industry in the United States and Canada, but oil firing and cold melt practice are mentioned.

Introduction

The air furnace, used either with a solid metallic charge or supplied with liquid iron from a cupola, is almost the standard melting unit in the malleable iron industry on the North American continent, and a major proportion of the iron melted (entirely of "black-heart" grade) is from such units. The air furnace is also used to a small extent for high-quality gray irons such as for chilled iron rolls.

Among the attractive features of an air furnace are relatively low capital cost, reasonable operating cost, ability to attain high metal temperature, the provision of a relatively large bath of iron of uniform and controllable composition, and the ability to provide irons of low carbon content. Disadvantages in some circumstances are the necessity of oxidizing conditions when high iron temperatures are desired, a capital and operating cost midway between cupola and electric furnace, and the awkwardness of handling high-carbon irons. However, the furnace is useful and versatile, and is amenable to improvement.

As experienced melters know, the air furnace is a responsive piece of equipment and correct design and attention to details of operation are essential to maintaining the low costs and high quality of metal produced, of which the unit is capable. The writer has observed several instances where operating costs were high, desirable tapping temperatures were almost unobtainable, or iron composition was erratic; and in every case the difficulty could be traced to poor design of the air furnace, incorrect operation due to lack of understanding of how such a furnace works, or deliberate "pushing" of the furnace so that it was operated at much over its designed capabilities. It is hoped that the following notes may be of interest and some help to air furnace operators, and although the duplexing air furnace will receive major attention, the cold-melt furnace is very similar and the same principles apply.

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Design

The function of the air furnace in a duplexing cupola-air furnace melting equipment is to receive molten iron from the cupola and to increase its temperature, adjust its composition, and provide a bath to absorb fluctuations in demand for iron from the pouring lines. These should be, and are, accomplished simultaneously and effectively, but uniform and reasonably efficient air furnace operation is dependent on the understanding and maintenance of a balance of many factors. Through experience certain optimum furnace proportions and firing conditions have been developed, which have been found to provide a good working balance, and it is essential to pay attention to these matters if a good furnace operation is to be obtained.

In a cold-melt air furnace, of course, the charge must first be melted down to a bath, which takes the major portion of total heat time, but then the superheating of the bath and the adjustment of its composition are the same function, and are subject to the same rules as the furnace operating as a duplexing unit.

A considerable amount of control can be had over the chemical composition of a liquid bath of iron in an air furnace, and this is one of the inherent advantages in use of the furnace. Since superheating is a primary function, and the hottest flame is oxidizing in nature, the normal tendency of an air furnace under conditions of most rapid heat input is to decrease the carbon, silicon, and manganese contents of the iron. The rate of oxidation of carbon can be under considerable control, and net decrease in silicon content can be held to zero or less by suitable operation, but the effect of loss of manganese can be compensated only in the charge or feed to the furnace, or in emergency by alloy additions to the bath. In duplexing operation the carbon, silicon, and manganese contents of the molten cupola metal are ad-

justed to make allowance for expected changes in the air furnace. Phosphorus content of the iron is changed very little in the air furnace (a very small increase due to oxidation of a small amount of iron) and sulphur content is increased by 0.01 to 0.04 per cent depending on the sulphur content of the fuel used in firing.

A decrease in carbon content of the iron in the air furnace is usually desired, and it is on this factor that preliminary design conditions are based. The carbon-decreasing power of an air furnace is obviously related to

1. The nature of the firing flame or furnace atmosphere.
2. The surface area of the bath.
3. The time the iron is in the furnace.
4. The depth of the bath of iron.
5. The temperature of the iron.

A figure in common use, and which has given satisfactory results in duplexing furnaces, is that with a furnace holding 2 to 2½ hr supply of iron, the carbon content will decrease at the rate of about 0.02 per cent per square foot of bath area, per ton throughout, per hour. For lower carbon irons, therefore, the specific bath area is increased and when duplexing, the cupola is operated to provide lower carbon iron. Data relating desired carbon content and bath area are summarized in Table 1.

TABLE 1

Desired Carbon Content, %	Sq Ft Bath /ton/hr	Expected Carbon Decrease, %	Desired Cupola Carbon, %
2.30	22	0.44	2.74
2.40	20	0.40	2.80
2.50	18	0.36	2.86
2.60	16	0.32	2.92
2.70	14	0.28	2.98
2.80	12	0.24	3.04

Experience has shown that the ratio of length to width of an air furnace should be from 4 to 1 for large furnaces to 5 to 1 for small furnaces, and from this it follows for an average ratio of 4.5 to 1, that the width of the bath is given by

$$w = \sqrt{\frac{\text{area}}{4.5}}$$

and of course the length will then be

$$l = 4.5 \times \text{width}$$

Usually, the height of the furnace side wall at the tap hole is about one-twelfth the length. Various profiles of side wall have been tried, from flat (i.e. the top of the side wall parallel to the surface of the bath from the burner end wall to near the bridge wall), to profiles with a hump or dip about midway along the furnace. However, the writers' experience is that a gentle straight line slope, of about 1/3 in. per ft, from the burner end wall to a few feet short of the bridge wall, with then a sharper slope to the bungs over the bridge wall, is the most efficient profile and gives most rapid and uniform heating.

The rise or arch of the removable bungs which constitute the roof of the furnace should be as low as practicable, and the flattest arch which will stay up without requiring very strong and heavy bung frames, should be used.

Very deep baths of liquid iron are not desirable in air furnace practice since heating is slow, carbon reduction is slow, and serious temperature and composition differences may exist between top and bottom layers. In practice from 2 to 2½ hr supply of iron is desirable in a duplexing furnace to give time for heating and composition adjustment, and to provide a reservoir, and this figure in combination with the above data will determine the bath depth.

The tap hole should be located about one third of the distance from burner end wall to bridge wall, and the bottom should be sloped about 3/16 in. per ft to the burner end wall, and 1/8 in. per ft to the bridge wall. The bottom should be dished so that the center is about 2 to 4 in. lower than the level at the side walls, depending on the width of the furnace, and a further drop of about 2 in. should be provided toward the tap hole to ensure adequate drainage on emptying the furnace.

The top of the bridge wall should be as close to the level of the iron in the furnace as practicable, and in practice should be only 2 to 5 in. above the metal line when the furnace is full. An unnecessarily high bridge wall will deflect the flame upwards so that the end of the bath will not be properly heated, which will result in dull iron at the tapping spout.

A firing rate of 105 to 110 lb of coal per hour per square foot of transverse cross-section above the metal at the tapping spout is necessary in order to give rapid heating, high flame temperature, and a flexible, smooth working furnace. With the firing rate determined, the area of the bridge wall opening can be found by assuming that ½ sq in. of opening is required for each pound of coal fired per hour. The bridge wall opening is very important, and close attention must be paid to ensure that it is maintained at the correct figure since it is in effect the throttle of the furnace that controls heating efficiency.

Adequate draft is necessary in the air furnace stack, sufficient to provide for the maximum firing rate under the poorest draft conditions, with some spare capacity so that a draft damper at the base of the stack will still be effective. A stack cross-sectional area (50 to 60-ft height) of about 1.3 sq in. per lb of coal fired per hour should be suitable

The data on firing rate, bridge wall opening, and stack are summarized in Table 2, where heat unit equivalents are also given for easy conversion to oil firing. There is indeed indication that the heat unit basis is the correct one, and that, for example, more of 14,000 Btu coal must be fired for optimum conditions than of a 15,000 Btu coal. How far this con-

TABLE 2—FIRING RATES

	Lbs per Sq Ft Per Hr	Millions Btu Per Sq Ft Per Hr
Furnace Cross-Section at Tap hole	105-110	15.2-16.0
Bridge Wall	288	41.8
Stack	110-115	16.0-16.7

ception can be extended in the direction of lower heat content coals is not known to the writer.

The questions of burners and coal pulverizing equipment will not be gone into here, since satisfactory equipment is readily purchased and design information is of secondary importance. Burner setting and location should follow the recommendations of the manufacturer, and it should be noted that present-day practice is towards single, water-cooled burners set at relatively flat angles of 3 to 7 degrees from horizontal.

Example—As an example of the above data, consider a specific requirement. A duplexing furnace is to provide 10 to 12 tons per hour of 2.50 per cent carbon iron.

Carbon desired	2.50 per cent
Area of bath (Table 1)	18 sq ft/ton/hr
Output per hour	12 tons
Total area of bath	216 sq ft
Width of bath	7 ft
Length of bath	31 ft
Height of side wall	30 in. (see below)
Furnace contents per inch of height	4 tons
Total bath at 2½ hr	30 tons
Bath depth center of furnace	9½ in.
Bath depth at tap hole	11½ in.

Assuming a bung frame which will provide a rise of 9 in. in 7 ft, the area under the bung will be given by

$$\frac{2}{3} \times \frac{9}{12} \times 7 = 3\frac{1}{2} \text{ sq ft}$$

Since the bungs must extend to rest on the middle or outer part of the top of the side wall, the total length of the bungs must be about 20 in. more, or a total of about 8 ft, 10 in., and the effective side wall height will then be increased by the rise of the bung line in 10 in., or about 2 in. The height to which the side walls will be built at the tap-hole would then be 30 less 2, or 28 in. above the metal line, or 39½ in. above the tap hole.

Cross-sectional area furnace at tap hole 3.5 plus 17.5	21 Sq ft
Firing rate	2270 lb coal/hr
Bridge wall opening	1135 Sq in.
Area of Stack	2950 Sq in.

Three access doors are usually provided, a small one near the burner end wall for lighting and for observing the condition of the burner port, and two larger ones, one near the tap hole for making bath additions and one near the bridge wall for skimming slag. The latter, of course, must be carefully adjusted in height so that the door sill is close to the metal line for ease in skimming slag, and a sloping sill is very helpful in this direction. The doors should be the minimum size that can be conveniently worked through.

On duplexing furnaces the iron is delivered from cupola to air furnace by a refractory trough, and this should enter the furnace 4 or 5 in. above the surface of a full bath. Troughs or spouts 24 ft long are in satisfactory service, but normally it should be possible to arrange a shorter spout. The temperature drop in the spout under steady running conditions amounts to about 1 degree F per ft of length.

The preliminary sketch for the furnace in the example described above would be as in Fig. 1.

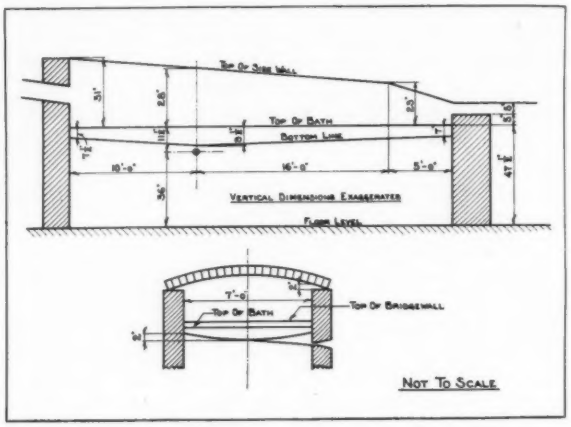


Fig. 1—Air Furnace Design Example.

As a final point in design considerations, it should be noted that on the basis of the data available to the writer, all of the above considerations are directly applicable to air furnaces using oil as fuel, on the basis of equivalent heat units or Btu. If a firing rate of 108 lb of 15,000 Btu coal per square foot per hour is suitable, then use of 8.1 gal of 200,000 Btu oil per square foot per hour will also give satisfactory operation.

Cold melt air furnaces, where all or most of the furnace charge is put in the furnace in the solid state, will be found to operate efficiently under the above rules. However, in such furnaces it is sometimes necessary to increase the height of side walls especially along the tap hole area in order to give sufficient room to get all of the charge in and still allow room for flame to travel down the furnace. It will be found advantageous to keep this increase in volume to a minimum in the interest of fuel economy. Firing rates during melt down will be found to be about 20 per cent greater than the rate during superheating, and the latter should be the design figure.

Operation

Various details of operating practice will be described with no attempt to be exhaustive, but with hope of covering the more important factors.

Coal—The desirable attributes of coal suitable for use in pulverized coal fired air furnaces are good friability or grindability, high heat content, low ash and sulphur contents, and a volatile matter content of the right order for the size of furnace involved. Such coals are premium coals, and are becoming more difficult to obtain to desirable specifications.

Grindability is very important, and should be at least 90 and preferably more on the Hardgrove scale. If a coal will not pulverize easily, combustion char-

acteristics will be poor and other desirable properties obscured. One of the most important characteristics of a coal in determining grindability is its moisture content, and this usually is not under the control of the coal producing company due to transit and storage conditions. Wet coal will not pulverize properly, and it is essential to either obtain the coal in a dry state and store under cover, or to provide facilities for drying the coal. Considering the large quantities of waste heat in an air furnace stack, the latter should not be difficult or costly. The moisture content of the coal may be determined regularly and a safe maximum figure set, but in the writer's experience a rough and practical criterion is that the coal, when disturbed, should yield dust. If the coal is dry enough to dust freely, little trouble should be had with effects of moisture content on pulverization.

Volatile Matter in Coal

The volatile matter content of the coal is important, since it affects the length and type of flame obtainable. Volatile matter content below 18 to 20 per cent is undesirable for most furnaces, since a short, hot flame is produced that can do serious drainage to the side walls and bungs near the tap hole while not heating the far end of the bath properly. In small furnaces, however, holding 6 to 10 tons and 16 to 20 ft in length, 18 per cent volatile coal can be used successfully and with some advantage. High volatile matter content, 30 per cent or over, results in a very long flame that is still burning after it has passed over the bridge wall, which does not heat the bath rapidly and results in high fuel consumption. Higher volatile coals also in general do not have good grindability, which is a further disadvantage. A range of 22 to 26 per cent volatile matter will be found satisfactory for most furnaces.

The ash content of good air furnace coal should be low for indirect reasons, but it is unwise to specify very low ash contents until all other characteristics of the coal are known. High ash content is normally undesirable because it decreases the heating value of the coal, increases wear on pulverizer beaters, and deposits large amounts of fly ash in flues and waste heat boilers.

The sulphur pick up of iron in an air furnace from the firing flame is proportional to the sulphur content of the coal, and in practice a maximum sulphur content of the coal is usually set at 1.0 per cent.

The heating value of a coal, in Btu per pound is important in air furnace practice, and in order to attain the flame temperatures of 3200 F or more usually necessary, a heating value of 14,000 Btu per pound minimum is required. Optimum furnace operation requires 14,300 Btu per pound or more.

In Table 3 are given the limits within which it is considered the properties of an air furnace coal should fall.

TABLE 3—COAL PROPERTIES

Moisture	low
Volatile matter, %	20 to 27
Ash Content, % max	7.0
Sulphur, % max	1.0
Btu per lb, min	14,000
Grindability	85 Hardgrove min.

Pulverization—Assuming a suitable coal is in use, of low moisture content or properly dried, and a pulverizer maintained in good condition, a large degree of control over the operation of an air furnace can be obtained by suitable manipulation of the rate of coal feed, the fineness of grind control, and the amount of secondary combustion air. The "bottom air" setting on Whiting pulverizers usually does not need to be changed once it is properly set for the furnace and the particular coal being used.

The rate of coal feed obviously determines the rate of heat input into the furnace, other things constant, and there is usually a fairly narrow range within which a given furnace operates most efficiently with the highest flame temperature and heating rate of the iron bath. Conditions must be kept in balance, however, in accordance with the data given above, and no amount of over-firing will adequately correct for, for example, a too-large bridge wall opening. Coal feed and heat input may, of course, be decreased as necessary for holding over a shut down, and must be decreased towards the end of a heat as the furnace is being drained and the bath becomes shallower. Overheating at the end of a heat will invariably cause silicon content to increase rapidly by reduction from the bottom, with resulting decrease in carbon content and the production of an iron with poorer fluidity and greater shrinkage than expected.

Since the important factor is pounds of coal per hour and not some figure on a dial or feed screw setting, the calibration of the coal feed screw should be checked frequently by actual weighing. Such trouble as this may seem will be adequately repaid in a more uniform and systematic furnace operation.

One factor frequently overlooked in firing an air furnace is the increase in volume and cross-sectional area of a furnace during a week's run, due to erosion and slagging of side wall and bung brick. This increased area demands a greater coal feed for proper combustion, and attempts to fire a furnace on Friday exactly as it was done on Monday may result in trouble with dull iron.

Fineness of Coal

The fineness control setting, on Whiting pulverizers at least, is effective, and the type of flame obtained varied accordingly. Use of relatively coarse coal is very useful when oxidation rate must be decreased, and if the bath is hot the carbon content may even be increased by proper use of a coarse coal setting on the fineness control.

There is a tendency in some quarters, especially in the front office, to suggest that an air furnace can be operated on a set time schedule of pulverizer and air settings. This is a serious mistake, and experienced melters know that an air furnace can be operated properly and efficiently only by constant attention to details throughout every heat. So many unpredictable factors can upset the balance of operation, such as variations in moisture content and grindability of coal, erosion of linings, build-up of bridge wall, changes in stack draft due to weather, to mention a very few, that the attention of an experienced melter with authority is essential.

A fairly common practice which in the writer's opinion is actually uneconomic, is to fire annealing furnaces and melting furnaces, or even several melting furnaces, from a central pulverizer station. The type of coal most suitable for firing annealing furnaces is seldom the best type for melting, and in a central operation both processes must suffer. Even when only melting furnaces are on the central pulverizer, the situation is not good since there is no way of controlling fineness of grind for individual furnaces, which limits the flexibility of operation. Unit pulverizers for each melting furnace in operation, with a central unit for annealing furnaces only, are considered desirable and economically justifiable.

Bridge Wall Opening—Even though correctly proportioned at the start of a week's furnace operation, the size of the bridge wall opening must be checked frequently and will usually have to be adjusted occasionally. There is a tendency to build up a mixture of coal ash, bits of slag and fluxed refractory on top of the bridge wall, gradually decreasing the area of the opening, and the one or two bungs over the bridge wall must then be raised slightly by inserting a sliver of brick or a "split" under each end of the bung. Attention to the size of this opening is important to good fuel economy, high operating temperature, and uniform operation.

Pressure—The pressure of the gases in an operating air furnace may be varied from less than atmospheric, characterized by no appearance of flame at openings and by drawing in of air through these openings, to well above atmospheric, when considerable flame is apparent at all openings. This is accomplished usually by a damper in the base of the stack which short-circuits a smaller or greater amount of the stack draft. Operation of a furnace under negative pressure, or high draft, is unwise, as a considerable volume of air is drawn into the furnace which not only disturbs flame distribution but cools down the furnace gases and the metal bath. On the other hand high pressures inhibit good combustion, and make working conditions poor. A preferred pressure is one which is only slightly above atmospheric, characterized by light brushes of flame especially around the slag door. It will be found worth while to keep the pressure adjusted by attention to the stack damper. Attempts to adjust furnace pressure by varying the bridge wall opening are ill-advised unless the opening is of the wrong size, and will upset the balance on which good furnace operation depends.

Flame Condition—The key to efficient air furnace operation is the condition of the flame obtained, and its control and characteristics are very important. Unfortunately, it is a difficult subject on which to write, since instrumentation is not good and so much depends on experience and judgment. No attempt will be made to go into detail here for lack of time, but some points will be discussed.

Usually the highest flame temperature consistently attainable is desired, in order to provide rapid heating of the bath, and this is characterized, when looking up through the furnace over the bridge wall toward the burner, through a peep-hole at the base of the stack, by a semi-clear, even, slightly rolling flame spreading rapidly from the burner and completely and evenly filling the furnace. If the slag door is not visible, the flame is too "heavy" and low in temperature; if the burner end wall can be seen clearly, the flame is too "light," heat input is down, and oxidizing conditions probably severe.

The use of an optical pyrometer, such as the Leeds and Northrup, on a blackbody setting, is very useful, and readings should be taken regularly and entered on the furnace log. The pyrometer is usually sighted through the opened door near the tap hole, onto the flame, standing about 10 ft back from the furnace wall. Reproducible readings are obtained, and although whether they actually represent true flame temperature or a combination of flame and far side wall temperature is uncertain, it is a fact that they correlate very well with flame condition and rate of bath heating, and are very useful for control purposes. Readings of at least 3200 F are necessary for satisfactory heating rate, and readings of 3240 to 3260 F are regularly obtainable with good coal and a well operated furnace, and represent rapid heating rates and good efficiency.

Control of Flame Temperature

Control of flame temperature and appearance is, of course, by means of coal feed, fineness of grind, amount of secondary air, and furnace pressure. All access doors should be kept shut and reasonably well fitting, since considerable quantities of heat are lost through such a large opening as an open door by radiation.

Bath Operation—This includes slag, iron composition, and furnace alloying additions.

It is essential to reasonable efficiency to keep an air furnace bath skimmed as completely free of slag as possible. This is important, as even a thin layer of slag allowed to spread towards the burner end will markedly decrease the heating rate of the bath. The motion of the flame will drive slag towards the skim door near the bridge wall, and slag should be skimmed whenever it begins to reach up more than one-third to half way to the tap hole. On cold melt furnaces, of course, there are considerable amounts of slag on melt-down, and on duplexing furnaces the bath should be thoroughly skimmed of slag on first filling because there is a relatively heavy blanket left from the previous day's run.

Attempts to control slag composition are made in a few shops, by adding lime for thickening and/or soda ash for desulphurizing. Since the refractories are all acid in character, attempts to make the slag more basic usually result in more severe erosion of side wall brick at the slag line, and there is some doubt as to the net economy. Use of soda ash for desulphurizing is mildly effective, but metallurgical conditions (an oxidizing, acid environment) are not conducive to good efficiency and costs are high. If, however, a course or two of basic brick were laid in the side wall at the slag line, much more freedom would be had in control of slag composition and desulphurizing power. Chrome-magnesite brick must not

be used since the carbon in the iron will reduce some of the chromium oxides to metallic chromium which then enters the bath.

The composition and temperature of the bath of iron in a normally full air furnace varies from the surface to the bottom, and the principal objection to deep baths, aside from heating rate, is that this variation becomes appreciable. The surface layers of iron are hotter, for obvious reasons, and the silicon content lower and carbon content higher, than those of the layers near the bottom. The silicon content of an air furnace bath with iron temperature of 2850 to 2900 F is the net result of loss by oxidation at the surface and gain by reduction from the bottom refractory, in the lower layers. Carbon content of the lower layers of iron is lower than average because it is the agent which reduces the silica of the refractory to silicon and is lost upward through the bath as bubbles of carbon monoxide. When the bath is thin and/or very hot, as towards the end of draining the furnace, increase of silicon content by reduction is more rapid than loss and silicon content of the bath can increase rapidly. It is a common practice to decrease the silicon content of the last five or six charges in the cupola, in order to compensate for a rise in bath silicon content near the end of a day's run, by substituting malleable for silvery grade pig iron or by decreasing the number of silicon briquets in the cupola charge. However, even during the normal course of operation, the net silicon loss of iron from the cupola to iron at the air furnace spout is frequently zero due to these factors, and it is not impossible to operate continuously with a net gain in silicon content in a duplexing operation.

When through faulty control or unforeseen occurrences an adjustment of bath composition becomes necessary by means of a carbon or alloy addition, some care must be used. Additions of carbon, ferrosilicon or ferromanganese will cause a mottling effect which persists for several minutes after the last alloy is dissolved, and iron should not be tapped under these conditions. A convenient method of obviating this mottling tendency and its potential holding up of tapping and pouring iron, is to charge 50 to 100 lb of light steel scrap before all of the alloy is dissolved. This destroys the increase in mottling tendency and permits continued tapping. The reason for this effect is not known, since the amount of steel is small in relation to the amount of iron and the change in composition is very small. However, the procedure is effective.

None of the additions for increasing carbon content have good or even reasonably uniform recoveries, in the writer's experience. Preferably coarse coal from the pulverizer should be used, but this is not a rapid method. Of usual additions, petroleum coke is considered poorest, with low, very uncertain recovery, and some samples of petroleum or pitch coke contain as much as 4 per cent sulphur. Ordinary coke is not much better in recovery or predictability. Low-volatile anthracite coal is reasonably satisfactory, but crushed carbon electrode scrap has been found the most practicable addition for increasing the carbon content of the bath rapidly.

Maintenance

Pulverizer and Burner—Coal pulverizing machinery wears fairly rapidly, and it is essential if control is to be maintained over the quality and nature of the pulverized product obtained, to keep the machinery in a uniform working condition. It is a serious mistake to let all of the beaters wear out and then replace them, since then large changes in the pulverized coal will be obtained and uniform operation impossible. It is much more sensible, and no more expensive, to replace beaters in pairs at such a rate that by the time the original pair are reached, they are due for replacement again. The same, of course, applies to turning beaters over to wear the opposite face. In this way a nearly uniform pulverizing efficiency will be obtained, with a reasonably uniform quality of output.

The tramp iron box should be emptied weekly, but of course, if a regular beater replacement program is in use, this will be automatic. The annular wear plates should be checked occasionally, and replaced when thin. The necessity for periodic re-calibration of the coal feed screw because of wear has already been mentioned.

Due to the longitudinal expansion and contraction of the furnace, burner settings are sometimes disturbed and any tendency to cut one side wall, or to cut the roof or to bounce the flame off the bath, should result in an immediate check of the burner. Coking on the sides of the burner port can seriously deflect the flame, and the burner port should be modified until coking is at a minimum.

Bungs and Side-Walls—It will be found worthwhile to pay attention to bung tension or pressure, by adjusting the tension nuts, especially on new bungs. In the past there has been experimentation with thinner and thicker roofs, but the bung brick at present in use seem to present a reasonable compromise. Thicker roofs, or the use of insulation over a roof, results in much more rapid erosion of roof brick, a much shorter life, and increased cost. Thinner roofs are more difficult to set up properly when new, and although heat loss is greater, it must be remembered that a bung nearing the end of its life, with the brick only an inch or less thick, seem to last a long time, due probably to lower working face temperature resulting from more efficient cooling. As a matter of fact, it is an indication of a well-operated furnace, with uniform flame distribution, when bungs remain in use with brick only 1 to ½ in. thick and uniform in thickness except for a normal thickening towards the more protected ends of the bung. This also, of course, indicates uniform brick quality.

Since the inner or working face of a bung becomes much hotter than the outer face, and more especially since on first heating up the inner face expands more rapidly, there is a severe pinching or compression effect on the inner face of the bung brick under these conditions, and if the bung brick are a perfect fit for the radius involved when the furnace is cold, spalling of the brick is very likely to occur, with resultant shortening of life. It would seem wiser, therefore, to use a bung brick made for a radius appreciably great-

er than that of the bung, so that when the brick are laid up in the bung frame cold their inner faces are each 1/64 to 1/32 in. apart. This leaves room for expansion, and spalling is much less likely to occur.

It is useful frequently to leave a space or slot in the roof just behind the back edge of the bridge wall by setting two bungs about 1 to 2 in. apart. Provided the stack draft is adequate, the air drawn in through this slot drives the flame downward before it reaches the stack, resulting in much decreased erosion of the brick at the throat of the stack. The solid material carried over the bridge wall with the flame is highly erosive to refractories, as can be seen in the following analysis of the deposit in the base of a duplexing air furnace stack.

TABLE 4—STACK DEPOSIT ANALYSIS

SiO_2, %	42.9
FeO, %	30.7
Fe_2O_3, %	3.6
Al_2O_3, %	13.1
CaO, %	4.7
S, %	0.05
Total	95.0

Remainder unburned combustible matter and alkalis.

On side walls it is a mistake, of course, to thicken the side wall refractory in order to decrease heat loss since the added brick erodes rapidly due to higher face temperature and refractory cost per ton melted is appreciably higher. In one specific case, increase of sidewall thickness from 13½ in. to 18 in. (the furnace was a little to broad originally) resulted in an increase of nearly 10 per cent in number of side wall brick consumed per ton melted. Use of side walls thinner than 13½ in. may save on brick cost, but not many are willing to take the risk of a run-out or collapsed wall part way through a week's run.

Bottoms—Many cold-melt furnaces and all duplexing air furnaces use brick bottoms, and there has been much experimentation with types of brick, methods of laying, and contour of bottom, in order to obtain low refractory cost and to decrease to a minimum costly and troublesome floating of bottom brick. The use of the air-cooled bottom furnace, which provides for heat dissipation from below the bottom brick, has shown much better results with brick bottoms than has the solid bottom furnace built on a firm continuous foundation, and results in a 30 per cent saving in bottom brick consumption. This is undoubtedly due to lower average temperature in the bottom brick and resulting higher strength.

Much depends on experience acquired by the melter and brick laying crew on the particular furnace being used. Comparison of bottom cost and life between plants is frequently misleading, since faulty operation can decrease bottom life, and one plant may of necessity be tapping iron 75 F hotter than another. Although bottoms are usually replaced in sections, since wear is uneven along the length of a furnace, laying time of a complete bottom under normal conditions may be judged by one practice where about 3300 brick are laid in a 7 by 30-ft furnace by four bricklayers and two helpers in 7 hr actual laying time.

Although the refractories in a duplexing furnace may seem to be under more adverse conditions than those in a cold melt furnace, actually refractory material cost is lower in a duplexing furnace by about $1.70 per ton melted.

Two Furnaces—Many cupola-air furnace duplexing installations use a cupola and one air furnace, and relining and patching of the air furnace are done over the week-end usually at overtime pay rates. If any trouble develops, such as a run-out or a floated bottom, the whole installation and associated molding facilities are out of operation for some days. The writer made a detailed study some time ago (unpublished) and showed clearly that the complete cost of installing a second air furnace would be paid for out of savings in less than two years in an 80-ton per day plant. The two furnaces are worked alternately, and use the same pulverizer and, of course, the same cupola. Repairs are done during the week on straight time and without hurry, and the repaired furnace is ready as a spare in a few days. A further saving is made possible by the fact that it is no longer necessary to work refractory life in full even weeks. With one furnace, it must be more than able to go the full ensuing week, or a major re-line is done, wasting possibly a half week's refractory life. With two furnaces, a furnace can be operated to the full life of the refractory before repairs.

Conclusion

It is not possible in a paper of this kind to describe in detail all of the factors involved in air furnace practice, and in the above only the more important points have been touched on. The essential thing is to understand the nature of an air furnace and its operation, and to appreciate the fact that to obtain uniform, low cost operation a balance of various factors must be obtained and continued under intelligent supervision.

Acknowledgments

In the course of the writer's experience in air furnace operation many people have been most helpful, and improvement in practice is dependent on such interchange of information. Of particular help and encouragement have been Mr. W. R. Jaeschke, Mr. J. Holik, Mr. R. T. Wilson, and Mr. A. Davies.

ARE YOUR COSTS RELIABLE?

By

R. T. Lewis*

You want to know with practical accuracy the cost of various casting items. You require this knowledge for multiple management uses essential to the most successful operation of your foundry. The variations between costs of different items should be consistent with their basic cost characteristics, the production processes employed and the equipment used. Does your cost system portray an accurate picture?

The purpose of this paper is to spotlight some of the more common and pertinent discrepancies inherent in costs developed from inadequate and incorrect cost systems today. These faults are widespread enough to exert an adverse effect on the vigor and health of the industry in general. See if you can identify one or more of these defects in your system. If you can and if you take corrective action the purpose of this paper will be fulfilled and worthwhile.

This is not intended to be a technical treatise on foundry cost accounting. It is intended to interest and reach Management in a manner which will aid them in demanding and attaining costs in which they can have confidence and will provide valuable assistance in reaching most profitable executive decisions.

If you are a cost accountant you must produce and sell this kind of cost to your Management if you wish to attain the kind of recognition and amount of compensation which men of your profession are capable of earning.

The technical information you need is available from articles and manuals published and indexed by the various foundry trade societies, the American Foundrymen's Society and the National Association of Cost Accountants. The Gray Iron Founders' Society has published numerous articles on costing problems and a basic cost manual.

As we examine in departmental order these most common and important discrepancies as well as their effects, keep these questions in mind. Does your cost procedure reflect the character of the casting or does it, instead, reflect the character of the costing? How does this effect your competitive position? How does this effect your earning capacity? Are your costs good enough to clarify your problems or poor enough to confuse them?

Melting Department

The most common, serious cost errors in the Melting Dept. evolve from failure of many foundries to set up their allocation of the Cost of Metal at the Spout on the Yield Basis. What is the Yield Basis? Simply stated this is the adjustment from average, of the metal charge to a casting on the basis of the pounds of melted metal required to pour the casting in ratio to the weight of good casting produced.

The majority of foundries develop the average metal cost per pound of good castings produced without difficulty and with reasonable accuracy. With but little variation, if any, they use this average cost on all casting items or accounts. While they thus succeed in allocating Metal Cost at Spout with fair accuracy in total, they fail to realize the extent of error involved as between substantial casting items or accounts having yields widely varied from the foundry average.

Let us explore the extent of such variations and their cost implications. The writer can attest that he knows one large consumer's requirements with an average yield of 40 per cent and another with an average yield of 75 per cent. A check between various castings in the writer's foundry has revealed that they vary from a low of 30 per cent to a high of 85 per cent.

Consider this factual example: A gray iron foundry has an average casting yield of 60 per cent. In other words it produces 60 lb of good castings for each 100 lb of metal poured. Per pound of good cast the average metal cost was 2.8 cents and the average melting and distribution cost was 1.3 cents, total cost at spout 4.1 cents. The simple mathematical computation required to develop a Yield Table will reveal that the at Spout Cost as between a yield of 30 per cent and 85 per cent will result in a cost variance from a low of 3.7 cents to a high in excess of 5 cents.

Any iron foundry using the Yield Basis will attest that on an item to item basis, differences of from ½ cent to 1 cent per pound will be found frequently. Is this significant to your profit and selling position? Do

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you want and need the truth?

The only obvious reasons foundries can have for neglecting this inadequacy in their costing is:

1. Fear of the truth. They would like this basis when the correct answers are favorable but do not have the courage to face the unfavorable situations. Of course application of this method to only favorable situations is a sure invitation to severe losses.
2. Lack of initiative or pure laziness.
3. Misapprehension as to the cost involved in making the necessary determinations of the yields of individual casting items.

Why this latter reason should exist we do not know. True, starting from scratch, the initial determination and recording is no small job. The writer can almost guarantee that once you get started on the most important items you will not quit until you have completed all. After this, the continuation on new or revised items will be routine and inexpensive. Even the initial cost is a most profitable investment, particularly so in view of the expense deduction under current high tax rates.

Alloying

Adjusting alloy costs on the basis of yield will also reveal differences of significant importance. The same principles are applied as for the basic melt. If the Yield Basis is put into practice for Metal Cost the extension of this method to alloying is simple and cheap. The only additional factor to consider is where alloy scrap is recycled into alloy melt, credit is due for such recovery.

Many foundries quote alloy cost at a separate or supplementary price or cost. They do not include this cost in the total casting cost and overlook or miss their normal mark-up. They are thus not compensated for the capital, know-how and hazard inherent in the alloying operation. These factors coupled with a yield discrepancy frequently result in alloying prices verging on the ridiculous and involving substantial losses.

Molding Department

The greatest common source of discrepancy in the Molding Department Cost Area is the failure by the majority of foundries to develop varied overhead rates for the different types of molding operations. In conjunction with this is the failure to identify to the greatest extent possible as Direct Costs the direct costs for convenience dumped into overhead accounts where they create problems in equitable allocation.

If you change, expand, modernize, mechanize or enter into more complex operations it is essential that the costing procedure receive comparable revision if you are to derive the benefit due from your progress.

Are you one of the many foundries using a cost system employing an average overhead rate applied to Direct Molding Cost, Direct Molding Hours or similar basis? This system may have been adequate when your operations were fairly consistent and simple. Assuming however, that from bench and floor molding you have progressed into squeezer, rollover, crane, conveyorized, sand handling and a similar diversity of molding operations. If as you advanced into one or more of these diversifications you have not discriminately altered your basis of overhead application, you are now costing one type of production excessively to the benefit of another. Your natural and inevitable trend will be to acquire more of the type less favorable than they appear and lose out on the types of work more favorable than they appear to be.

Calculations of the differences in average overhead rates as compared to varied rates from bench and floor to machine floors commonly reveal differences readily as much as 100 per cent of the direct cost against which the overhead rate is being applied. Carried further into comparison with highly mechanized operations this discrepancy increases to from 200 per cent to 400 per cent and more. These differences will, of course, vary considerably between cost systems depending on to what degree direct costs are identified as such.

The cost discrepancies developed in this department by these failures will exceed in amount those already described as critical for the Melting Department.

It is, of course, a fact that no cost system will produce 100 per cent accurate costs. A well developed cost system merely produces reasonable accuracy and consistency in costs. Considering abnormalities of various items such as variations of direct molding costs from ½ cent to 3 cents per pound as compared to an average of 1 cent, the development of items from a jobbing to a production status, from loose work to machine production, etc., it becomes evident that the exercise of common sense is essential to effective operation of even a comprehensive cost system.

It is the writer's opinion that if there is any least common denominator of foundry operation it is the capacity of the foundry to produce. For any pressing purpose this production capacity may be expressed in tonnage, sales, cost absorption requirements, etc. An expedient test to determine the adequacy of the molding overhead rate in abnormal situations is to calculate to what extent the rate being applied would absorb the total overhead if the entire capacity or type of capacity you have, were devoted to production of such abnormal items. It is good insurance particularly to thus establish a floor or minimum amount to be absorbed for any given unit of capacity.

The following illustration may illustrate this procedure. Suppose a 20-lb casting is being produced at a direct cost of 20 cents per mold and an applied overhead rate of 200 per cent absorbs 40 cents per mold. Assume that by any device the Direct Cost is cut in half or in other words the production is doubled for the same direct cost. It may be readily observed that if the entire capacity were thus changed it would be necessary to double the overhead rate to meet the required total overhead absorption. Assume further that the device accomplishing this change in Direct Cost involved added mechanization and the transfer of some former Direct Labor operations to an Indirect Labor classification. It then becomes apparent that in addition to doubling the overhead rate it is necessary to further adjust it to absorb the cost of the added mechanization and indirect labor.

Such expedient calculations by your cost accountant pending the establishment of variable overhead rates for the different types of molding operations will modify cost discrepancies. They will also preclude

your getting started on a cost precedent which would in effect liquidate your improvements in behalf of your customer.

Core Department

The same discrepancies exist between different types of core production as exist between different types of molding production methods. They evolve from the widespread failure to establish separate cost centers and overhead rates for bench, machine, blower, assembly, and other types of core production.

The majority of foundries are still using an average overhead rate for the composite of the different types of coremaking. Development of different rates for each type will reveal great differences from the average.

By far the greatest discrepancy prevalent in core costs develops from the fact that it is common practice to include Core Material and Baking Costs in the overhead rate. This is done because it is an easy and cheap method. Cheap method? It is merely believed it is a cheap method. Actually the unrecognized expense of this method has driven foundries into financial difficulties.

Irrespective of whether you use an average overhead rate or different rates for different types, if you include Core Material, Mixing and Baking Costs in your overhead rate, see if you can find cores in your foundry showing these common differences:

1. Bench core, weight 2 lb, direct cost 5 cents.
2. Bench core, weight 10 lb, direct cost 5 cents.
3. Machine core, weight 30 lb, direct cost 5 cents.
4. Machine core, weight 20 lb, direct cost 5 cents.
5. Blower core, weight 5 lb, direct cost 2 cents.
6. Blower core, weight 1 lb, direct cost 2 cents.

Having found a test list of cores in your coreroom with variations comparable to the above, apply your overhead rate and list the absorption per item. Pause at this point and see if the differences offend your sense of reason, particularly if you are using an average rate.

Foundries determining the cost of Baked Core Material find that this cost closely approximates 1 cent per lb of the weight of the core at present. It is not necessary that you agree with or confirm this figure to use it for a simple test of the honesty and consistency of your present overhead rate or rates. Take the foregoing list of cores you assembled and deduct from the amount of overhead you absorbed on each, 1 cent per lb times the weight of the core. Divide the remainder of your overhead absorption by the Direct Core Cost. Note the wide variations in the overhead you are actually absorbing instead of the consistent allocation you thought you were making.

In the foundry of the writer one large production item has a ratio of Baked Material cost alone to Direct Labor Cost of 5 to 1. On this same item the Baked Material Cost consists of 3 cents per lb of the casting cost. Using an average overhead rate including the material, only one-third of this Baked Material Cost would be allocated to the casting. This will illustrate to what serious proportions this kind of cost discrepancy can develop.

The accounting problem of determining the cost per pound of Baked Core Material is no more difficult than determining the Cost of Metal at the Spout as it is determined in your Melting Department. The initial cost of determining and recording the core weight per casting is the greatest cost involved in putting this core procedure into operation. Again this cost is not prohibitive particularly in view of current tax rates.

The writer will challenge anyone making or who has made this costing improvement to state that this was not a highly profitable investment.

Cleaning, Grinding, Finishing, and Shipping Departments

In this area of foundry costs some of the most universal and serious discrepancies exist. These costs, both administrative and accounting-wise are the most difficult of accurate allocation. This opinion is confirmed by the fact that only a limited number of foundries have adopted the cost procedures necessary to cost castings with reasonable accuracy for these elements.

A preponderant number use a composite average rate per pound or a ratio to Direct Production Cost in the allocation of these costs.

Methods of intelligently determining and obtaining more accurate allocation of these costs cannot be presented in this article. Your foundry societies have the articles and manuals available describing the possible and worthwhile methods.

Adoption of such improved methods will disclose variation commonly in the area of 200 to 300 per cent of a composite rate per pound and running from minus 50 per cent to plus 500 per cent.

General Discussion

At this point those of you who are cost minded or have competent cost systems are probably saying, "This is all very true, but too elemental to benefit me." It is a fact, however, that these discrepancies exist throughout a large segment of the foundry industry. You have to contend with ill-advised competitive pricing resulting from these deficiencies. The false purchasing standards and practices which flow from such pricing adversely affect you and the entire industry. You will be part of a more wholesome and healthy industry to the extent you help to bring about more intelligent costing. It is the assigned intent of this article to create an awareness of these deficiencies where they exist and to encourage those with adequate cost systems to offer their counsel and assistance to those who need help in improving theirs.

A question in your mind is, "How is it that if so large a segment of the industry so deplorably misses reasonably true costs, they have stayed in business this long?" Some of the reasons may be as follows:

1. The costing is not erroneous in all of these cost areas.
2. These errors are not always cumulative. In some instances the errors are contra or offsetting.
3. In the usual competent foundry there are a number of established accounts whose average requirements closely approximate the average for the foundry operation.
4. For the large part of the last 15 years consumers have been more concerned with having a source of supply than with price consideration.

With the return of highly competitive conditions, it is certain that those without good road maps will

encounter the roughest roads.

This is no time to be smug. It is a statistical fact that the most long lived and successful foundries are cost and costing conscious. It is a further fact that the foundries with inadequate and untruthful cost systems are consistently high on one group of castings while low on another. The foundry business is largely small business and therefore largely dependent on retained earnings for the investment capital to improve plant efficiency and reduce costs. Those foundries of even proven foundry ability, with the advent of strictly competitive conditions, will steadily lose ground if their profitable production is prejudiced by inflated cost figures in favor of loss items apparently profitable. In turn this will be accelerated by the limitation of their source of investment capital, and retarded progress in efficiency improvement.

Up to this point emphasis has been placed on the most common and serious deficiencies of the various cost areas or departments and cost centers, in cost accounting terminology. Likewise, emphasis has been placed on the effect of these costing errors on profit, prices, competitive position, etc.

A comprehensive cost system, competently installed and administered will in addition furnish the following partially listed valuable management tools:

1. Cost Control Standards, data and reports.
2. Cost trend and forecast data.
3. Break Even charts.
4. Comparative statements of Actual Cost and Allocated Cost. This is both a test and fine tuning device of costing accuracy.
5. Comparative statement of Actual Cost and Cost at Normal operating rate.
6. Profit and Loss Analysis, showing:
   (a) Amount of profit or loss due to variation from normal operating rate.
   (b) Portion of profit derived from the difference in depreciation charges at acquisition cost and at sound replacement values.
   (c) Portion of Profit and Loss due to unusual or non-recurring income or expense.
   (d) Portion of Profit or Loss due to inventory gains or losses.
   (e) Portion of Profit due to overtime operation, less the premium cost of overtime operation.
   (f) Profit or Loss attributable to financial and non-operating income or expenses.
7. Cost, Sales, Profit Statements:
   (a) By customers.
   (b) By classes of work—Core, Non-Core, etc.

Would such information be of valuable assistance to you? You might be surprised at what you actually earned on your casting production, if from your apparent favorable profit for the last few years, you deleted windfall and fictitious profit factors. Your break-even point may thus be much higher than you realized it was.

Cost of Costing

It may be your opinion that the writer is trying to insure full future employment for accountants, or that he is willing to cost everything but the Cost of Costing. The writer has frequently harbored the same opinion. Cost accounting is not an exact science even if pursued, as it can be, to a prohibitively costly degree. The costing expense like any other business expense has to be economically justified and budgeted.

The writer can attest that a comprehensive and adequate costing function, embracing the standards outlined in this paper, can and is being done for less than 1 per cent of the Sales Dollar. In fact this is what was set years ago as an afforded amount for the costing function. This now covers the entire accounting function including general, tax and labor accounting.

In the writer's opinion, the following principles are essential to attaining an economical costing function:

1. The cost system must be well planned and installed. It must be co-ordinated with and not overlap or duplicate general accounting, timekeeping, order keeping, and production recording functions.

2. The best professional assistance is the least costly. It is like the good pattern which is paid for only once as compared to the poor pattern which is costly the rest of its life.

3. Your accountant should be a quality product at a quality price. He should be thoroughly indoctrinated in foundry practice. He should participate in Cost Groups, National Association of Cost Accountants, etc. Do not put him in the position of a garden variety bookkeeper or clerk.

4. Subscribe to your society sources of past and future costing articles and manuals. Job Costing methods and Standards Costing methods each have distinct advantages in foundry costing. The writer prefers job costing for a jobbing foundry operation. Do not overlook Standards Costing methods, however, because in this method will be found costing economy devices adaptable to minimizing the cost of a job costing operation.

5. Do not inject into your costing function, precise balancing, recording and computing requirements which are not essential. It is not necessary, for instance, to charge scrap losses back to every department in which the costs occurred. In practical operation, the writer has not found it necessary as text books teach, to tie the costing to the general books, for either inventory, statement or tax purposes.

It has been conceded that Cost accounting cannot be practically developed to an exact science. This is particularly true in the jobbing foundry field where an infinite variety of castings are produced.

It is, however, possible for you to enjoy the fruits of a properly improved and instituted cost system, eliminating the highlighted discrepancies most common and detrimental to your increased success. If, hereby, you are moved to or assisted in this profitable endeavor, the intent and effort of the writer will be rewarded.

ACID CUPOLA SLAGS AND SOME RELATIONSHIPS WITH MELTING CONDITIONS

By

Sam Carter* and Ralph Carlson*

ABSTRACT

Compositions are reported for 130 acid cupola slags from six different cupolas ranging in output from 1½ to 40 tons per hour, and melting several combinations of charge materials. Analytical determinations were made by wet chemical and spectrographic methods. Some effects of slag condition and fluxing practice on cupola operation and iron analysis are discussed, and some attempts made to quantitatively account for the sources of the slag constituents. Some relationships of slag composition with fluidity measurements are graphed. Some effects on slag composition are indicated for the following factors: Cupola size, type lining, lining practice, type of coke, amount of coke, coke size, charging method, volume of air, size of steel scrap, and hot blast. Several experimental heats with unusual combinations of special fluxes are described.

Over a period of several years a number of cupola slags have been analyzed in the authors' plant in an effort to better understand the relationships in cupola melting. These are presented for the benefit of any conclusions that might be drawn.

In basic cupola development, reports have included slag analyses, and much emphasis has been placed on slag composition. This is natural since the advantages expected from basic operation depend largely upon slag chemistry.

In the field of acid cupola melting, practically nc slag analyses have been reported in the American literature. A few acid slag analyses have been reported from this plant to point out the differences between acid and basic slags[1]. Since no refinement was expected from the usual slags with a predominance of acid constituents, acid slag composition has been of little interest.

The importance of adequate fluxing and slag elimination from the cupola has been recognized. The proper amount of flux is usually determined by trial and error and is generally considered as somewhere between the amount of basic flux that would render the acid constituents sufficiently fluid for elimination and that excess of basic flux that proved very corrosive to the cupola basin and slag-hole refractories. However, variations in acid slag composition have been found to exert some definite effects on cupola operation and resulting metal chemistry, effects often as great as factors given more serious consideration.

Carbon pickup and final carbon content of the metal have been found to be affected to some extent by slag composition. A viscous slag produces carbons as much as 0.15-0.20 pct lower than average.

On the other hand, acid slags with a relatively high proportion of basic constituents have been found to produce carbons as much as 0.15 pct higher than normal. Just as found in basic cupola practice, the more basic the slag the more efficiently the ash is cleaned from the coke and the more readily it is available for combustion and absorption.

Slag composition, through its effect on fluidity, can influence the general operation of a cupola. A slag of low fluidity is more inclined to clog up the interstitial spaces between small coke, and produce bridging and channeling with the usual consequences of reduced melting rate and temperature, and excessive oxidation of silicon.

Uneven stack permeability produces high velocity air channels which increase silicon loss by oxidation. If the flux deficiency is enough to cause serious trouble, the cause is more likely to be obvious. However, if the flux deficiency is only enough to give slightly impaired efficiency, the cause is less often recognized and the condition frequently attributed to a characteristic of the furnace or the charge materials.

Small Cupola Slag Control

On a very small cupola melting a low carbon iron, proper slag control has been found very important for good consistency. With other materials carefully weighed, but limestone added by estimated weight, calibrated scoops, etc., undesirable variations were frequently experienced. Since installing a small scale for accurate weighing of limestone, control has been much better. On such a small, low carbon charge, a variation of a few pounds of limestone has made a significant difference in carbon pickup and general operation. With the charge essentially fixed by raw material economics, variations in the flux have been used to regulate carbon level.

Another rather small cupola experienced a regular

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[1] Sam F. Carter, "The Basic Lined Cupola for Iron Melting," A.F.S. TRANSACTIONS, vol. 58 (1950).

tendency to give lower carbons and reduced melting rate in the afternoon. With some "doctoring" with special fluxes and extra coke the shift was always completed without serious trouble. The drop was not very clean and the cupola considerably bridged. The amount of flux being used was proportionately greater than on some of the larger furnaces, but not sufficient for this cupola. The insufficiently fluxed slag was gradually built up around the tuyeres and the coke clogged up to the point of trouble after a few hours. After raising the limestone the "afternoon drop" was eliminated and the bridging essentially prevented.

Serious troubles have been experienced occasionally in larger cupolas from accumulations of a viscous insufficiently fluxed slag. With the flux at a minimum for clean charges, increased amounts of accumulated dirt from damp scrap bins have been included in metal charges. With the limestone then inadequate the level of the slag of low fluidity built up in the basin to tuyere level and the viscous descending slag accumulated as a bridge over the tuyeres.

Under such conditions melting rate and temperature were greatly reduced, carbon level dropped and silicon oxidation increased. Considerable "doctoring" has been necessary to bring such cupolas back to satisfactory melting performance and iron quality within 2 or 3 hr. These experiences have suggested that more emphasis should be placed on the flux. Cupola literature does not seem to give sufficient definite direction on slag control.

In addition to its effect on cupola operation and metal reactions, slag composition can contribute an insight into conditions within a cupola. From the slag analysis and the known proportion of flux it is possible to determine abnormal sources of slag contribution. The iron oxide content of the slag has been found to indicate to some extent the relative degree of oxidation within the cupola. This slag FeO content can be related to silicon loss within the same cupola.

The importance of slag study should not be overemphasized. The primary objective is to melt *iron* to a given composition, temperature, and combination of physical properties as efficiently as possible. Obtaining a precise slag analysis, color, or fluidity is not the primary objective. Slag analysis like gas analyses are of value only in so far as they give direction toward this objective or explanation to the reactions.

Slags are difficult to analyze. Methods and results vary among analysts. Allowance must be made for some sampling and analytical errors. Sampling procedure is also important as well as sample preparation. Samples in this report were collected in a shallow metal spoon inserted under the slag stream. From a smoothly-operating front-slagging cupola the slag stream seems to be fairly representative.

Most of the earlier slags in this report were analyzed by wet chemical methods[1] while more of the later slags have been analyzed by spectrographic methods[2].

Wet chemical methods are more reliable on slags of unkown or unusual types, and are the basis for spectrographic calibration. However, wet methods of slag analyses are long and expensive with many chances for human error.

[1] Chemical methods of slag analysis were essentially those outlined by Lundell, Hoffman, and Bright in their book *"Chemical Analysis of Iron and Steel"* with some minor modifications. Total sulphur was determined gravimetrically on a fresh sample, precipitated as $BaSO_4$. Titanium was determined on a fresh sample with a photo-colorimeter.

[2] Spectrographic determinations were made by packing the pulverized sample into the "centerpost crater" type of electrode and sparking with an excitation on the regular range of 2700-3400 Angstroms. The mutual standard method of calculation was used in which the content of the various oxides were determined in relation to SiO_2 content. Lines of the following wave lengths were used—Ca 3006.8, 3159 and 3181; Fe—2767, 2762, 2756; Mg—3336, 2790, 2782; Al—3082; Mn—2886; and Ti—3088.

TABLE 1—SOME TYPICAL ACID SLAGS

Nature of Slag	Size Cupolas, in., ID	Charge Proportions, % Steel	C.I. Scrap	Pig	Coke, %	Flux %	Slag Composition, % SiO_2	Al_2O_3	FeO	MnO	CaO	MgO	Analytical Method
	(1)							(2)	(3)				(4)
Average fluidity	84 Eq.	13	30	57	9.2	1.9 L'st	45.3	10.3	1.0	3.4	36.3	1.6	(S)
Average fluidity	84 Eq. HB	33	8	59	7.5	1.9 L'st	47.1	12.1	6.9	4.6	22.0	1.6	(W)
Average fluidity	84 Eq.	13	31	56	9.2	1.9 L'st	47.9	12.0	3.6	3.5	29.7	1.9	(W)
Slightly sluggish slag	84 Eq.	14	25	61	7.9	1.4 L'st	50.7	12.4	5.6	4.7	24.4	1.1	(S)
Average fluidity	48 HB	22	12	66	12.8	3.0 L'st	46.2	11.0	1.1	1.4	37.2	1.0	(W)
Viscous slag	48 HB	22	12	66	12.8	2.2 L'st	52.1	12.4	1.5	1.9	29.2	0.9	(W)
Very viscous slag, dirty reclaimed scrap	21 Exp.	0	100	0	12	1 Soda Ash 5 Dol.	53.4	12.7	12.7	1.9	9.6	6.3	(W) $\frac{Na_2O}{3.7}$
Very fluid and corrosive	84 Eq.	14	25	61	11	3.5 L'st	39.6	10.1	3.8	2.9	38.8	2.9	(W)
Very fluid and corrosive	84 Eq.	15	25	60	11.5	4.5 L'st	38.3	12.1	3.1	2.8	36.7	3.4	(W)
Overblown Cupola (490 cfm/sq ft)	21 Exp.	50		50	14.0	5.0 L'st	45.6	18.3	16.1	2.8	15.4	1.0	(S)
50% Cans	21 Exp.	50		50	14	8.0 L'st	40.4	5.1	44.4	2.1	7.2	0.5	(S)
From tuyere of bridged cupola	72	35	8	57	12	2.5 L'st	36.8	4.7	40.7	2.6	3.7	1.8	(S)

1) HB indicates hot blast at 450 F.
Eq. indicates a link shaped 72 x 99-in. cupola *equivalent* in area to an 84-in. cupola.
Exp. indicates experimental cupola.
2) TiO_2 presumably present in all slags from 0.7 to 1.5% but determined in only a few.
3) FeO represents total Fe as FeO.
4) (S) indicates spectrographic analysis, (W) conventional wet analysis.

TABLE 2—COKE PROPERTIES AND ASH ANALYSES

Foundry Regular Cokes	Ash, %	Volatile, %	Sulfur, %	Softening Ash Temp.,F	Ash Analysis, % SiO_2	TiO_2	Al_2O_3	Fe_2O_3	MnO	CaO	MgO
A-1	9.18	0.70	0.70		53.3		32.3	9.3	0.02	1.6	2.6
A-2	9.51	0.90	0.73	2631	54.8	1.5	29.7	7.0	0.15	1.2	2.2
A-3	9.38	0.86	0.71	2587	51.4		31.7	9.3	0.02	3.8	2.0
E-1	9.92	0.88	0.83	2645	51.2	1.5	33.5	6.9	0.05	2.8	1.9
E-2	7.69	0.98	0.74	2658	50.9		34.4	8.1	0.02	0.5	2.1
S	8.47	1.03	0.75	2782	48.3		36.0	9.9	0.04		1.8
M*	12.94	1.00	0.82	2497	51.8		32.5	7.6	0.03	1.4	3.0

* Malleable (low carbon) coke.

Spectrographic determinations are much simpler and relatively inexpensive; several slags can be analyzed and calculated within an hour after preparation. The spectrograph determines relative concentrations with good precision, but the true accuracy of the exact percentage depends largely on the accuracy and range of chemically determined standards used for calibration.

The mutual standard method of calculation has some limitations. Since the calculation is based on a total of 100 pct, accuracy can be impaired if some unrecognized constituent is present in any significant concentration. If much of a given element is present as some other compound or some other oxide of the element than that represented in the standards, then crystalline history may be affected and some inaccuracy caused. It is expected that improved methods and better standards will make spectrographic slag more accurate and more useful, and there facilitate the study of slag metal relations.

Some Typical Acid Slags

In Table 1 the first five slags are typical of compositions of normal cupola slags. The silica contents range from 42-50 pct, alumina contents range from 10-18 pct, total iron oxide as FeO generally 1-8 pct, manganese oxide 1-5 pct, and CaO+MgO 25-40 pct. Molecular proportions may be compared to weight proportions on a typical composition like (Slag No. 1, Table 1):

	SiO_2	TiO_2	Al_2O_3	FeO	MnO	CaO	MgO
Wt., %	45.3	1.0	10.3	1.9	3.4	36.3	1.6
Molar, %	46.0	0.8	6.7	1.6	2.9	39.6	2.4

Slag No. 4 was slightly sluggish from lower than average fluidity. SiO_2 content was 50.7 pct. Slags Nos. 6 and 7 in Table 1 lacked sufficient fluidity for smooth front slagging and clean cupola operation, one because of insufficient basic flux, and the other because of excessive sources of slag from a very dirty charge of 100 pct scrap reclaimed from a sand dump. Slags with insufficent fluidity have usually contained over 50 pct SiO_2.

The next two slags (Nos. 8 and 9) were more fluid than usual—a result of SiO_2 contents under 40 pct and relatively high lime to SiO_2 ratio. The 3½ and 4½ pct quantities of limestone were much higher than necessary for the charge melted. The ratios of $CaO+MgO/SiO_2$ were actually 1.04 and 1.05, which are in a composition range that might be considered slightly basic. Furnace performance was very good. Carbons averaged 0.10 to 0.15 pct higher than usual.

The acid refractories in the basin of the cupola and the slag trough were very deeply corroded by the relatively high basicity; and the tap hole was enlarged enough to cause the breast to blow before the end of two shifts. In all probability these slags had been more basic in the cupola before consuming some acid refractories from the basin, breast, and trough.

The last three slags (Table 1) had unusually high FeO contents, indicative of excessive oxidation, high silicon losses and high iron losses. In No. 10 a 16.1 pct FeO was obtained because of excessive air volume on an experimental cupola in studying results of overblowing. Slag No. 11 came from a charge with 50 pct cans. Excessive oxidation from the very thin steel produced a slag with 44.4 pct FeO. The very low CaO content of 7.1 pct in spite of an 8 pct limestone addition indicates the very large volume of slag produced from very severe lining attack and a very high silicon loss of 35.4 pct.

The last slag shows a similar very high FeO content and very low CaO content. This slag sample was removed several years ago from a tuyere of a badly bridged cupola.

Analysis of Sources of Slag

One major source of cupola slag is the ash of the coke. Table 2 contains coke properties, ash content, and ash analyses for seven typical cokes selected from a number we have analyzed.

Silica is the predominant constituent in all the coke ashes ranging roughly from 50 to 55 pct by weight. Alumina contents generally range from 30 to 35 pct and iron oxide contents 6 to 10 pct. Calcium, magnesium, manganese, and titanium oxides are present in small percentages in the coke ashes from the Birmingham district. With the SiO_2 and Al_2O_3 contents totaling about 85 pct, the coke ash is obviously almost entirely acid in its contribution to the slag.

Another source of slag nearly as great and frequently greater is the refractory lining that is consumed or fluxed away during the day's melt. Lining consumption is frequently referred to as one of the incidental minor sources of slag and generally has not been given proper consideration in flux calculations. With the exception of our largest cupola (72 ×99-in. I.D. link shaped equivalent to 84-in. I.D.) operated for 16 to 18 hr, lining consumption makes the largest single contribution to the slag in all the several cupolas we operate.

As the cupola size increases and/or heat time becomes longer, lining consumption decreases and

TABLE 3—THEORETICAL CALCULATIONS OF SLAG SOURCES AND FINAL COMPOSITION, 48-IN. CUPOLA
(Charge, 25% steel; 20% return; 55% pig)

	Total Slag Material	Slag Constituents/100 lb Metal, lb					
	lb/100	SiO_2	Al_2O_3	FeO	MnO	CaO	MgO
Lining consumption, 1½% of melt (L-1)*	1.50	0.95	0.36	0.03			0.03
Coke ash, 12½% coke. 9.2% ash (A-1)*	1.15	0.60	0.37	0.10		0.018	0.03
Si loss—10% of 2.20 Si	.47	0.47					
Dirt and sand on scrap	.10	0.10					
Oxidation of iron	?			?			
Mn loss—10% of 0.70 Mn	.09				0.09		
3% limestone**	1.50					1.50	
	4.81	2.12	0.73	0.13	0.09	1.52	0.06
Calculated Slag Analysis, %		44.2	15	2.7	1.9	31.7	1.2
Actual Analysis of Slag, %		43.2	16.1	3.2	1.8	30.8	1.4

* Numbers in parenthesis indicate the refractory in Table 4 and the coke in Table 2.

** In calculating CaO from limestone some allowance was made for impurities, fines, and moisture usually present as used.

TABLE 4—ANALYSIS OF SOME TYPICAL CLAY REFRACTORIES

Refractory	Refractory Composition, %							Ignition Loss
	SiO_2	TiO_2	Al_2O_3	Fe_2O_3	CaO	MgO	Na_2O	
Clay-Brick								
L-1	62.1		32.6	1.7		1.2	1.4	.11
L-2	59.3		34.8	1.4		2.1	1.6	.03
K-1	60.5	2.2	32.1	2.9	0.0	1.3	2.3	.13
K-2	58.6	2.0	31.9	3.2	0.0	1.3	2.4	.28
Canister-Clay Gun Mixes								
H	62.8	1.0	24.0	2.3	0.0	1.9	0.6	7.8
C	73.1	0.8	15.8	2.3	0.0	1.8	1.5	5.2

makes less contribution to the slag relative to other sources of slag material. In all our cupolas operated less than two shifts and under 48-in. I.D. the refractory consumption is generally equal to all the other sources of slag. This effect of lining consumption on slag composition and the effect of slag composition on metal chemistry largely account for the effectiveness of basic cupola operation. Installing a basic lining and simply changing this consumed refractory from acid material to basic material has in most cases been sufficient to change the slag from one of acid analysis to a slag with the basic constituents predominant.

A third inevitable source of some slag material is the oxidation products of Si, Mn, and iron. From Si lost in melting the acid silica formed can be calculated fairly accurately. In like manner the basic MnO comes from the Mn oxidized from the metal. Iron oxide is more difficult to calculate. Some iron oxides enter the cupola in the coke ash, a little in the refractories, and a considerable amount as rust on the metal. Some of this iron oxide may be ultimately reduced by the stronger deoxidizers, Si and Mn.

In the reducing zones of the cupola it seems likely that some of the iron oxide is reduced to iron by the coke. In the oxidizing zones of the cupola some iron oxide is formed. The final FeO content of the slag depends largely on the net effect of these competing reactions. Under more reducing conditions the FeO content[1] of the slag might be reduced below that content actually obtained from the coke ash. However, the normally efficient cupola is slightly more oxidizing, and the FeO content of the slag is 1 to 4 pct beyond that which can be accounted for from the coke ash and refractories. Extremely oxidizing conditions from any of several bad cupola conditions have produced iron oxide contents much higher and in some cases as high as 20 to 40 pct.

Dirt on Scrap a Source of Slag

One unintentional source of slag is dirt and sand on scrap. Under favorable conditions of dry weather, cleaned foundry return, and high proportions of pig iron, this source of slag is negligible. However, with the use of much purchased scrap, the use of uncleaned foundry return with adhering sand, this source of slag can be significant.

Such extraneous sources of dirt may be quite variable and difficult to compensate for. In damp weather more of the dirt adheres to the scrap and is carried into the cupola than in dry weather. As storage bins become low, more of the accumulated dirt containing sufficient iron oxide is attracted to make-up magnets. Abrupt changes in extraneous dirt from scrap have caused more slag trouble in our plant than any one factor.

The intentional source of slag is the neutralizing flux (usually limestone) deliberately added to provide sufficient basicity to counteract the acids from the inadvertent sources and provide sufficient slag fluidity for clean cupola performance and convenient slag elimination.

Some theoretical calculations of slag composition for a rather typical 48-in. cupola are shown in Table 3. From the analysis and amount of the cupola patch material, the amount and analysis of coke ash, and the other sources of slag, the proportions of the vari-

[1] FeO contents represent total iron in all iron oxide forms converted to FeO.

TABLE 5—EFFECT OF CUPOLA AND LINING CONSUMPTION

In. Diam. Cupolas	Melt Rate T/Hour	Consumption of Lining, % of Melt (1)	Flux, %	Coke, %	Steel, %	Slag Composition, %						
						SiO_2	Al_2O_3	FeO	MnO	CaO	MgO	S
84 Eq	40	½	1.9	9.0	13	47.9	12.0	3.6	3.5	29.6	1.9	0.21
48	12	1½	3.5	12½	20	43.2	16.1	3.2	1.8	30.8	1.4	0.09
36	5	3	5.5	18	25	42.7	19.0	3.6	1.6	26.8	2.2	0.20
24*	1½	6	7.0	20.0	60	45.0	17.5	2.9	1.6	30.4	1.6	
			8.0	24	67	42.4	13.5	2.8	3.5	34.7	1.6	0.28
21 Exp	1		5.0	14	50	46.5	21.1	4.2	1.0	24.2	2.6	0.11

* High silicon, low carbon iron.

(1) Lining consumption is weight of material required to patch to original dimensions with estimated allowance for chipping out. Expressed as per cent of day's melt.

ous constituents were determined. The theoretically calculated slag composition compared rather closely with the actual slag analysis, almost within the limits of sampling and analytical methods. The total quantity of slag for this furnace was calculated to be 4.8 pct of the metal melted.

Effect of Cupola Size and Lining Consumption

The effects of cupola size on refractory consumption and necessary flux, as experienced in our plant, are shown in Table 5. On heat No. 1 the lining consumption from the cupola equivalent to 84-in. I.D. and operated for 18 hr was hardly 0.5 pct of the melt. With only 9 pct coke and a reasonably clean charge only 1.9 pct flux was necessary for a reasonably fluid slag.

On heat No. 2 on the 48-in. cupola operated for 11 hr the refractory consumption was 1.5 pct of the melt or three times that of the larger cupola. This difference in lining consumption would require 1 pct more limestone to flux; the higher proportion of coke and slightly higher silicon consumption obviously require some further limestone increase. A 3½ pct limestone addition to this furnace produced a slag very similar to that resulting from 1.9 pct limestone in the larger cupola.

On the still smaller 36-in. cupola the lining consumption increased to 3 pct and limestone flux found necessary was 5.5 pct of the metal charge. The resulting slag is very similar to the slags above it in Table 5 with much less flux.

On the 24-in. cupola the high steel charge and high silicon content produce some increase in slag volume irrespective of size. The lining consumption continued to increase to 6 pct; and the necessary limestone flux to obtain equal slag chemistry and fluidity increased to 7 to 8 pct.

These differences indicate the importance of considering cupola size in comparing experiences on various cupolas and the necessity of considering refractory consumption in establishing the correct amount of flux.

Theoretical calculations of slag composition of the two extremes, the 84-in. and the 24-in. cupolas, are shown in Tables 6 and 7. These calculations show readily why the large furnace has been operated with as little as 1.4 pct limestone, while the 24-in. cupola has required as much as 8 pct limestone to yield a similar slag. Compared to the large furnace, the small cupola with its characteristic mix produces 12 times as much slag from the lining, 3 times as much from the coke ash, and 4 times as much from silicon oxidation. The slag to metal ratio was 13.8 pct compared to 2½ pct on the large cupola.

Within the same size cupola, lining contribution to the slag can be affected by refractory quality and care of application. Erratic lining technique obviously can affect the slag condition.

Effect of Lining Practice and Types of Coke—24-In. Cupola

Variations in lining practice and types of coke on the 24-in. cupola previously described have provided some experience in adjusting flux to maintain uniform slag condition and smooth cupola performance. This mix is our most difficult to control and very dependent on proper fluxing. The high silicon charge is unusually sensitive to oxidation losses, and the low carbon charge is more dependent than usual on carbon pickup. The small diameter cupola can be quickly clogged up if insufficiently fluxed. Some of these variations are illustrated in Table 8.

In the first mix the coke used was a high-ash coke made intentionally for low carbon pickup. This coke was purchased for a larger installation to meet a temporary raw material emergency and was not as suitable on this application where it is economically desirable to use the maximum amount of steel. Some trouble was encountered before adjusting the practice to this coke. By increasing the flux to 9 to 10 pct limestone with 1 pct fluorspar the carbon was raised to the desired level.

On the next mix regular foundry coke was used. The 9-in. lining was maintained with clay brick. Lining attack was very severe from this high steel mix.

TABLE 6—THEORETICAL CALCULATIONS OF SLAG COMPOSITION—84-IN. CUPOLA
(18-hr heats—13% steel, 31% scrap, 7.8% coke, 1.4% limestone)

	Total Wt. of slag, lb/100	Lb of Slag Constituents/100 lb Metal						
		SiO_2	TiO_2	Al_2O_3	FeO	MnO	CaO	MgO
Lining consumption, ½% (C)*	0.50	0.36	0.04	0.079	0.010			0.009
Coke ash, 7.8% coke 9.2% ash (A-1)*	0.72	0.38		0.23	0.060		0.012	0.019
Si loss, 10% of 1.50% Si	0.32	0.32						
Dirt on charge, approx.	0.10	0.10			?			
Iron oxidation	?				?			
Mn loss, 15% of 0.60% Mn	0.12					0.12		
1.4% Limestone	0.70						0.70	
	2.46	1.16	0.04	0.31	0.07	0.12	0.71	0.028
Calculated Composition, %		47.4	1.6	12.6	2.8+	4.9	28.8	1.1
Actual Analysis, %		51.0	1.4	13.3	5.5	4.8	23.0	1.1

* Letters in parenthesis indicate refractory in Table 4 and coke in Table 2.

TABLE 7—THEORETICAL CALCULATIONS OF SLAG COMPOSITION—24-IN. CUPOLA
(10-hr heat—60% steel; 24% coke; 8% limestone)

	Total Wt. of Slag, lb/100	Lb of Slag Constituents/100 lb Metal					
		SiO_2	Al_2O_3	FeO	MnO	CaO	MgO
Lining consumption, 6% (L-1)*	6.00	3.72	1.96	0.09			0.07
Coke ash, 24% coke, 9.2% ash (A-1)*	2.20	1.13	0.71	0.18		0.035	0.057
Si loss, 16% of 3.60%	1.29	1.29					
Dirt on scrap, approx.	0.10	0.10		?			
Iron oxidation	?			?			
Mn loss, 15% of 0.90%	0.18				0.18		
8% Limestone	4.00					4.00	
	13.77	6.24	2.67	0.27	0.18	4.03	0.13
Calculated composition, %	45	19.4	1.9+	1.3	29	1	
Actual Analysis, %		47.7	14.1	3.0	1.5	28.0	1.4

* Letters in parenthesis indicate refractory in Table 4 and coke in Table 2.

TABLE 8—EFFECT OF TYPES OF COKE AND LINING PRACTICE—23-IN.-32-IN. PRODUCTION CUPOLA
Charge, 60-70% Steel; 5-15% Return; Balance Silicon Pig. Length of Heat—8 to 12 hr.

Type Lining	Type Coke	Lining Consumption, % of Melt	% Ash in Coke	% Coke	% Flux	Slag Composition, % SiO_2	TiO_2	Al_2O_3	FeO	MnO	CaO	MgO	S
Convent. Clay Brick	High Ash	6.5	13	27	9 Lst—1% Sp	48.4		16.1	2.6	2.8	26.2	1.3	0.227
					Lst—1% Sp	45.0		15.6	3.6	3.3	28.5	1.5	0.252
Convent. Clay Brick	Regular	6.0	9½	24	8 Lst	42.4		13.5	2.8	3.5	34.7	1.6	0.288
		6.0	9½	24	7 Lst	46.3	0.3	12.2	3.3	3.9	31.0	1.6	0.185
Water Cooled (1)	Regular	3.0	9½	20	5.2 Lst	44.3	0.6	15.5	2.6	1.8	35.8	1.4	(S)
		3.0	9½	20	5.2 Lst	46.2	0.7	15.6	2.4	3.5	30.2	1.6	(S)
Graphite W.C. (1)	Regular	<½	9½	20	1.7 Lst	48.8	0.6	10.6	8.9	2.4	25.4	3.4	(S)

(1) With water cooling and graphite lining, lining thickness reduced 4½ in. and ID increased 9 in. to 32 in.

Lining consumption was estimated to be 6 pct of the metal melted. The flux on this regular mix was 7-8 pct limestone. The resulting slags are typical acid compositions.

Three years ago this cupola was water cooled by external sprays. The resulting mix is shown next in Table 8. Water cooling reduced the lining consumption to half or 3 pct of the metal melt. This made it possible to cut the limestone to 5.2 pct and still obtain a comparable slag composition, and also made possible a slight reduction in coke.

The last mix shows the results from an experimental graphite lining tried about 2 years ago. This lining made practically no contribution to the slag. To prevent the slag from being decidedly basic and carbon too high the limestone was reduced to 1.7 pct.

The resulting slag analysis was 48.8 pct SiO_2, 10.6 pct Al_2O_3, 8.9 pct FeO, 25.4 pct CaO, and 3.4 pct MgO. The FeO content is higher than normal. This might be attributed to a normal amount of iron oxide but a total slag volume reduced to less than one third.

Some Effects of Charging Method and Coke Ratio—84-In. Cupolas

Table 9 shows some slag compositions obtained over a period of 6 years on a large cupola. The first five slags were from old style side-dump charging. On the first mix shown, dolomite was used as flux, producing a slag containing 18.2 pct CaO and 12.3 pct MgO, with the total 30.5 pct comparable to heats using limestone. With slags of this type little or no significant difference has been apparent between the use of dolomite and limestone. On this first heat the 60 pct scrap required a higher than usual flux of 3 pct. The higher scrap and coke of poorer quality required the highest percentage of coke, 12½ pct.

The next four heats with 2½ pct dolomite or limestone and 11 to 11½ pct coke produced slags ranging from 41.9 to 45.9 pct SiO_2, 4.8 to 8.3 pct FeO, and 23 to 32.3 pct CaO+MgO.

The next group of heats (Nos. 6-10) were run after installing cone bottom bucket charging. The most apparent difference is the lower FeO content of the slags in spite of some reduction in coke. Slags Nos. 6 and 7 ran only 2.4 and 2.8 pct FeO with 10 pct coke compared to slag FeO contents ranging from 4.8 to 8.3 pct with 11 to 12½ pct coke. This reduction in oxidation is obviously made possible by more uniform charging, and more favorable distribution of stack permeability.

On heats Nos. 8 and 9, 9 pct coke and 1.9 pct limestone ran very good, slag FeO was slightly higher than with 10 pct coke. On heat No. 10 the 7.8 pct coke proved adequate to produce a 2600 F iron, and 1.4 pct limestone was found sufficient when the scrap

TABLE 9—EFFECT OF CHARGING AND COKE RATIO—84-IN. PRODUCTION CUPOLAS

	Flux, %	Coke, %	Steel, %	Pig, %	C.I. Scrap, %	Slag Composition, % SiO_2	Al_2O_3	FeO	MnO	CaO	MgO	S
Side Dump Charging 250 F Hot Blast												
1.	3.0 Dol	12½	10	30	60	43.9	16.7	4.8	4.0	18.2	12.3	
2.	2.5 Dol	11	15	60	25	45.9	13.5	8.3	5.3	14.3	8.6	
3.	2.5 Lst	11½	15	60	25	43.5	16.2	4.8	1.8	29.2	1.3	0.230
4.	2.5 Lst	11½	15	60	25	44.8	12.4	5.5	2.3	27.0	1.3	0.218
5.	2.5 Lst	11½	15	60	25	41.9	11.6	4.9	1.7	31.0	1.3	0.337
Cone Bottom Bucket Charging Cold Blast												
6.	3.0	10	13	56	31	44.9	13.5	2.4	2.1	33.0	2.0	0.40
7.	2.1	10	13	56	31	49.0	13.0	2.8	2.3	30.1	2.1	0.25
8.	1.9	9	13	56	31	47.9	12.0	3.6	3.5	29.6	1.9	0.21
9.	1.9	9	13	56	31	48.4	11.7	5.5	3.9	28.1	1.8	0.21
10.	1.4	7.8	14	61	25	51.0	13.3	5.5	4.8	23.0	1.1	
Cone Bottom Bucket Charging 450 F Hot Blast												
11.	3.0	9.1	27	65	8	45.1	14.4	2.1	1.7	32.4	2.2	0.25
12.	2.1	9.1	27	65	8	47.8	16.4	3.1	2.4	28.7	2.0	0.25
13.	1.0	8.3	33	59	8	52.2	13.4	3.2	4.1	25.6	1.7	0.08
14.	1.5	7.5	33	59	8	47.1	12.1	7.0	4.6	22.0	1.6	0.14
15.	1.5	7.5	33	59	8	50.9	11.1	4.8	4.2	26.7	1.8	0.22

(All analyses by wet chemical methods)

TABLE 10—EFFECT OF HOT BLAST

	Age of Heat, hr	Coke, %	Flux, %	Slag Composition, % SiO_2	TiO_2	Al_2O_3	FeO	MnO	CaO	MgO	S
48-in. Cupola—20% Steel					(1)						
Cold Blast	1	14	4 Lst	42.8		16.4	3.3	1.8	31.4	1.8	0.15
	5			42.2		17.3	3.1	1.8	32.0	1.8	0.10
	9			43.7		17.1	2.8	1.3	31.6	1.9	0.10
Cold Blast	1	14	4 Lst	42.2		17.8	2.4	1.3	32.5	1.9	0.14
	5			45.1		15.2	4.2	2.4	30.5	1.2	0.09
	9			43.2		16.1	3.2	1.8	30.8	1.4	0.09
Hot Blast 450 F	1	12½	4 Lst	41.5		16.8	1.9	2.2	32.9	1.4	0.16
	5			39.2		17.1	1.1	1.8	33.0	1.8	0.14
	9			41.4		17.9	1.1	1.1	38.1	1.4	0.17
Hot Blast 450 F	1	12½	4 Lst	41.4		17.9	1.1	1.1	34.1	1.2	0.18
	5			43.2		15.0	1.0	1.6	34.0	1.6	0.19
	9			39.5		15.8	0.9	1.6	40.2	1.6	0.26
84-in. Eq. Cupola—13% Steel; 31% Scrap; 56% Pig											
Cold Blast	3	9.2	1.9 Lst	48.7	1.2	9.7	6.1	3.6	29.7	1.1	
	7	9.2	1.9 Lst	50.6	1.2	10.1	4.6	3.7	28.8	1.0	
Hot Blast 450 F	9	9.2	1.9 Lst	48.0	1.1	10.6	1.2	2.8	35.5	0.9	
	12	9.2	1.9 Lst	49.3	1.2	14.3	1.8	3.3	29.0	1.0	
	15½	9.2	1.9 Lst	47.0	1.3	15.1	3.8	4.9	26.8	0.9	
Cold Blast	3	9.2	1.9 Lst	52.1	1.3	11.1	4.5	4.5	25.8	0.9	
	7	9.2	1.9 Lst	49.3	1.3	13.3	1.4	2.9	30.8	1.0	
Hot Blast 450 F	9	7.5	1.9 Lst	46.9	1.2	12.0	3.5	3.9	31.5	1.0	
	12	7.5	1.9 Lst	46.1	1.2	10.5	3.6	3.7	34.1	0.9	
	15½	7.5	1.9 Lst	51.5	1.2	11.6	3.9	4.1	26.8	1.0	

(1) TiO_2 presumably present in all slags from 0.7 to 1.5. Determined only where indicated.

was reasonably free of dirt. However, the resulting slag with 51 pct SiO_2 showed a fluidity slightly below average that could cause trouble if some unexpected source of acid contamination should occur. Further reductions in coke produced excessive oxidation which was more costly than the coke saved.

The last five heats were charged from the same cone bottom buckets, but contained higher proportions of steel and were blown with preheated air (450 F). Heats 11, 12, and 13 with 8.3 to 9.1 pct coke produced slag FeO contents of 2.1 to 3.2 pct, which is rather low for such a nigh steel charge and low proportion of coke.

On heat No. 11 with 3 pct limestone the slag was very fluid. SiO_2 content was 45.1 pct and $\frac{CaO+MgO}{SiO_2}$ was 0.76. Corrosion of the tap hole and slag trough was above average.

On heat No. 12 with limestone reduced to 2.1 pct the slag had moderate fluidity. SiO_2 was raised to 47.8 pct and $\frac{CaO+MgO}{SiO_2}$ reduced to 0.64. Corrosion of the tap hole and trough was reduced.

On heat No. 13 with the limestone reduced further to 1.9 pct, fluidity was reduced but was still adequate. SiO_2 increased still more to 52.2 pct and $\frac{CaO+MgO}{SiO_2}$ was decreased further to 0.52.

Heats Nos. 14 and 15, with coke reduced to 7.5 pct (13½:1 metal to coke ratio), produced slags much higher in FeO, 4.8 pct and 7.0 pct, indicative of greater oxidation. Cupola performance was good at this and lower coke proportions, but the increased oxidation became economically inadvisable and metallurgical control more difficult. The FeO content of the slag has proved a fairly useful indication of over-all oxidation.

Table 10 shows some slags from two different cupolas comparing results from blast preheated to 450 F with slags from air at atmospheric temperature.

On the 48-in. cupola with cold blast, slag FeO contents ranged from 2.4 to 4.2 pct. With the blast heated to 450 F, slag FeO contents were reduced to 0.9 to 1.9 pct in spite of some reduction in coke. Slags from the hot blast contained slightly more sulfur, accounting for a small decrease in the sulfur content of the iron.

On the 84-in. cupola the slags were not as consistent and the change was not as distinctive as the previous example. The effect of preheated blast has not been found to be as great on larger diameter cupolas with a lower ratio of circumference to cross-sectional area. However, a fairly definite decrease in slag FeO was experienced after turning on the air heater to 450 F and holding the coke constant.

In the last group the coke proportion was reduced from 9.2 to 7.5 pct with the change to preheated air. Little change in slag FeO was experienced. The hot blast apparently counteracted the normal tendency for slag FeO to increase with decreases in coke.

From these data it appears that preheated blast lowers slag FeO, indicative of decreased oxidation, or permits coke reductions for the same slag FeO and degree of oxidation.

Experimental Heats With Various Fluxes

A number of heats have been run in a 21-in. experimental cupola with various flux combinations to study the effect of slag composition on cupola operation, the effect of fluxing on carbon pickup, and the possibility of some desulfurizing within acid lined cupolas. Heat results are shown in Table 11.

A 70 pct steel mix was chosen because of its sensitivity to carbon and sulfur pickup. All these slags were inclined to run 6.9 to 12.4 pct FeO. The high

TABLE 11—EFFECT OF VARIOUS FLUXES AND SPECIAL ADDITIONS
21-in. Experimental Cupola—70% Steel; 30% Pig Iron; 14% Coke; Lining, Clay Brick

Flux, %	Special Additions, %	S in Metal, %	Slag Fluid., in	Slag Composition, %									
				SiO_2	TiO_2	Al_2O_3	Fe_2O_3	FeO	MnO	CaO	MgO	S	
3 Limestone	4 Graphite	0.127	¼	49.4	1.0	23.4	0.9	7.7	2.4	10.5	1.5	0.066	
3 Limestone	4 SiC	0.144	½	46.7	1.2	21.4	0.9	8.0	4.1	12.7	1.1	0.050	
3 Limestone	4 Sp. briq.	0.128	¼	50.5	1.0	22.2	1.0	6.9	3.0	11.9	1.4	0.108	
6 Limestone	0	0.109	2	42.5	1.0	18.2	1.1	7.5	2.2	20.9	1.0	0.063	
8 Limestone	0	0.104	3	41.8	*	17.5	**	8.3	1.9	25.6	1.3	0.108	
3 Lst, 2 Soda Ash	0	0.126	¾	50.4	*	19.0	**	12.4	2.0	10.0	1.6	0.055	Na_2O 3.2
3 Lst, 2 Soda Ash	0	0.108	1¼	46.3	*	18.6	**	6.3	2.6	20.3	1.0	0.071	F 0.07
3 Fluorspar	0	0.128	¼	51.3	*	20.9	**	9.5	2.0	9.8	0.8	0.055	0.11
5 Mn Ore	0	0.092	1½	46.8	*	17.4	**	8.8	23.4 16.3	3.9 5.3	0.9	0.097	
3 Limestone	4 CaC_2	0.104	3	38.9	0.8	20.7	0.7	1.9	1.9	30.6	1.0	0.294	
5 Lst, 2 Spar	5 CaC_2	0.085	4	37.5	0.7	21.4	0.5	1.5	1.1	31.8	1.0	0.331	
5 Lst, 2 Spar	5 CaC_2	0.071	5	37.0	0.4	20.0	**	1.3	1.7	38.6	1.1	0.266	

(1) Slag fluidity measured in inches run in Herty type metal viscosity mold with ⅜-in. channel.
* TiO_2 presumably present in all slags. Determined only in those indicated.
** In all slags a small fraction of Fe is present as Fe_2O_3. When not shown iron in Fe_2O_3 is included as FeO.

steel mix is inclined to run very high in slag FeO, and such a small cupola is inclined to run a higher FeO on an equivalent mix.

On heats Nos. 1, 2, and 3 three pct limestone produced slags of inadequate fluidity; metal sulfur content ranged from 0.123 to 0.144 pct. The viscous slags were very sluggish and stringy down the front slag trough and measured only ¼- to ½-in. in the slag viscosimeter (metal viscosity mold of the Herty type used on steel slags, channel diameter, ⅜ in.) shown in Fig. 1. These heats showed some tendency toward bridging and slag adherence inside the cupola. Slag SiO_2 contents were high, 46.7 to 50.5 pct, and lime to silica ratios were very low, 0.25 to 0.32.

On heats Nos. 4 and 5, limestone was increased to 6 and 8 pct. Slag fluidity was improved. Fluidity tests measured 2 and 3 in., the slags ran freely and continuously down the front trough, and the cupola dropped clean with no slag adherence or bridging. Looking at the slag analyses, the reasons for improved fluidity are obvious. As the CaO contents were increased, the SiO_2 contents were decreased to 42.5 and 41.8 pct, and lime/silica ratios increased to 0.54 and 0.64, respectively. Metal sulfur contents on these higher limestone heats ran approximately 0.020 pct lower.

On heat No. 6 with 3 pct limestone and 2 pct fused sodium carbonate the slag seemed slightly more fluid for its SiO_2 content, apparently a result of the soda content, but fluidity was hardly adequate for good operation.

On heat No. 7 with 3 pct limestone and 2 pct fluorspar, slag fluidity measuring 1¼ in. was better than 3 pct limestone alone but hardly as fluid as the 6 pct and 8 pct limestone heats. Metal sulfur content was equal and carbon content slightly higher.

On heat No. 8, 3 pct fluorspar alone obviously did not provide sufficient CaO content for the slag. Cupola operation was good but the ¼-in. slag was too viscous for clean elimination. Compared to 3 pct limestone, metal sulfur content was essentially the same but carbon content was higher.

On heat No. 9, 5 pct manganese oxide ore was used as flux instead of limestone. The results are primarily of academic interest. The resulting slag had about 20 pct more MnO and 20 pct less CaO than normal slags. The slag of this composition had a reasonably good fluidity of 1½-in. Cupola operation was good; silicon and carbon results were normal. Metal sulfur was lower and manganese content was at least 0.40 pct higher than normal as a result of manganese reduced from the ore.

The last three heats with calcium carbide added experienced the most fluid slags, the cleanest cupola operation, lowest sulfurs, and highest carbons.

On heats Nos. 11 and 12 with 5 pct carbide, 2 pct fluorspar, and 5 pct limestone, sulfur content was reduced to 0.085 pct and 0.071 pct on the two heats. Slags were almost mildly basic with $\frac{CaO+MgO}{SiO_2}$ ratios of 0.87 and 1.08. Total FeO of the slag was reduced to 1.9 and 1.3 pct. Slags of this composition were found to contain 0.27 to 0.33 pct sulfur which explains the considerable desulfurizing ability. Metal carbons were increased to 3.15 and 3.16 pct. Oxidation losses of silicon and manganese were reduced considerably.

These heats indicate some effects of fluxing practice and slag composition and suggest occasional advantages from deviation from standard fluxing.

Slag Fluidity—Some Causes and Effects

Wide variations in slag fluidity are readily apparent, especially on a front-slagging cupola. A viscous slag is sluggish and stringy as it runs down the slag trough. The other extreme is the slag with exceptional fluidity that runs briskly down the slag trough. A need was recognized for some means to measure and express slag fluidity. A split metal mold of the Herty design as used in the steel industry was adapted to cupola slags (Fig. 1). The mold is held under the slag stream, filling the mold basin with molten slag. Out from this basin the slag runs into a ⅜-in. channel until its flow is stopped by its solidi-

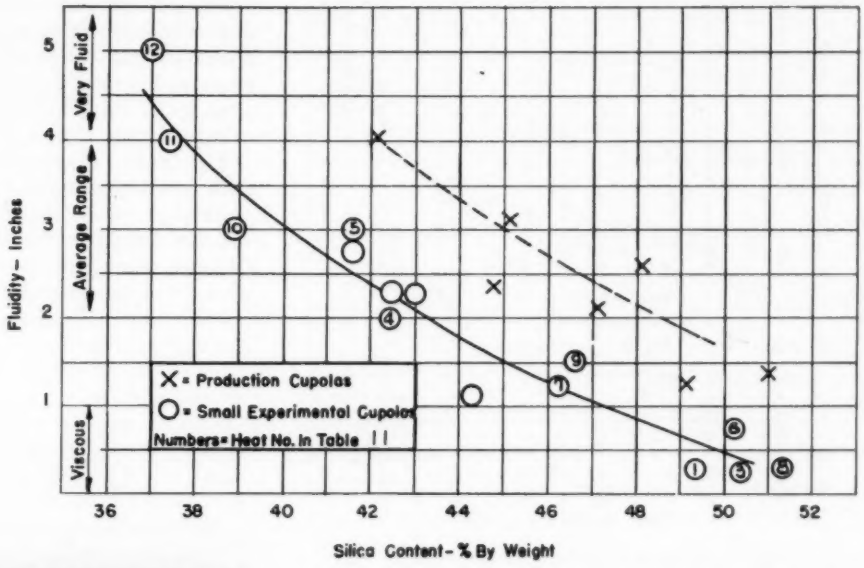


Fig. 2—Slag SiO_2 vs. fluidity.



Fig. 1—Slag viscosity mold—Herty design.

fication in the mold channel. The length of run is measured in inches.

Several slag fluidity measurements are made on each of our experimental heats, and slag fluidity is checked at intervals on all our production cupolas. Experience has indicated that most of the slag conditions that give the best operation range in fluidity from 2-4 in. Fluidities below 1 in. have been found inclined to give some trouble from slag accumulation and bridging. Fluidities above 5 in. have generally been found to be more corrosive on basin refractories, tap hole, etc., unless special refractories are used.

This fluidity test indicates only relative differences in fluidity and has some limitations. Obviously in slag fluidity the temperature of the slag is an important factor as well as its composition. Since most cupolas melt within a 150 F temperature range and any particular furnace usually melts within a 50 F range, temperature as a short term variable does not seem as influential as composition. On the other hand, if one cupola should have an unusually long slag run-off trough, the slag might lose more temperature before reaching the test mold and thereby give a lower fluidity reading for the same slag.

Small slag streams from very small cupolas might lose more temperature and fill the mold basin more slowly than on larger streams from bigger cupolas. Some allowance must be made for the mechanics of taking the test. The fluidity tests were made by holding the fluidity mold directly under the slag stream coming steadily off the slag run off trough. No spoon was used. On the same cupola, fluidity tests have shown reasonably good reproducibility and significant variations in fluidity have been associated with differences in operation and cupola condition.

Table II includes some fluidity measurements for the wide variety of slag compositions and special fluxes discussed in the previous section. Attempts to correlate fluidity with composition have been made. Fluidity seemed to correlate with SiO_2 content better than with any constituent or group of constituents. In slags of this type alumina gives more evidence of behaving as a base and increasing fluidity. All the basic constituents, CaO, MgO, Na_2O, MnO, and FeO seem to increase fluidity *per se*. Correlation with SiO_2 content is equivalent to correlation with the total content of all basic constituents.

In Fig. 2 a number of slags have been graphed with SiO_2 content vs. fluidity. Molar per cent is probably scientifically more accurate, but weight per cent seemed sufficiently accurate within the precision of the test and of more value to the practical operator. The small cupola experimental heats, indicated by circles, show fairly good correlation of fluidity with silica content. With the SiO_2 as high as 50 to 52 pct the slag dropped to ½ to ¼-in. fluidity. With the SiO_2 as low as 37 pct, fluidity increased to approximately 5 in.

A few tests on larger production cupolas seemed to be parallel with the smaller cupola, but at a slightly higher level of fluidity for the same composition. This seems to be due to the larger stream of slag that gets into the mold more forcefully and with less heat loss.

Effect of Coke Amount—Experimental Heats

Some fundamental studies of slag chemistry have been obtained from series of heats on a 21 in. experimental cupola. This cupola has four 3½×2½-in.

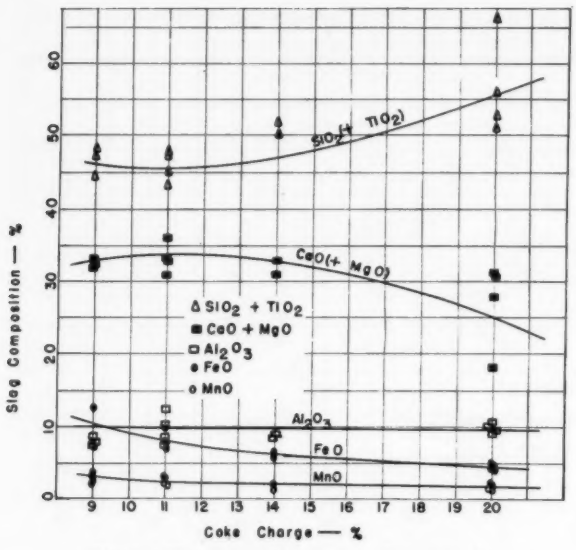


Fig. 3—Slag composition vs. coke amount.

tuyeres located 12 in. from the bottom door, and a stack height of 7 ft 4 in. Air is supplied by a 10×24 positive displacement blower.

The 100-lb charges were 50 pct steel and 50 pct pig. Flux was 5 pct limestone, and coke was 14 pct of the metal charge, screened to pass a 4-in. mesh and be retained on a 3-in. mesh. The normal air volume for standard heats is about 900 cfm or 370 cfm/sq ft of cross-sectional area. These conditions usually result in a melt rate of 1.5 tons/hr and a pressure range of about 8-12 oz.

Slag fluidities are measured in the viscosimeter at regular intervals throughout the heat. Slag samples at 1/3 the heat and 2/3 the heat were analyzed spectrographically. As previously mentioned exact accuracy in the analysis is not claimed, but the variations in the relative proportions should be very accurate.

Several series of heats have been made with variations in one of the above conditions such as coke amount, coke size, air volume, and size and shape of the steel portion of the charge. The primary purpose of these heats was to show the effects of the above mentioned variations on such things as metal temperature, metal chemistry, melting rate and slag performance. The effects of these variables on cupola operation and metal chemistry will be given in another paper. Only the effects on slag composition will be discussed in this report. Some very evident correlations with cupola operation were obtained.

Variations in coke amount of 9, 11, 14 and 20 pct were made in a series of seven heats. Slag composition trends are graphed in Fig. 3. Keeping the flux constant and increasing coke amount from 9-20 pct increased the SiO_2+TiO_2 from about 46 to 56 pct. (Titanium oxide contents were only about 1.0 pct so that the figure shown is essentially SiO_2 content.) This increase of SiO_2 may be accounted for in the increased coke ash and lining consumption.

The CaO+MgO contents decreased from about 34 to 25 pct. (The MgO contents were approximately only 2 pct with the greater proportion as CaO.) The slags with increased silica and decreased lime were rather viscous and troublesome. These variations emphasized the need for additional flux with higher percentages of coke, as indicated by higher percentages of acid constituents and lower fluidities.

The Al_2O_3 content showed no significant change with possibly a slight increase with the higher coke percentage. Both the increased amount of coke ash and the slightly increased refractory consumption contributed some additional alumina, but the total slag quantity apparently increased nearly as much proportionately. The manganese oxide content showed

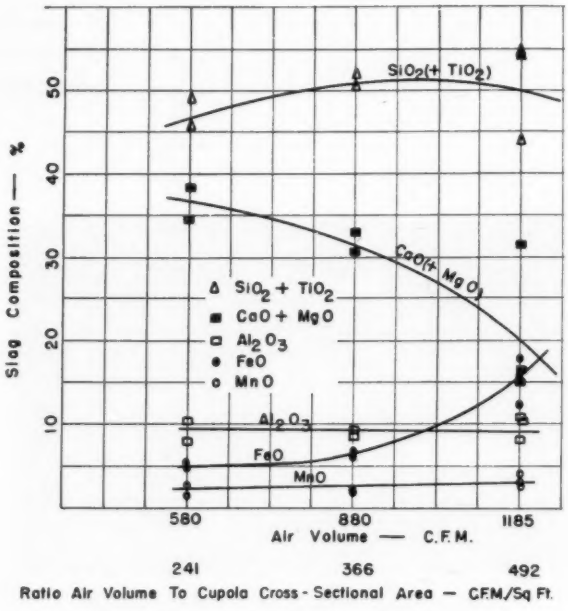


Fig. 4—Slag composition vs. air volume.

no significant change, but a slight decrease with the larger amount of coke.

The most significant effect of coke amount was on slag FeO. Increasing coke amount from 9 to 20 pct decreased FeO from 12 to 5 pct. The greater oxidation with lower percentages of coke was accompanied by higher silicon and manganese losses from the metal. This was reflected in the slag chemistry by higher SiO_2 and MnO constituents at the 9-pct level.

Effect of Air Volume

In another series of three heats, air volume variations of 580, 880, and 1185 cfm (241, 366, 492, cfm/sq ft) were made. Slag results are graphed in Fig. 4. Excessive air volume caused severe lining attack, as indicated by the increase in SiO_2, decrease in CaO, and slight increase in Al_2O_3. This was substantiated by visual and measured inspection of the lining after each heat.

Excessive air volume also caused high slag FeO indicative of high metal oxidation losses. MnO content was also slightly higher. Oxidation of silicon was reflected in the metal chemistry and was very high.

The increase in slag volume due to increased refractory consumption is probably a result of the oxi-

dation. If not compensated for, slags of this nature can very quickly cause bridging, loss of tuyere and stack permeability, and subsequent forced shut-downs.

Effect of Coke Size

In a series of 14 heats, coke amount was kept constant and coke size varied from about 1 to 5 in. Several mixtures of various sizes were tried, also. With one or two exceptions average coke size seemed to show some correlation with metal properties and slag chemistry. (One combination of five screen sizes resulted in a badly bridged cupola and erratic results.) Results are graphed in Fig. 5.

The coke averaging 1 in. (5 pct of cupola diameter) resulted in slag of very high FeO content (18 pct), and low CaO (24 pct). Excessive oxidation caused heavy lining attack, which coupled with metal oxidation products resulted in the low basicity ratio.

As coke size was increased to 2-3 in. (9 pct to 14 pct of cupola diameter), the FeO content of the slag dropped below 5 pct and a better balance of SiO_2 and CaO was obtained. This range of coke size seemed best suited to this size cupola operating under the conditions previously described.

Increasing coke size above this range progressively produced slags of higher FeO and lower basicity. The 5-in. size (24 pct of cupola diameter) resulted in a slag of 12 pct FeO, 58 pct SiO_2+TiO_2, and 12 pct CaO+MgO.

This greater oxidation was directly reflected in excessive metal oxidation losses.

The effects of coke size might be exaggerated some by the small cupola, especially the effects resulting from lining consumption.

Slag condition is more critical and more sensitive in the small cupola.

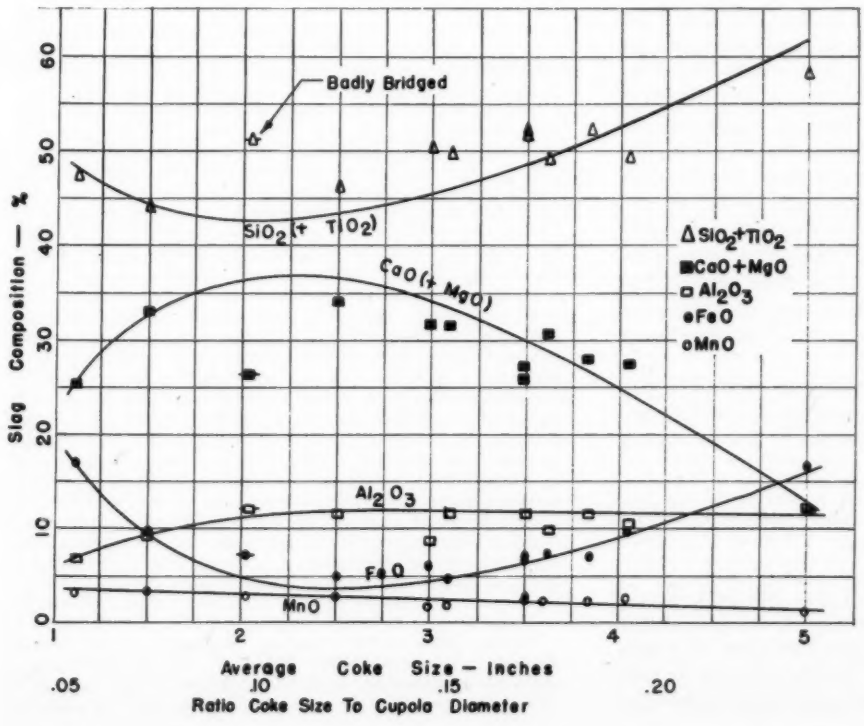


Fig. 5—Slag composition vs. coke size.

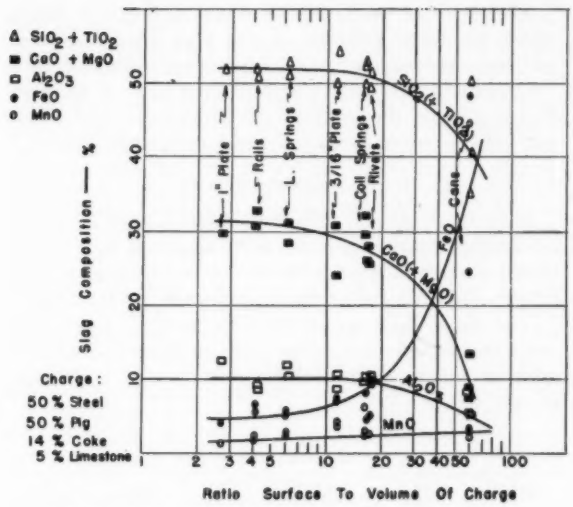


Fig. 6—Slag composition vs. section size of steel.

Our experience suggests that trouble can and is frequently caused by too large coke in a very small cupola and too small coke in large cupolas.

Effect of Section Size of Steel

The importance of selecting the proper grade of scrap steel was emphasized on this series of 14 heats in which the surface area to volume ratio of the steel portion of the charge was varied from about 2.5 to 60. Steel plate from 1-in. thickness down to cans, and steel rods from 3 in. down to ½-in. rivets were used. Results are graphed in Fig. 6.

Large pieces of steel produced definite changes in the physical operation of the cupola, especially rate of melting, metal temperature, etc., but slag chemistry was not greatly affected. Until a ratio of surface area to volume of the steel reached about 10:1, the slag chemistry remained quite normal with about 50 pct SiO_2, 30 pct CaO, 10 pct Al_2O_3, 5 pct FeO, and 2 pct MnO. With very thin steel that increased the ratio above this point, drastic changes in slag chemistry resulted. Extremes were attained on a 60:1 ratio heat in which flattened tin cans were charged as 50 pct of the charge weight. The FeO shot up to 30-40 pct, SiO_2 dropped below 40 pct and CaO was less than 10 pct.

The extreme oxidation of metal and severe lining attack produced volumes of slag very difficult to control, and the tuyeres were kept open with difficulty. The entire stack was saturated with slag. It is interesting to note the amount of dilution of SiO_2, CaO and Al_2O_3 constituents by metal oxidation products.

Summary

Slags from several different cupolas under various operating conditions have been analyzed and an attempt made to correlate their composition with their origin and their relation with cupola performance.

The usual acid cupola slag seems to be predominantly a combination of silicates of lime, aluminum, iron, and manganese, and generally within the ranges shown below:

Acid	Basic
40-50% SiO_2	25-38% CaO (+MgO) (+Na_2)
(0.7-1.5% TiO_2)	10-20% Al_2O_3
	1- 8% iron oxides
	1- 5% MnO

The predominantly acid constituents naturally produced in melting require balancing with additional basic fluxes to develop sufficient fluidity for satisfactory slag elimination. This fluidity has been measured and found for all practical purposes to be proportional to the total of all basic constituents or inversely proportional to the silica content.

High silica slags with low fluidity result from the minimum addition of basic flux and produce lower carbons, especially on low carbon mixes very dependent on carbon pickup. Slags of this extreme are more inclined to accumulate and adhere to the coke. If too viscous, melting efficiency may be impaired and oxidation increased.

On the other extreme large amounts of limestone produce slags with higher basicity and great fluidity that leave the stack cleaner and the coke more reactive, resulting in higher carbons and more efficient combustion. Although not capable of significant desulfurization, acid slags with higher basicity and low FeO can absorb enough sulfur to reduce metal sulfur content 0.005 to 0.015 pct lower than a slag of less favorable analysis. With basicity too high refractory attack increases. Mildly basic slags have been produced in an acid-lined cupola for short periods but are impractical for long heats. If the advantages of a more basic slag are desired, a basic lining should be adopted.

The proper amount of basic flux to produce a slag of desired consistency varies considerably on different cupolas and mixes due to variations in the sources of slag, namely:

1. Ash of coke
2. Refractory lining consumed
3. Oxidation products
4. Extraneous dirt

The total of these sources of slag has been found to vary from the extremes of 1½ to 10 pct of the melt requiring from 1½ to 8 pct limestone to obtain satisfactory slag composition. Increased coke as well as higher ash coke both require more basic flux. The need for more limestone has been shown when refractory consumption has been increased, either from the inherent increase with decreasing cupola size, overblowing, excessively small coke, or high steel mixes.

Since SiO_2 is the predominant product of oxidation still more basic flux is required to combat higher degrees of oxidation caused by poor charging, high steel mixes, thin steel, overblowing, and small coke. Preheated blast and good charging have been shown to reduce oxidation, sources of slag, and slag FeO. Smaller cupolas experience relatively more oxidation for the same conditions. Iron oxide content of the slag is a good indication of the degree of over-all oxidation and correlates resonably well with silicon loss.

With clean charges in a large cupola, slag control is not difficult with consistent practice. But in smaller cupolas slag control becomes more difficult when forced to use high proportions of scrap or steel, high-ash coke, or small coke.

Some attention to the slag factors discussed has been found necessary to maintain consistent operation through all the variations in raw material quality and operating conditions.

Acknowledgments

The authors wish to acknowledge the assistance of R. E. Deas and R. H. Elder and their associates in the chemical laboratory for their numerous slag analyses, and W. R. Kennedy and his associates in the spectrographic laboratory for the development of spectrographic methods and the numerous determinations. We wish to thank James Ellis and members of the melting department for assistance in collecting the information, and James Moore and Hugh Smith for the graphs. Thanks are due the management of the American Cast Iron Pipe Company for permission to publish this data.

DISCUSSION

Chairman: H. A. DEANE, Campbell, Wyant & Cannon Foundry Co., Muskegon, Mich.

Co-Chairman: G. L. RICHTER, Farrel-Birmingham Co., Ansonia, Conn.

Recorder: W. M. SPEAR, Worthington Corp., Harrison, N. J.

R. A. CLARK *(Written Discussion)*:[1] The authors of this paper have successfully assembled a large mass of data into such form that logical conclusions as to the effects of variation in slag condition and analysis can be made. They should be congratulated on a valuable contribution to our knowledge of the factors affecting cupola operation.

Their conclusions, in many cases, check very well with the writer's personal experience. We were particularly interested in the conclusion that high lime slags result in increased carbon pickup in the metal, even in the acid cupola and agree with this conclusion. This is attributed to the fact that a viscous, siliceous slag prevents efficient combustion of coke and tends to blanket the fuel in such a manner that contact between the metal and coke is limited. It may be interesting to note, however, that a somewhat similar relationship exists in other metals melted in furnaces in which the increased carbon pickup under more basic slags cannot be related to the condition of a coke bed. As an illustration, there is a trend toward carbon pickup when melting low carbon stainless steels in the basic electric furnace. This trend becomes more decided as the basicity of the slag is increased. One of the methods of limiting carbon creep under these conditions is to keep the slag as acid as is practical in a basic furnace.

Another point which might be emphasized is the authors' statement that irregularities found in melting conditions at their plant were the result of variation in the amount of SiO_2 introduced into the cupola as adhering sand on scrap, etc.

such variation, of course, makes control of slag balance difficult. It might be emphasized also that it takes a great deal of heat to form and melt slag. We understand blast furnace plants operate on the basis that 0.4 lb of coke are required for each pound of slag produced. If this figure applies to the cupola also, for every unnecessary pound of SiO_2 introduced as sand or eroded refractory, we need extra limestone and make some 2 to 2½ lb of extra slag, requiring an extra pound of coke for fusion. These figures point to the importance of clean melting stock and consistently good refractory practice, points which are borne out in commercial experience but sometimes overlooked in individual foundries. Certainly, charging of uncontrolled amounts of sand can lead to many serious irregularities in cupola melting. Some SiO_2 is necessary for slag balance. The ideal condition would be to control the amount as closely as the weight of limestone or other flux.

We might also suggest that, while the authors have shown a close relationship between the analysis of the tapping slag and results obtained from their cupolas, the slag which actually affects the metal may not be the slag as it is tapped from the furnace, but rather, the slags existing in the melting zone and coke bed of the cupola. The fusion temperature of burnt lime resulting from calcination of limestone is high (4700 F for pure CaO) and certainly above the temperatures met within the cupola. This burnt lime must come into physical contact with and be fluxed by SiO_2 from coke ash and other sources (also having high fusion temperatures) in order to form a fluid slag. There is an old foundry saying which has come down from the days of hand charging to the effect that careful spreading of limestone is worth an extra shovelful per charge. This seems to be borne out, and in practice we find that where charging equipment is such as to make uniform distribution of the flux charge across the section of the cupola difficult to obtain, it is also difficult to obtain consistent results from the furnace. In such cases, the slags found in various portions of the cupola bed probably bear little relationship to the tapping slags which can be sampled at the slag hole. The excellent correlation between chemical analysis of the cupola slags and the results reported by the authors undoubtedly indicate a high level of excellence in their melting practice. Such an exact correlation might not be obtained in plants having less carefully controlled melting conditions.

Again, the authors should be congratulated on an excellent paper forming a worthwhile addition to our literature on cupola operation.

MR. CARTER *(Author's Reply to Mr. Clark)*: We appreciate the remarks of Mr. Clark and verification from his experience that high lime slags give increased carbon pickup and that variations in sand on scrap are frequent causes of slag irregularity.

We agree with the emphasis that the slag as sampled might not always accurately represent the slag in the cupola. It is true that uneven charging, improper blast distribution, irregular lining practice, and many other factors can produce a slag composition on one side of a cupola considerably different from the other side. It can only be hoped that these irregularities are held to a minimum and that the slag sample from a smoothly flowing front slagging cupola represents a fair average of the overall slag composition.

Actually it seems likely that a high lime slag might be diluted some from the refractories consumed in the cupola well and trough so that the external sample appears less basic than actually experienced in the cupola melting zone. Likewise on a viscous high silicon slag it seems possible that proportionately more of the more fluid portion of the slag flows out the tap hole, (or slag hole) to be sampled, while proportionately more of the less fluid slag adheres to the coke and refractories and remains to form a bridge in the cupola. I believe we can safely assume that variations in slag composition inside the cupola are greater than indicated in the slags as sampled.

We appreciate the compliments on the good correlation and excellence of melting practice, but we must admit we left out some data that did not show such good correlation because we felt we did not have sufficiently controlled melting conditions. Because of some of these irregularities, sampling limitations etc. we feel that definite conclusions on slag behavior should be drawn very carefully and only after a sufficient volume of well controlled data.

A. E. SCHUH *(Written Discussion)*:[2] First of all, the authors are to be congratulated for presenting an excellent paper. Information of a quantitative nature regarding this important subject has been needed in this country for some time. True enough, much has been written in the past few years regarding slag chemistry in connection with basic-lined cupolas and a general inference, I fear, has been drawn that either (1) slag reactions were relatively not important in acid-lined cupolas, (2) that in a general way we knew about the subject, and (3) that not much could be done about it in any event. This paper definitely serves to correct any such erroneous impressions.

The material presented is obviously based on a wealth of observations obtained over a considerable time and a wide variety of operating conditions. It cannot be mere coincidence that in the operation of a group of acid-lined cupolas having a sixteen-fold range in cross-sectional area it is found that "good" slags in terms of satisfactory practical cupola operation have nearly identical chemical composition. To obtain this relative constancy of slag composition the authors found it necessary to increase the proportion of basic flux material in the charge over a rather wide range as the size of the cupola decreased, which in turn modified the ratio of lining surface to cross-sectional area and normally required higher percentages of coke for equivalent thermal balances. It is particularly gratifying to learn how closely the calculated and actual composition of the slags matched over the extreme of the range when proper recognition was made of the source of the materials which formed them, both in the charge and from the reaction with the acidic lining.

Although the authors emphasize the fact that the cupola's function is to make satisfactory iron and not satisfactory slag it is clear that the two go hand in hand. This mutual relationship illustrates the diagnostic value in slag appraisal. The situation is not too dissimilar from that prevailing in a medical check-up in which the examination of the products of elimination are considered necessary steps towards diagnosing our state of health. The authors will, it is hoped, elaborate on this relationship in the future but it is clear that the use of the requisite and adequate quantities of limestone in the charge has served among other things to make the coke more available both thermally and chemically, thereby increasing the metal temperature and hence its carbon content. The authors did not mention it, but it would be interesting to learn how much beneficial effect a properly fluid slag has had on metal cleanliness, although it is realized that the measurement of this property is difficult.

It is hoped that the presentation of this paper will stimulate others in the industry either (1) to report their experiences along similar lines of investigation or (2) to instigate new work along this line for future presentation. By this means a broad base of understanding will be obtained.

The chief deterrent for work of this nature has been the cumbersomeness of the analytical techniques involved. The following suggestion may be somewhat premature, but it does seem that enough background information has now been furnished so that a less complete appraisal of the slags, when properly correlated to the cupola operating factors might furnish much useful information quickly, without inviting excessive danger of over-simplification of distortion. The writer has in mind a concentration of the appraisal technique to merely three tests, namely a determination of the silica content of the slag, its FeO content, and its viscosity. With respect to viscosity it is apparent that further developmental work is needed, particularly with respect to the effect of the temperature and volume of the slag. In making the above suggestion it is assumed that only normal basic fluxes such as limestone or dolomite are employed. A further and perhaps too obvious suggestion is that the readily visual factors of the slag such as its color, gloss, texture, and fracture appearance be at least qualitatively correlated with the composition of the slag and the attendant cupola operating conditions. Considerable work along this latter line is being done currently by the Gray Iron Research Institute. These suggestions may expedite the usefulness of slag appraisal for guiding cupola performance and comments on them by the authors would be helpful.

In closing, the writer wishes again to express his appreciation of the importance of this paper and hopes it will stimulate constructive further activity within our industry.

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[2] Director of Research, United States Pipe & Foundry Co., Burlington, N.J.

EVALUATION OF MOLD MATERIALS FOR TITANIUM CASTINGS

By

R. M. Lang, J. G. Kura, and J. H. Jackson*

Introduction

Development of processes for casting titanium presents many difficult problems because molten titanium is so chemically active. It reacts with or dissolves all known refractory materials. When contaminated by only small amounts of oxygen, nitrogen, or carbon, titanium becomes very hard and brittle. Uncontaminated titanium is ductile, has a high strength-to-weight ratio, and excellent corrosion resistance. The research described in this paper has been directed toward the development of processes for making high-purity titanium castings.

There are two major problems that must be overcome before high-purity titanium castings can be made. These problems are (1) to develop a method for making high-purity titanium melts large enough to be cast into molds, and (2) to develop mold materials which will not react with or contaminate the titanium castings.

Concurrent research programs at Frankford Arsenal and at Battelle have been directed toward the solution of these two problems. Simmons and Edelman[1] have described a skull-melting arc furnace developed for melting high-purity titanium. A modification of this furnace has been built at Battelle for future evaluation of more promising mold materials. Meanwhile, preliminary screening of mold materials has been conducted in an assembly which utilizes a graphite-lined, induction-melting furnace operating in a sealed dome. This latter furnace and the mold-development program are discussed at length in this paper.

Experimental Procedure

Description of Furnace—The graphite-lined induction furnace enclosed in a sealed chamber, which was used in this investigation, is sketched in Fig. 1. The whole furnace assembly is mounted on a hinged table which is tilted to pour the molten metal into the mold. The induction coil is connected to a motor-generator unit having a rated output of 80 kw at 9600 cycles per second.

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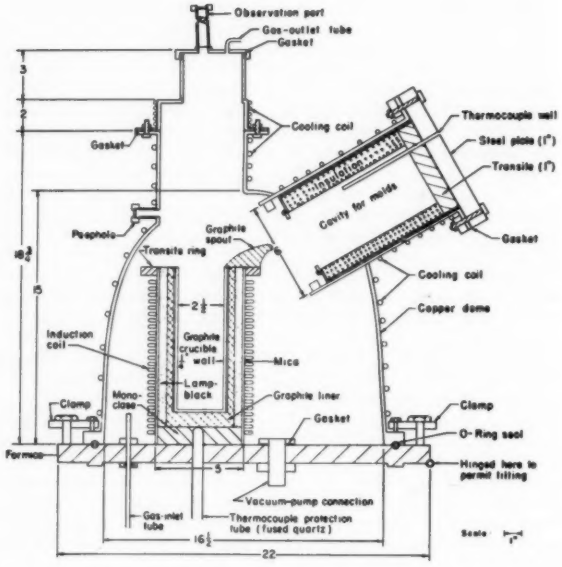


Fig. 1—Sectional view of gas-atmosphere induction furnace.

Process A titanium sponge was used as melting stock for all tests. Considerable difficulty was encountered in melting loose sponge, because it tended to form a bridge in the crucible. To eliminate this difficulty, the sponge was formed into briquettes which were melted without difficulty.

During melting, the sponge evolved a considerable amount of smoke. This made it impossible to measure the temperature of the melt with an optical pyrometer. Immersion thermocouples could not be used because of the high temperature and reactivity of the molten titanium. Therefore, a standard melting procedure was used, so that all melts would be poured at approximately the same temperature. The procedure employed was as follows:

1. Charge 2 lb of briquetted Process A titanium sponge into the graphite crucible.

2. Evacuate the furnace and backfill with helium. Maintain a slight, positive measure of helium.

3. Melt charge. Maintain power to induction coil at 30 kw and unity power factor.

4. Superheat melt for 30 seconds after the charge becomes molten.

5. Pour melt into the mold. Maintain power on induction coil during pouring.

All mold materials were tested using the above procedure.

Test Casting Design—The test casting used for evaluating mold materials was a rectangular step bar shown in Fig. 2. This casting was designed so that the effect of section size on mold reaction could be studied. All castings were poured from the large end of the mold with the steps in the cope and the flat surface in the drag.

Mold Materials—Previous work had shown that there was considerable difficulty with pinholing in titanium castings made in shell molds.[2] It was not known whether the pinholes were caused by gases

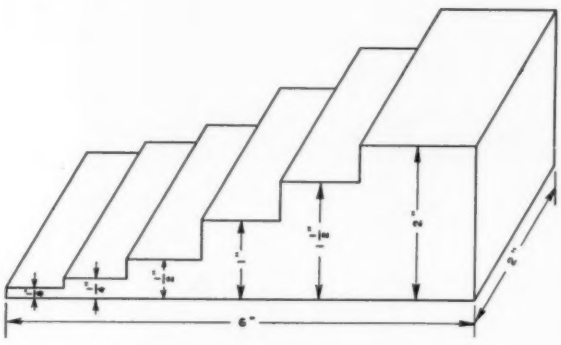


Fig. 2—Rectangular step casting designed for evaluation of mold materials for titanium.

dissolved in the melt, by reaction between the melt and the mold, or by gases evolved by the decomposition of the resin used to bond the mold.

To determine whether or not the pinholes were caused by gases dissolved in the melt, a casting was made in a graphite mold. The casting made in the graphite mold, as shown in Fig. 3, was free of pinholes. Therefore, pinholing could not be attributed to gases in the melt.

If pinholes in shell-molded castings were caused by reaction between the mold and the titanium, it might be possible to inhibit the reaction by coating the mold with a material more resistant to attack by titanium. Because graphite molds are fairly resistant to reaction, a graphite wash was tested on various shell molds. This wash was composed of a mixture of 1 part concentrated dispersion of colloidal graphite and 3 parts isopropyl alcohol. This colloidal graphite diluted with alcohol was found to be a better wash than colloidal graphite diluted with water, because it was more penetrating and formed a smoother, more adherent coating.

Shell molds made of either electrically fused zirconia, electrically fused alumina, zircon, or silica were coated with the colloidal graphite wash. The screen analyses of the refractories used in these molds are listed in Table 1. Four per cent phenolic shell-molding resin was used to bond these molds.

Castings made in the colloidal graphite-washed shell molds are shown in Figs. 4 to 7, inclusive.

A comparison between washed and unwashed molds revealed that the graphite wash reduced the number of pinholes in the castings, but did not completely eliminate them. Castings made in the graphite-washed zirconia shell mold had the least number of pinholes, while castings made in the graphite-washed silica shell mold had the most.

Because some metals are resistant to attack by momentary contact with molten titanium, metallic coatings were tested on shell molds. These metallic coatings were applied to silica shell molds with a metallizing gun. The coatings were approximately 0.005 in. thick. Coatings of copper and molybdenum adhered very well to the shell molds, but 18-8 stainless steel tended to shrink and pull away from the mold so that it could not be used.

A casting made in the copper-coated shell mold is shown in Fig. 8. Apparently, the copper was melted away by the titanium, and, in the heavy sections, the titanium reacted extensively with the silica. The casting made in the mold coated with molybdenum is shown in Fig. 9. The molybdenum coating resisted attack by the titanium, but the surface of the casting was very irregular. Because of the difference between the rates of expansion of silica and of molybdenum, the molybdenum coating buckled and formed large bulges and wrinkles on the casting surface.

It was believed that pinholing in titanium castings might be caused by gases evolved by the decomposition of the shell-molding resin during the casting operation. Therefore, several different molds bonded with inorganic materials were made and tested to determine what effect they would have on pinholes. Zirconia was used as the refractory in these molds because it seemed to cause less pinholing than the other materials tested as shell molds.

One of the inorganic-bonded molds was made of zirconia bonded with 8 per cent sodium silicate. This mold was fired at 1800 F for 2 hr to remove all chemically bonded water and was cast while still hot. The resulting casting, shown in Fig. 10, had many large pinholes. It appears that the sodium silicate has a

TABLE 1—SCREEN ANALYSIS OF MOLD MATERIALS USED FOR SHELL MOLDING

Screen Size	Amount on Screen, Weight Per Cent			
	Fused Zirconia	Fused Alumina	Zircon	Silica
20	—	0.2	—	—
30	0.1	5.6	—	—
40	0.6	14.0	—	—
50	26.5	14.3	—	—
70	37.9	12.7	—	—
100	27.3	14.1	2.6	6.7
140	1.8	9.4	30.7	43.2
200	1.1	7.4	55.7	33.8
270	1.4	5.5	10.4	11.2
Fines	3.3	16.8	0.6	5.1
A.F.S. Fineness No.	65	157	132	131

Fig. 3 — Titanium cast in a graphite mold. Defects are cold shuts and seams caused by rapid chilling of the melt by the mold.

Fig. 4—Titanium cast in a zirconia shell mold washed with colloidal graphite.

Fig. 5—Titanium cast in an alumina shell mold washed with colloidal graphite.

Fig. 6—Titanium cast in a zircon shell mold washed with colloidal graphite.

Fig. 7—Titanium cast in a silica shell mold washed with colloidal graphite.



Fig. 8—Titanium cast in a copper-coated silica shell mold.

Fig. 9—Titanium cast in molybdenum-coated silica shell mold.

Fig. 10—Titanium cast in a zirconia mold bonded with sodium silicate.

Fig. 11—Titanium cast in a zirconia mold bonded with zirconium oxychloride.

Fig. 12—Titanium cast in a baked core-sand mold washed with graphite.

fluxing action which caused the mold to react with the molten titanium.

Another inorganic bonded mold was made of zirconia bonded with zirconium oxychloride. The molding mixture contained 5 per cent zirconium oxychloride which was added as a saturated water solution. The mold made from this material was baked at 450 F before casting. When titanium was cast in this mold, there was some chlorine evolved from the mold, but this did not seem to affect the casting. As can be seen in Fig. 11, this casting had only a few tiny pinholes. This type of mold material shows considerable promise, because it may be possible to improve the surface finish of the casting and eliminate pinholes entirely by using zirconia of a finer particle size.

A baked core-sand mold for titanium castings was studied by Johnson.[3] A mold mix, similar to that reported by Johnson, was prepared as follows:

Silica sand, A.F.S. Fineness No. 84 — 87		pct
Silica flour	— 4.5	"
Pitch	— 2.0	"
Cereal binder	— 0.5	"
Core oil	— 1.0	"
Resin binder	— 2.0	"
Water	— 3.0	"

This mold was baked at 425 F for 2 hr and then washed with a colloidal dispersion of graphite in benzene. After applying the wash, the mold was dried by baking at 325 F for 20 minutes. The mold was then cast while still hot. A large amount of gas was evolved by this mold. The resulting casting, as shown in Fig. 12, had many defects which can be attributed to the gas evolved by the mold. It is believed that, even if this mold had been vented, there still would have been a large number of pinholes in the casting.

Conclusions

Titanium castings up to ½ in. in cross-section can be cast with good surface finish and accurate reproduction of detail in graphite-washed zirconia or graphite-washed alumina shell molds. If the section size is larger than ½ in., there may be considerable difficulty with pinholing and mold reaction. Commercially acceptable titanium castings of larger section size, possibly up to 2 in., can be cast in zirconia molds bonded with zirconium oxychloride. Before making larger castings, however, more work must be done to determine the depth to which castings of various section sizes are contaminated by the mold.

The test casting used in this investigation provided a severe test of the mold materials. Most of the defects occurred at the re-entrant angles where there was a change in section size. These re-entrant angles with unfilleted corners are usually avoided in good casting design, because of the high concentration of heat in these areas. Castings for specific applications can be designed in which there are no such areas of high heat concentration. Thus, with good casting design, it may be possible to eliminate many of the pinholes found in the test castings.

The mold materials developed in this investigation are fairly limited in their use, however, and are quite expensive. Therefore, more work must be done to develop better and less expensive mold materials.

Acknowledgments

The authors express appreciation to Frankford Arsenal for permission to publish the results obtained on the work sponsored under Contract No. DA-33-019-ORD-223. Acknowledgment is also given to Messrs. O. W. Simmons, H. Markus, and Lt. Col. D. J. Murphy, under whose supervision the work was conducted. The authors also wish to thank C. Gruber and J. Ulrich, who helped conduct most of the laboratory work.

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DISCUSSION

Chairman: W. A. DEAN, Aluminum Company of America, Cleveland.

Co-Chairman: R. A. LUBKER, Armour Research Foundation, Chicago.

Recorder: P. D. FROST, Battelle Memorial Institute, Columbus, Ohio.

MARVIN GLASSENBERG:[1] How did the authors maintain a constant amount of superheat for the charge in each heat?

At Armour Research Foundation, we control superheat by probing the charge with a graphite rod to determine the initial melting point. After the charge is molten, we permit a given time to elapse before the furnace is tapped. Thus, superheat of the charge is measured by the amount of time elapsed between the initial liquid state of the metal and tapping.

MR. LANG: We used the same weight of charge and the same power input from heat to heat. When melting occurred, considerable quantities of fumes were evolved, and this occurred at about the same time for each heat. After the appearance of the first fumes, we superheated 30 to 60 sec prior to pouring.

M. J. BERGER:[1] We have found in our work at Armour that the surface quality of titanium castings varies considerably from heat to heat, even when identical mold materials and metal charges are used. One good casting does not guarantee that the next one made in the same way will also have a good finish. Will the authors comment on the reproducibility of their results?

MR. LANG: We have made only one casting for each mold material described in this paper. In previous work with other mold materials, we have made several castings in the same type of mold and got fairly good reproducibility.

MEMBER: What was the crucible charge, and how was the crucible held in place when the furnace assembly was tilted for pouring?

MR. LANG: Process A titanium sponge was the crucible charge. The crucible was held in place by the graphite pouring spout shown on top of the transite ring.

MEMBER: What was the method used for mixing the zirconium oxychloride binder with the zirconia sand? What is the cost of the zirconium oxychloride?

MR. LANG: The zirconium oxychloride was added as a saturated water solution and mixed with the sand by hand. The zirconium oxychloride cost approximately 35 cents per pound.

R. E. EDELMAN:[2] Is the furnace used adaptable to larger castings?

MR. LANG: No.

O. W. SIMMONS:[3] I should like to mention that titanium reacts with the mold materials exothermally; thus, the reaction is self-propagating so that the mold and metal surfaces are kept at the reaction temperature until the reacting surfaces have been consumed.

[1] Armour Research Foundation, Chicago.
[2] Frankford Arsenal, Philadelphia.
[3] Rem-Cru Titanium, Inc., Midland, Pa.

CHAIRMAN DEAN: How sound are these castings? Is pinholing the only defect?

MR. LANG: Pinholes are the only defect, and these penetrate the castings to a depth up to about ⅛ in.

S. V. ARNOLD: [3] Under what pressure are the melts made? Could the gas which causes pinholing be absorbed hydrogen?

MR. LANG: Melts were made under slight positive pressure of helium. What causes the pinholing has not been established.

MR. ARNOLD: It would seem that, if melts are made under a slight positive helium atmosphere, hydrogen from the titanium sponge could be the only gas available to cause pinholes.

MR. DEAN: Do you mean that hydrogen would be absorbed from the mold surface?

MR. ARNOLD: No, it would come from absorbed water present in the titanium sponge as charged.

RECORDER FROST: Have you observed the titanium hydride phase in your castings? Large amounts of this phase would suggest that hydrogen might be responsible for pinholes.

MR. LANG: No work has been done to determine whether the hydride phase is present.

M. W. MOTE: [4] I was under the impression that titanium, unlike metals which do not form hydrides, has an increasing solubility for hydrogen with decreasing temperature. Thus, the absence of porosity in the casting may not be a real indication of a low hydrogen content in the melt.

MEMBER: Was the sponge given a purifying treatment prior to being melted?

MR. LANG: No, it was briquetted and melted.

W. E. MCCULLOUGH: [5] This paper and the present discussion will certainly be of historic value in the years to come after the titanium-casting industry has been established. I should like to ask whether or not the mold materials used in this work might be applicable for cores?

MR. LANG: I believe they could be applicable.

MR. JACKSON: We should point out that this furnace is only a screening tool. A large, skull-melting, arc furnace of a design similar to that used at the Frankford Arsenal by Simmons is available for larger scale evaluation of the most promising mold materials.

MR. LANG: Titanium castings weighing over 2 lb have been made in the skull-melting furnace. The melts made in this furnace had good fluidity, and no trouble was encountered with cold shuts or folds except when casting into graphite molds.

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[4] Battelle Memorial Institute, Columbus, Ohio.
[5] Bohn Aluminum & Brass Corp., Detroit.

PHOSPHORUS REMOVAL IN BASIC CUPOLA OPERATION

By

J. E. Bolt* and W. C. Jeffery**

ABSTRACT

The paper summarizes the results of a research project conducted in partial fulfillment of the requirements for a masters degree and subsequent work by the author at the University of Alabama.

Except for the few but growing number of basic lined cupolas, nearly all the iron melted in foundries of the United States is melted in cupolas with acid linings. While the cupola offers great economy in melting, acid practice cannot affect the removal of two detrimental elements, namely, sulphur and phosphorus.

Actually, the sulphur content normally is increased during acid melting by absorption from coke. Many foundries use some type of post melting treatment for desulphurization and in recent years more progressive foundrymen have turned to the basic cupola for reduction of sulphur. Phosphorus, on the other hand, has for years gone through our cupolas with neither increase nor decrease in content and will do likewise in basic cupolas unless operating conditions are adjusted especially for phosphorus removal.

Years of practice in steel making and the works of Carter,[1] Flinn and Kraft,[2] and others on basic cupolas have given us the principles upon which to base operating conditions for either desulphurization. Fundamentally, desulphurization is favored by hot, basic, and reducing conditions while dephosphorization is favored by lower temperature, basic, and oxidizing conditions.

The exact dephosphorization reaction has not as yet been reliably determined. Phosphorus will oxidize most readily at low temperatures, but without a basic slag containing free lime to hold the P_2O_5 as calcium phosphate in the slag, the P_2O_5 may be reduced again by CO gas returning the phosphorus to the iron as Fe_3P. This latter reaction becomes more prevalent with increasing temperatures.

It was the object of this investigation to study the removal of phosphorus and its dependence on other elemental contents in basic cupola melting. In the first four heats it was desired to show a relationship between phosphorus removal and the degree of oxidation in the cupola by varying coke ratios from 6:1 to 12:1, keeping all other factors constant. The fifth cupola heat duplicated the best apparent coke ratio of the first series using a 50 per cent increase in limestone.

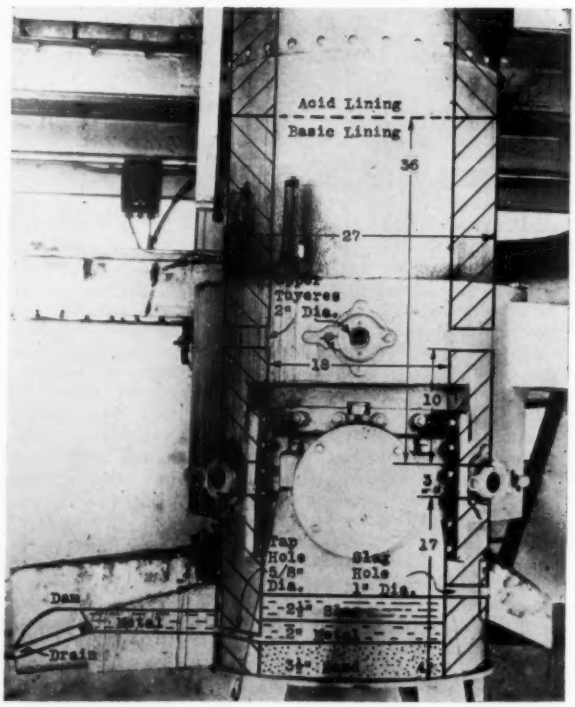


Fig. 1—Details of cupola lining, slag, and metal levels.

Lower Lining Was Basic

The cupola used in this investigation was equipped with an air weight controller and centrifugal fan type blower. The cupola measured 85 in. from the bottom of the base plate to the charging apron. The well and four rows of brick above the lower tuyeres were lined with No. 3 magnesite-chrome arch bricks directly

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2. R. A. Flinn and R. W. Kraft, "The Importance of Slag Control in Basic Cupola Operations," *Transactions, A.F.S.*, vol. 59, pp. 323-329 (1951).

* Lt. USNR, Welding and Castings Code, Bureau of Ships, Dept. of the Navy, Washington, D. C. Formerly foundry instructor, University of Alabama. ** Asst. Professor, Metallurgical Engineering Dept., University of Alabama.

TABLE I . . .CHARGE COMPOSITIONS

Heat	Coke, lb	Limestone, lb	Soda Ash, lb	Metal, lb
1	16⅔	8	2	100
2	12½	8	2	100
3	10	8	2	100
4	8⅓	8	2	100
5	11	12	2	100

TABLE 2 . . . CHEMICAL ANALYSES OF METAL

Heat	Metal Sample	Time after tap-out, min	Spout Temp., F	Composition, per cent				
				TC	Mn	P	S	Si
1	1A01	13	2420	3.50		.282	.184	.30
	1A02	21	2590	3.63	.35	.278	.170	.29
	1A03	34	2600	3.35	.35	.270	.161	.28
	1A04	46	2620	3.55		.262		.25
	1A05	58	2560	3.36	.33	.238	.155	.22
	1A06	69	2580	3.36		.257		
	1A07	80	2560	3.38	.32	.251	.136	
	1A08	91	2520	3.40		.241		
	1A09	106	2540	3.37	.32	.232	.132	.17
	1A10	112	2520	3.28		.223		
	1A11	118		3.23	.27	.215	.133	.14
2	2A01	1	2410	3.64	.32	.286	.197	.32
	2A02	12	2480	3.57	.31	.282	.158	.29
	2A03	27	2580	3.29	.35	.272	.160	.24
	2A04	39	2540	3.42		.248		
	2A05	49	2530	3.35	.34	.238	.149	.19
	2A06	57	2520	3.28		.225		
	2A07	67	2470	3.05		.193		
	2A08	80	2480	3.09	.32	.200	.132	.15
	2A09	88	2460	3.13		.212		
	2A10	99		3.05		.209		
	2A11	101	2420	3.09	.32	.215	.142	.12
	2A12	104		3.16		.189		
	2A13	109	2410	3.14	.32	.218	.139	.13
	2A14	111		3.11	.29	.219	.129	.13
3	3A01	2	2400	3.54	.30	.325	.175	.35
	3A02	12	2560	3.60	.36	.303	.140	.39
	3A03	22	2610	3.50	.34	.278	.129	.30
	3A04	32	2570	3.46		.272		
	3A05	42	2580			.250		
	3A06	52	2590	3.44	.28	.245	.124	.21
	3A07	62	2570			.223		
	3A08	72	2560	3.19		.188		
	3A09	82	2540			.182		
	3A10	92	2520	3.05	.24	.182	.129	.13
	3A11	102	2420			.147		
	3A12	109	2410	2.69		.149		
	3A13	112				.145		
	3A14	115		2.78	.23	.151	.138	.12
4	4A01	1	2520	3.54	.33	.319	.206	.26
	4A02	11	2500	3.52		.299	.178	.23
	4A03	21	2540	3.53	.30	.287	.164	.23
	4A04	31	2550			.275		
	4A05	41	2550	3.29		.225	.141	.20
	4A06	51	2480			.231		
	4A07	61	2480	3.24	.24	.180	.143	.12
	4A08	71	2480			.200		
	4A09	76	2450	3.06		.182	.131	.11
	4A11	81	2420	3.02	.20	.179	.133	.10
	4A12	83	2440			.182		
	4A13	85	2470*	2.95		.182	.141	.12
	4A14	87		2.98	.20	.189	.136	.12
5	1B01	1	2480	3.60	.32	.33	.216	.36
	1B02	11	2580	3.62		.30	.165	
	1B03	21	2620	3.47	.32	255	.145	.29
	1B04	34	2620			.228		
	1B05	41	2550	3.33	.32	.211	.153	.20
	1B06	51	2560			.193		
	1B07	61	2500	3.20	.29	.165	.130	.13
	1B08	71	2410			.163		
	1B09	81	2390	2.93	.17	.108	.134	.09
	1B10	90		2.73	.15	.104	.148	.07

*Taken after drain plug was opened; represents cupola well temperature.

against the 27-in. shell resulting in an 18-in. inside diameter. Acid bricks were used to complete the lining.

Figure 1 shows the details of levels and lining inside the cupola. The height of the dam was raised 1 in. over the inside iron level to counteract the blast pressure. Operation was continuous tap, back slagging without interruption after the blast was turned on for each of the heats. This method was chosen in order to maintain constant slag and metal levels.

Every effort was made to insure that all heats were run exactly alike except for the desired single variable per heat. Tempered heap sand was used for the cupola bottom. Wood was used for lighting the bed, and a 4-hr burn-in time was used. The final bed height, 52 in. above the top of the lower tuyeres, was considered the minimum for this cupola to give a satisfactory tapping temperature. Charging started on a closely controlled time schedule using the sequence of coke, flux, and metal with no limestone and fused soda ash on the first two charges. Table 1 shows the charges per heat with the exception that the coke charges on all heats were maintained for the first five charges at the 6:1 ratio, or 16⅔ lb per charge to insure molten metal at tap out.

Coke was screened to 3 x 2 in. size, soda ash blocks were crushed and placed in paper bags with limestone, and the metal charge was of very uniform block size, 1½ x 3½ x 4 in., of the following chemical composition: C, 3.45-3.55 per cent; Si, 0.50-0.55; Mn, 0.50-0.55; S, 0.14 max; and P, 0.28-0.33.

Changes in Composition

Care was taken to see that each charge was placed level in the cupola and that the cupola was kept full at all times. It was considered that the first hour after tapping would be required for equilibrium conditions to be reached inside the cupola and that the runs should continue for one additional hour giving a total melt of 3000 to 3700 lb dependent upon melting rate. This was accomplished on all but the last two heats which will be discussed later.

Tables 2 and 3 show the chemical analyses of both metal and slag samples taken during each heat.

Figure 2 is a summary of curves showing the chemical analyses for the elements phosphorus, silicon, total carbon, manganese, and sulphur, along with the metal temperature at the spout, each plotted against time in minutes after tap out.

For all the elements with the exception of total carbon, the ordinates, or percentages, are plotted at the same scale to facilitate comparison.

The abscissa, or time scale, is comparable for all curves.

Below is listed each element followed by a brief discussion of its relationships under the conditions of this experiment.

Sulphur: All heats show sulphur pickup from the bed coke with analyses ranging at tap-out from 0.175 per cent for Heat 3 to 0.216 per cent for Heat 5 with the charge metal containing from 0.11 to 0.14 per cent sulphur.

Little effective desulphurization was obtained as the curves for all heats level out at approximately charge level. This does indicate, however, that there was some

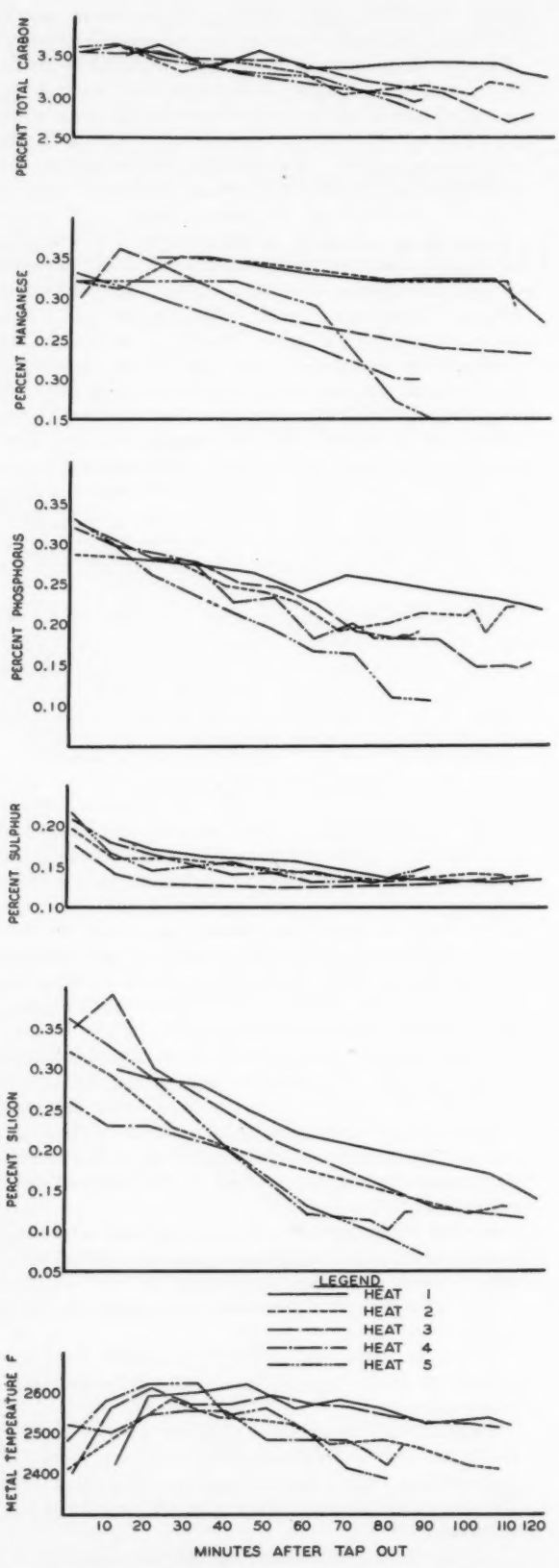


Fig. 2—Experimental results for five test heats.

desulphurization since sulphur pickup from the coke was eliminated. This may be verified by the sulphur content in the slag analyses. Of particular interest is the upswinging trend in sulphur content at the ends of Heats 4 and 5 when the metal temperatures were very low.

Manganese: At tap-out, the manganese contents ranged from 0.30 per cent for Heat 3 to 0.33 per cent for Heat 4 with the charge metal at 0.50-0.55 per cent. Heats 1 through 4 show progressively more oxidation with little difference between Heats 1 and 2. The proximity of manganese contents in Heats 1 and 2 does not preclude the assumption that the latter was under more oxidizing conditions considering that silicon, which takes precedence over manganese in oxidation,

TABLE 3 . . . SLAG COMPOSITION

Heat	Sample	CaO	MgO	Al_2O_3	SiO_2	S	P_2O_5	FeO	Fe_2O_3	MnO
1	1A04	16.50	11.18	24.38	41.70	.19	.17	2.60	.48	3.17
	1A06	24.90	10.72	21.28	37.30	.36	.50	1.80	.32	3.39
	1A08	25.65	18.20	12.50	34.60	.33	.99	3.45	.56	4.15
	1A10	26.20	9.00	22.48	34.00	.45	1.02	2.75	.56	4.10
	1A13	27.70	18.60	11.68	31.90	.55	1.07	3.75	.40	3.60
2	2A07	20.20	13.30	14.80	34.00	.28	1.84	9.07	.56	4.97
	2A09	22.80	16.50	13.00	31.24	.22	1.76	8.35	1.20	4.64
3	3A04	12.10	12.65	23.50	37.90	.12	.47	7.60	.64	4.45
	3A05	15.80	14.10	19.44	39.84	.20	.61	5.25	.32	4.48
	3A06	18.90	15.40	16.62	38.36	.19	.81	3.82	.48	4.67
	3A09*	19.15	16.38	17.20	40.46	.22	.22	1.87	.32	3.40
	3A10	21.00	15.20	13.20	20.96	.43	3.48	15.40	3.82	5.32
4	4A03*	14.30	15.30	18.30	40.80	.13	.63	4.70	.49	4-5%
	4A06*	16.60	15.85	14.87	35.80	.23	1.67	8.30	.49	on
	4A09	18.45	17.26	12.50	29.80	.36	3.18	12.50	.36	all
	4A14*	20.25	16.05	14.33	39.30	.30	.85	2.05	.30	(est.)
5	1B06*	14.25	14.60	21.30	40.40	.16	.29	3.20	.32	3.50

*Slag sample taken from metal spout after coming through tap hole.

was at a consistently lower level when comparing Heat 1 with Heat 2.

Silicon: At tap-out, silicon ranged from 0.36 per cent for Heat 5 downward to 0.26 per cent for Heat 4 while the silicon charged ranged from 0.50-0.55 per cent. Each of the first four heats generally showed progressively more silicon oxidation. The curves for Heats 2 and 3 did not assume their respective positions on the graph until well after the estimated time for equilibrium conditions to be established. However, the curves do approach each other until they cross. Heat 3 started at a higher silicon level and the over-all silicon loss was greater than in Heat 2. Heat 5 shows a consistently low silicon level with a low of 0.07 per cent at 90 minutes which condition greatly facilitates the removal of phosphorus.

In every heat the oxidation loss was greater for silicon than for manganese.

Total Carbon: The percentage scale on this curve is one-tenth the magnitude used for the other elements to avoid a prohibitively large graph.

Carbon content at tap-out ranged from 3.50 per cent for Heat 1 to 3.64 per cent for Heat 2. This generally showed little effective carbon pickup from the bed coke since the charged metal contained from 3.45 to 3.55 per cent carbon.

With a few exceptions, the carbon curves lie in the same relative positions as those for silicon at any particular time. Throughout the middle portion of the curves, as is the case for silicon, Heat 2 is at a lower level than is Heat 3 and the points at which these two curves cross is within 3 minutes of each other on the time scale.

This apparent reversal in the early portions of both the carbon and silicon curves for Heats 2 and 3 is attributed to a slightly lower dam height and consequent lower metal temperatures in Heat 2. Even though Heat 3 maintained relatively high metal temperatures in the later portion of the heat, the curves for total carbon, silicon, and manganese assume their correct relative positions illustrating that there was progressively more oxidation in each of the heats from Heat 1 through Heat 4. Again it is stated that this was the desired relationship to be obtained by decreasing the coke charge weight on each successive heat in order to observe the resultant effects upon dephosphorization.

Phosphorus: At tap-out, phosphorus percentages for all heats lie within the limits of the metal charge, 0.28-0.33 per cent. It is significant that during the period from 10 to 35 minutes after tap-out, all four curves on the progressive oxidation series lie within a spread of 0.02 per cent before they diverge while Heat 5 started at the highest phosphorus level and continued downward at the most favorable rate. In the middle portion of the heats the greater dephosphorization in Heat 2 as compared to Heat 3 may be explained by the relationship of silicon content and metal temperatures for the two heats. In this portion, Heat 2, as compared with Heat 3, had both a lower metal temperature and silicon content, both of which aid in the dephosphorization reaction.

Soon after the assumed time for equilibrium inside the cupola the phosphorus curve for Heat 3 drops below that for Heat 2 meaning greater dephosphorization at which time the silicon contents for the two heats are almost identical.

Reach Lowest Phosphorus

Of the progressive oxidation series, Heat 3 reached the lowest phosphorus level which was consistently between 0.145 and 0.151 per cent amounting to a removal of approximately 50 per cent of the phosphorus charged. This is considered to be more than a spot check since the four analyses within this minimum range were spread over a period of 13 minutes during which time approximately 350 lb were melted.

Heat 5, repeating the coke ratio used for Heat 3 and with 50 per cent more limestone, showed a consistent drop in phosphorus content ending at a low of 0.104 per cent amounting to a removal of approximately 68 per cent of the phosphorus charged. Here nearly all the metal melted after the first hour, roughly 800 lb, contained less than half of the phosphorus charged.

Heats 4 and 5 ended short of the desired operating time of two hours. With operations designed for the lowest possible fluid metal temperatures, the tuyeres finally choked off the incoming blast until the cupola froze. Punching the tuyeres to keep them open was futile as the accumulation of slag and frozen metal droplets was quite dense.

It is noted that the acid constituents of the slag are relatively high in comparison to bases and not by any accepted method of calculating slag basicity can a basic slag be assumed.

It is highly possible that these heats did not continue long enough and did not have sufficient slag fluidity to flush out the acid slag constituents resulting from the bed coke ash and oxides formed during melting before the slag hole was opened. Attempts were made to obtain slag viscosimeter readings, but the flow was never maintained long enough to take reliable readings. Consequently it is not ascertained whether or not the slag samples recorded are representative of conditions inside the cupola.

Analyses were made of slag samples taken from various locations inside the cupola and from the bottom drop, but the results were not sufficiently consistent to report.

Spectographic analyses of slag samples failed to reveal any more than very small percentages of sodium from the soda ash (Na_2CO_3) charged.

The cupola lining above the tuyeres did not burn out to any great extent during the performance of these heats. A maximum diameter of approximately 20 in. was reached after Heat 5, however patching in one localized burned area was required before Heats 4 and 5. Difficulty was experienced in chipping out the cupola below the tuyeres. The slag was extremely hard and rather than chipping clear of the lining, it appeared to be fused into the brick, breaking off with it adhering sections of brick ½ to ¾ in. thick. Figure 3 illustrates a cross section of such a fracture showing the dark porous slag area on the right fused into the lighter brick area on the left.

In an effort to simulate forehearth conditions and to study the possibility of further dephosphorization after melting, iron from the cupola heats was remelted in an induction furnace. After taking a metal sample for analysis, sodium carbonate in the amount of 2.5 per cent and iron oxide (mill scale) in the amount of 0.25 per cent of the metal charge weight were added to the crucible. After keeping the metal at as low a molten temperature as possible and allowing 10 minutes slag-metal contact, a second sample was taken for chemical analysis.

Results here indicate the feasibility of post-melting dephosphorization and verify the work previously reported by Carter.[1] It is further illustrated that dephosphorization is aided by a low silicon content. Specifically, a melt containing 0.293 per cent P and 0.48 per cent Si before the additions described above yielded a final P content of 0.222 per cent. A melt containing 0.206 per cent P and 0.03 per cent Si yielded a final P content of 0.085 after the additions.

Conclusions

Conclusions based on this study are:

1. Phosphorus can be removed in the basic cupola.
2. Phosphorus removal increases with increasing oxidation in the range covered by this study.
3. Phosphorus removal is facilitated by lower silicon content in the metal.
4. Phosphorus removal is facilitated by lower metal temperature.

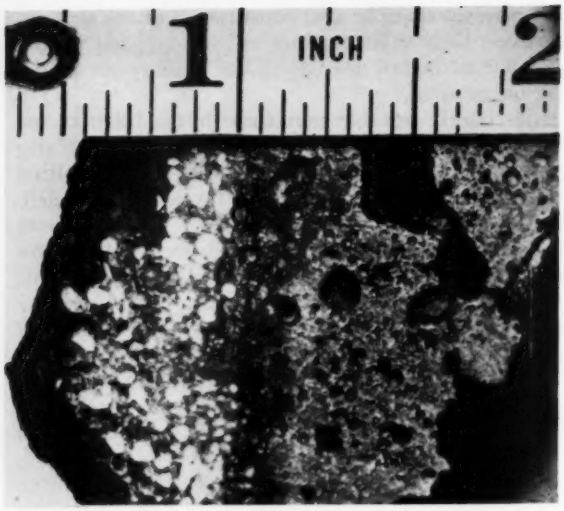


Fig. 3

5. Phosphorus removal is facilitated by a higher percentage of limestone charged in the range covered by this study.

6. Phosphorus removal is possible without excessive carbon oxidation.

7. More silicon than manganese is oxidized in basic cupola operation.

8. Limited desulphurization occurs with dephosphorization.

9. Refractory burn out is low at the low melting temperatures used, but the fusion of slag to the brick results in considerable brick loss when chipping out.

10. Ladle handling or pouring castings with the low temperature metal coming from the cupola spout is impractical. Duplexing or remelting is essential.

It seems logical to assume that in basic cupola operation using insulation between the shell and basic brick lining, and sufficient coke to maintain slag and metal fluidity, successful dephosphorization can be accomplished on a production basis.

Acknowledgments

The authors wish to express their appreciation to the students who aided in making the cupola runs, and to E. C. Wright and N. N. Engel of the University of Alabama Metallurgical Engineering Department where the work was performed. Grateful acknowledgments are also extended to R. A. Flinn, University of Michigan, and America Brake Shoe Co., Mahwah, N. J., for assistance and for the chemical analyses.

DISCUSSION

Chairman: H. A. DEANE, Campbell, Wyant & Cannon Foundry Co., Muskegon, Mich.

Co-Chairman: G. L. RICHTER, Farrel-Birmingham Co., Ansonia, Conn.

Recorder: W. M. SPEAR, Worthington Corp., Harrison, N. J.

C. L. FREAR *(Written Discussion):*[1] In the writer's opinion, this is an excellent paper, being basic in character, pointing to the economic possibility of removing one of the troublesome elements which is of rather general occurrence in gray iron. It should be an incentive for discussion with regard to the principles involved and possible developments to refine and extend the utility of the process.

Theoretically, the removal of phosphorus should be a simple matter if certain variables can be controlled, these variables being amply discussed in the present paper. However, the control of these variables appear to be rather difficult in normal cupola operation as we think of it today. The ability to remove phosphorus probably depends upon controlling the chemical reaction $4P + 5O_2 \rightleftarrows 2P_2O_5$ in such a manner that the equilibrium is displaced toward the right side of the equation. This displacement will be almost complete provided strong oxidizing conditions prevail and some means is provided for removing the phosphoric anhydride as fast as it is formed. Desirable oxidizing conditions should be aided by the use of slow burning coke and high blast volumes, or perhaps by enrichment of the blast by oxygen. On the other hand, reducing conditions which would displace the equilibrium toward the left, would be increased by fast burning coke, low blast, excessive water vapor in the air, and high silicon.

Increase in the amount of lime should aid the reaction by fixing the P_2O_5 in the form of a rather stable phosphate. For the maximum effect in this direction, it appears that a highly fluid basic slag should be present. The use of some fluorspar charged with the limestone might be a help in this direction.

It is the present writer's opinion that the dephosphorization reaction occurs high in the melting zone, where the charge first melts, and where we have the minimum amount of carbon monoxide. A perfectly fluid slag at this point would be desirable to effect complete interaction between the phosphoric oxide and the lime, and before the lime has had an opportunity to react with silica or other acid constituents. This opinion may be strengthened by the fact that the heat showing the maximum dephosphorization produced a slag very high in silica, hence acid, and theoretically incapable of uniting with additional acid constitutents, such as the phosphoric oxide.

The writer cannot agree entirely with the author in his statement that little effective desulphurization occurs as the chemical analyses show a reduction of 5 to 7 points in every case.

The commercial value of the process will depend to a considerable extent upon whether it will be more economical to dephosphorize, then duplex the metal and make up the necessary silicon content with ferrosilicon, than it would be to purchase low phosphorus pig in the first place, assuming that suitable low phosphorus pig is available in required amounts. Of course, the very low silicon content provides the maximum opportunity to obtain desired properties by innoculation. Further development may even point to the production of a cupola or similar equipment, built for the specific purpose of dephosphorizing, designed in such a manner as to minimize the difficulties reported in the present paper.

J. E. Bolt *(Written Reply to C. L. Frear's Discussion):* In reply to Mr. Frear's written discussion the writer is essentially in full agreement. It is generally agreed that the exact dephosphorization reaction has not as yet been reliably determined. Works by Winkler and Chipman* show the dephosphorization reaction to be represented by

$$2P + 5O + 4(CaO') \rightarrow (4\,CaO \bullet P_2O_5)$$

where CaO' represents free lime.

and the equilibrium constant assuming ideal solutions in the metal and slag phases to be:

$$Kp = \frac{(4CaO \bullet P_2O_5)}{[\%P]^2\,[\%O]^5\,(CaO)^4}$$

where () signify mol fractions of the slag components

[] signify per cent by weight of the metal components

It is evident from these relationships that no practical dephosphorization can be accomplished without the presence of free lime and that the influence of oxygen concentration in the metal is more powerful than that of the CaO in the slag. Values of Kp vary from 5×10^{11} at 1500 C to 4×10^{7} at 1700 C illustrating the effect of temperature on the reaction.

The writer cannot disagree with the opinion that the dephosphorization reaction occurs in the melting zone. As is recorded in the text of the paper the Na_2CO_3 did not appear to any extent in the slag even by spectographic analysis and it must be assumed that its effect, if any, took place either in the melting zone or at least above the well. Only one slag

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[1] Production Engineer, Department of the Navy, Bureau of Ships, Washington, D. C.

* T. B. Winkler and J. Chipman, "An Equilibrium Study of the Distribution of Phosphorus Between Liquid Iron and Basic Slags," *Transactions*, American Institute of Mining and Metallurgical Engineers, vol. 167, pp. 111-132 (1946).

sample taken from the wall at the melting zone was analyzed. This showed 31.7 per cent CaO, 26.8 per cent MgO, 11.4 per cent SiO_2, 13.4 per cent FeO, and 1.2 per cent P_2O_5. This single spot check is far from adequate data on which to base conclusions but coupled with other data obtained seems to indicate validity of the expressed opinion. The slag referred to by Mr. Frear was also a spot check being the only available sample for the heat.

Formation of the more stable phosphate in the melting zone would certainly be preferred and the desirability of a highly fluid slag at that location and in the well probably would dictate the next phase of study on dephosphorization in the basic cupola.

E. A. Loria:[2] First, I would like to congratulate the author for a start on a difficult problem. One realizes that the conditions for sulphur removal in a basic cupola are not generally compatible for conditions of phosphorus removal. I have two questions for speculation and possible future research.

1. Has the possibility of using a much larger coke size and increasing the air blast in the cupola to produce high FeO slags and lower basicity been considered? Viscosity of the slag increases with the lime content or basicity ratio and this procedure would produce a more fluid slag to absorb phosphorus. When operating with slags of high basicity in order to intensify carbon pickup and sulphur reduction, a high silicon loss can be expected, however. The maintenance of the coke bed throughout these experimental heats is another critical consideration.

2. Has the possibility of adding oxygen by means of a lance through the tuyeres at certain intervals been considered? I realize that it would produce a higher metal temperature which might not be desirable but, on the other hand, it would produce a more oxidizing condition which is desirable. Furthermore, when operating with low silicon content charges, it would open up the viscous slag in front of the tuyeres by producing better combustion in the cupola, thus making easier to manipulate the slag. The slag itself would be in a better physical state to absorb more phosphorus pentoxide. Also it would aid in the transfer or oxidation of the phosphorus in the iron to phosphorus pentoxide in the slag.

Despite the theory calling for strongly reducing conditions, the introduction of plant air with its 20 per cent oxygen as a carrier or pressure agent for burnt lime injections produces no detrimental effects in basic electric furnace desulphurizing of high sulphur bearing iron. The important factors are the ability to change the slag metal interface for sulphur absorption, producing a foamy, rolling slag rather than a strongly carbidic, dead slag, and the added possibility of transferring sulphur out of the bath as sulphur dioxide when air (or oxygen) is used as the lime carrier.

It might be considered unusual that dephosphorization can take place in the relatively high temperature of electric furnace steelmaking compared to cupola melting. The difference is explained by the higher oxidation level and the lower carbon content of steel. Review of the total carbon and phosphorus values given in Table 2 show that usually where the carbon was low the phosphorus was relatively low too. Progressive oxidation was playing a big part in the series of heats studied.

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[2] The Carborundum Co., Niagara Falls, N. Y.

PROPERTIES OF GRAPHITIC NICKEL ALLOY CASTINGS

By

J. T. Eash* and G. L. Lee**

ABSTRACT

Graphitic nickel containing 1-2.5 pct carbon, 2 pct silicon, 1.5 pct manganese, and 0.05-.10 pct magnesium in excess of sulfur is a readily produced casting alloy. Due to the magnesium content, the graphite occurs in spheroidal form and results in an alloy having good mechanical properties coupled with excellent resistance to galling. This material has a tensile strength of some 60,000 psi with 22 pct elongation and a fatigue ratio of 33 pct. The addition of 2-4 pct tin causes an increase in the yield strength with some loss in toughness and further improves the anti-galling characteristics.

Engineers are frequently confronted with the necessity of using a cast material that possesses both corrosion resistance and anti-galling properties. Nickel is well known for its good service where the former requirements are needed. While pure nickel does not have very good resistance to galling, it can be alloyed so as to give excellent performance in moving parts. Graphite has been used in several alloys to impart lubricating characteristics and it has the same effect on cast nickel. By controlling the amount and shape of the graphite particles and by using alloys for strengthening and improved castability, a high quality product is obtained.

Newly developed graphitic nickel castings containing 1-2.5 pct carbon, 2 pct silicon, 1.5 pct manganese and 0.05-0.10 pct magnesium in excess of sulfur can be readily produced and the effects of moderate variations in chemistry are not very pronounced. Information will be presented on the production of this alloy, and its mechanical properties and resistance to wear and galling.

Melting and Casting Practice

Most of the alloys studied in this investigation were melted in an induction furnace with a clay graphite crucible; however, a few were made in a basic-lined direct-arc furnace. Carbon was added to the charge by means of a master alloy of carbon-nickel of known analysis or in the form of graphite. Very close control of the composition was maintained with the former method by following a straight melting and deoxidation practice. When graphite was used, about 125 pct of the desired carbon was charged with electro nickel and melted down. In the arc furnace melting, a convenient method to control the higher carbon contents when adding graphite was to cover the bath with a lime-alumina slag, and by inspecting slag samples removed on an iron rod at intervals, it was possible to tell when the graphite had gone into the nickel by its absence from the slag. In some cases this required holding the melt at about 2800 F for 1 hr. With the induction furnace, the carbon content was controlled by the use of master alloys or by definite heating cycles when graphite was added.

When the carbon was in solution, about one-half the desired silicon and all the manganese were added together and stirred. Silicon and manganese metals were used in both cases. Any other desired alloy additions were then made. The heat was treated with 0.05-.15 pct magnesium as 66 pct magnesium 34 pct nickel by plunging it into the melt with an iron rod. A final addition of the balance of the silicon was made to clean the surface of the heat and improve the magnesium recovery.

A pouring temperature range of 2450 F to 2750 F depending upon the section size of the casting was found satisfactory. The castability of these nickel alloys was excellent over this temperature range. This material has a pattern shrinkage of ¼ in. per ft, the same as other high nickel alloys. Riser and gating practice should be rather generous to provide adequate feeding.

Green synthetic sand or dry synthetic sand mixes giving molds with a permeability of 50 or more work well with these alloys. High moisture content in green sand or high oil content in poorly baked cores have at times caused porosity and should be avoided.

Cast to shape tensile bars were produced for the present study using the four-bar Eash[1] pattern with the bars arranged in a square with a heavy riser at each corner and a central pouring sprue connected to each corner by diagonal runners. For a measure of soundness obtained, a bushing 2½-in. O.D. x ⅜-in. wall x 4-in. long was made and tested under air pressure of 100 psi. Other castings such as rotary pump parts, liners, gears, plungers, gate valves, and pipe fittings have been produced having sharp defini-

* Metallurgical Supervisor and **Research Metallurgist, Research Laboratory, The International Nickel Company, Inc., Bayonne, N. J.

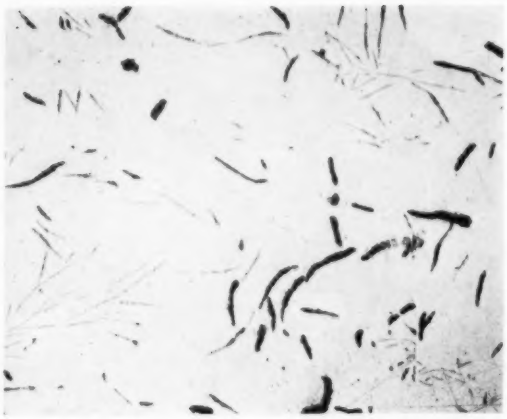


Fig. 1—Flake graphite in magnesium-free alloy No. 1.

tion and excellent surface finishes by using the practice described.

Microstructure and Tensile Properties

In magnesium-free cast nickel containing 1 pct carbon, 2.30 pct silicon and 1.5 pct manganese the graphite occurs in flake form as shown in Fig. 1. This alloy will have a tensile strength of about 35,000 psi with 8 pct elongation. According to Kihlgren[2] an increase in carbon causes a decrease in strength and ductility as shown in Fig. 2. This material is easy to cast and machine and has good resistance to galling, but it does lack the toughness and strength desired in some applications. A minor improvement is obtained by raising the manganese content. Likewise if the carbon content is lowered, better mechanical properties are obtained; however, then the anti-galling characteristics are diminished.

When magnesium is added, the graphite becomes compacted and may exist in various forms depending upon the amount of magnesium and other elements present.[3,4] With an insufficient amount of magnesium, the graphite will occur as a mixture of flake, nodules and spheroids with radial structure as shown in Fig. 3, or as clusters of nodules as shown in Fig. 4; these heats contained about 0.009 and 0.011 pct magnesium respectively. The 0.009 pct magnesium melt had a tensile strength of 39,000 psi with an elongation of 9 pct, which is some 10,000 psi higher than would be obtained had the alloy contained coarse flake such as illustrated in Fig. 1. At a magnesium level of 0.05-.10 pct in excess of sulfur, the graphite is converted predominately to the spheroidal form, Figs. 5 and 6, and tensile strengths of 60,000 psi and 25 pct elongation are obtained, as listed in Table 1.

Part of the magnesium that is added to the nickel alloys is consumed by the formation of magnesium sulfide; however, unlike its behavior in cast iron, the sulfur is not purged from the melt. Consequently the residual magnesium content includes that combined with the sulfur and that needed to produce spheroidal graphite. The alloys listed in Table 1 and described by the various figures contained very low sulfur so that the magnesium contents given are practically in excess of the sulfur. Other melts that have been made containing higher sulfur showed that to obtain the desired spheroidal graphite structure, the magnesium should exceed the sulfur content by a minimum of about 0.04 pct. It is desirable of course in the production of all nickel alloys to keep the sulfur content as low as possible.

In addition to the graphite, there is present also a beta phase having a eutectic structure, which increases with the magnesium content as shown in Figs. 7 and 8. The beta constituent forms in an interdendritic pattern and apparently contains a small amount of combined carbon, because the latter also increases with the magnesium content, as indicated in Fig. 9. It is believed that both the graphite and the beta phase have an important effect on the wear resistance of the alloys as will be described later.

Comparison of Figs. 2 and 10 shows that the presence of magnesium in cast nickel permits greater tolerance in carbon content; for with the graphite in spheroidal form, the carbon may vary from 1 to 2.5 pct and have little effect on the mechanical properties. Spheroidal graphite has been obtained with magnesium contents as high as 0.5 pct; however the strength and particularly the ductility fall as shown

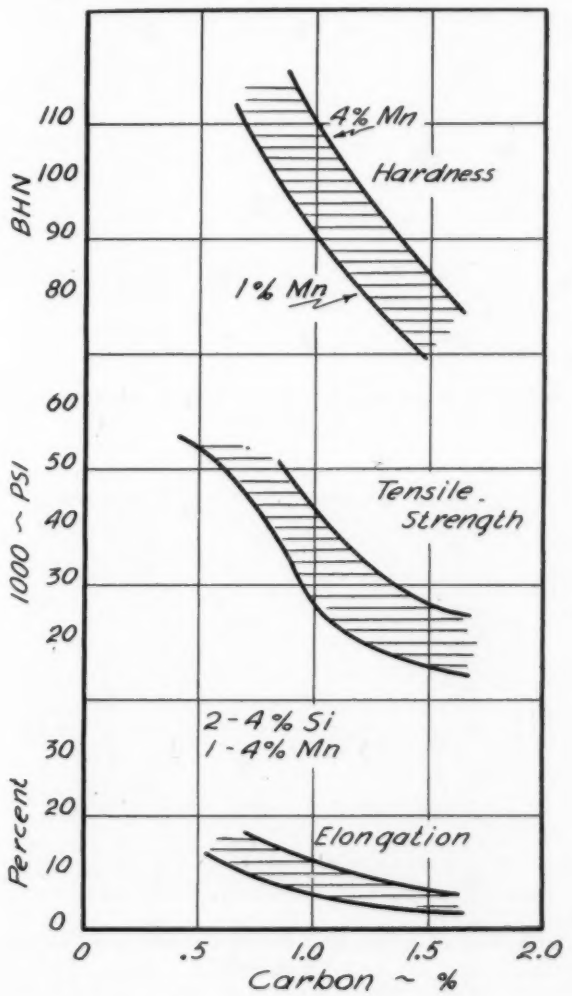


Fig. 2—Effect of carbon on properties of flake graphite nickel (Kihlgren).

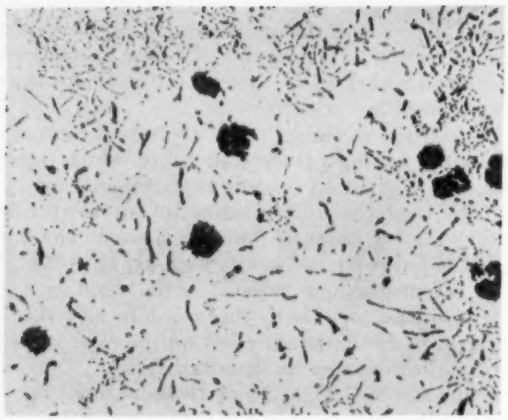


Fig. 3—Flake, small nodules, and spheroids in alloy No. 3 containing 0.009 pct Mg. Cyanide persulfate etch. 100x.

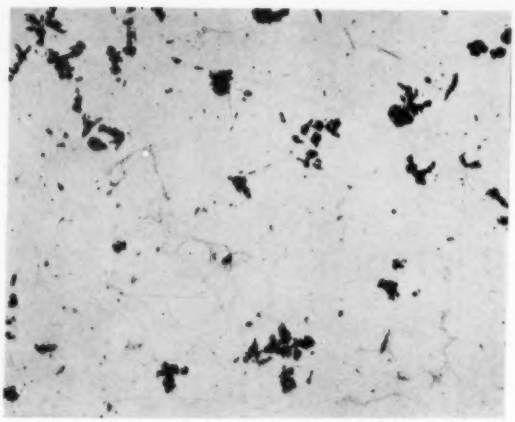


Fig. 4—Nodular graphite in nickel alloy No. 4 containing 0.011 pct Mg. Cyanide persulfate etch. 100x.

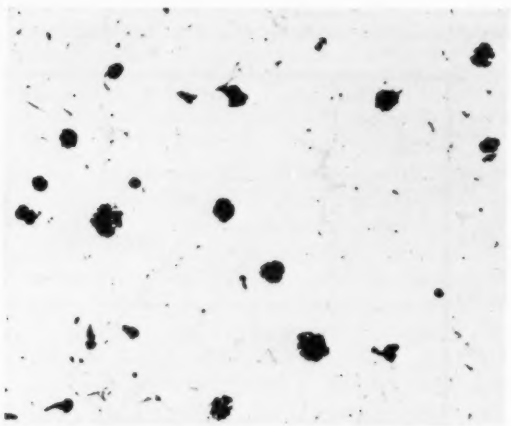


Fig. 5—0.06 pct Mg alloy No. 5. Cyanide persulfate etch. Mag. 100x.

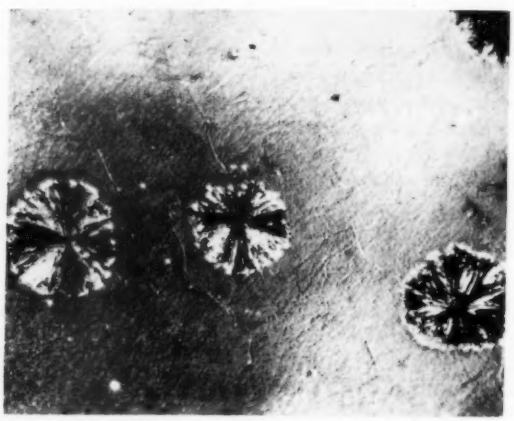


Fig. 6—Radial structure of spheroids. Shown with polarized light. Mag. 500x.

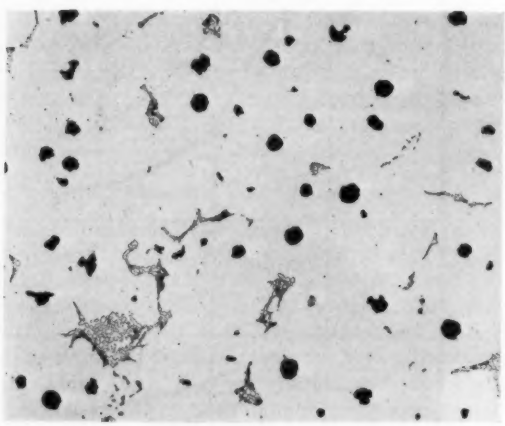


Fig. 7—0.16 pct Mg alloy No. 11. Cyanide persulfate etch. Mag. 100x.

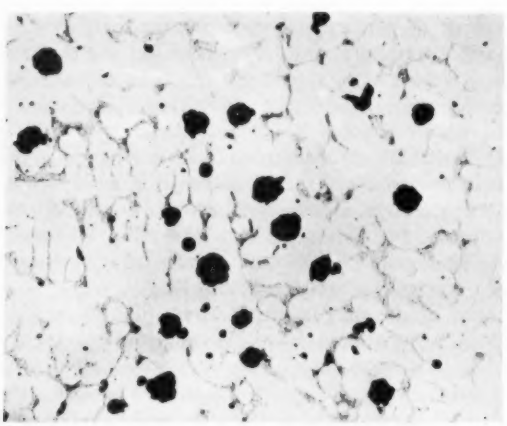


Fig. 8—0.50 pct Mg alloy No. 16. Cyanide persulfate etch. Mag. 100x.

Figs. 5-8—Spheroidal graphite structures in graphitic nickel castings of variable magnesium content.

Table 1—Properties of Graphitic Nickel Alloys

No.	Chemical Analysis TC	CC	Si	Mn	Mg[3]	Sn	Mechanical Properties BHN	Yield,[1] psi	Ultimate, psi	Elong., %	Charpy Impact,[2] ft-lb	Graphite Structure
							Magnesium Free					
1	1.15	.09	2.32	1.64	—	—	90				13	Flake
2	.92	.06	2.23	1.41	—	—	92	19,800	37,800	12	10	Flake
							Very Low Magnesium					
3	1.53	.07	2.42	1.62	.009	—	107	24,400	39,700	7	9	Flake+Nodules+ spheroids.
4	1.47	.07	2.05	1.32	.011		108					Nodular
							0.06-.18% Magnesium					
5	.90	.15	2.31	1.32	.063	—	110	24,900	60,750	27		Spheroidal
6	.96	.20	1.92	0.95	.14	—	118	29,300	60,000	22	28	"
7	1.89	.33	2.03	1.59	.10	—	114	25,800	60,000	27		"
8	1.94	.09	1.95	1.65	.09	—	112	25,700	58,500	22		"
9	1.95	.16	2.07	1.56	.10	—	116	25,800	58,000	25	26	"
10	2.30	.24	1.89	1.49	.14	—	124	26,700	54,000	15	26	"
11	2.45	.26	2.01	1.60	.16	—	118	26,300	56,500	19	27	"
							0.18-.50% Magnesium					
12	1.61	.34	2.04	1.53	.18	—	122	28,000	60,000	19	28	Spheroidal
13	1.57	.45	2.02	1.54	.26	—	128	29,100	59,650	18	26	"
14	.96	.30	1.16	.89	.20	—	122	27,600	57,300	16		"
15	.96	.29	1.94	.90	.37	—	130	28,000	58,750	8		"
16	1.59	.32	2.28	1.35	.50	—	148	31,800	44,000	4		"
							Low Silicon and Manganese					
17	1.05	.30	.72	.47	.21	—	122	26,200	54,200	17		Spheroids+ nodules.
18	2.30	.35	(.25)	1.38	.10	—	120	25,100	35,300	5		Spheroidal
19	2.24	.47	(.50)	1.38	.08	—	124	26,600	41,800	11		Nodules+ spheroids.
							Tin-Containing					
20	.76	.12	2.02	1.37	.10	1.20	118	31,000	46,500	9	36	Spheroidal
21	.98	.14	2.38	1.41	.06	2.56	146	36,400	65,000	14	21	Spheroids+ Nodules.
22	.89	.10	2.02	1.34	.04	3.91	168	47,300	67,300	10	18	Nodules+flake.
23	(1.0)		(2.)	(1.5)	(.15)	(4.)	150	44,800	80,300	16		Spheroidal
24	.91		(2.)	(1.5)	(.15)	6.30	152	48,800	75,000	18	12	"
25	.86		(2.)	(1.5)	.14	8.34	193	58,000	81,100	11	5	"
26	.87		(2.)	(1.5)	.23	10.16	209	63,000	81,300	2	4	"

( ) indicates amount added.
(1) Yield determined at 0.5% extension under load.
(2) V-notched Charpy specimens used.
(3) The sulfur contents of these alloys were very low, consequently the magnesium values quoted were practically in excess of sulfur.

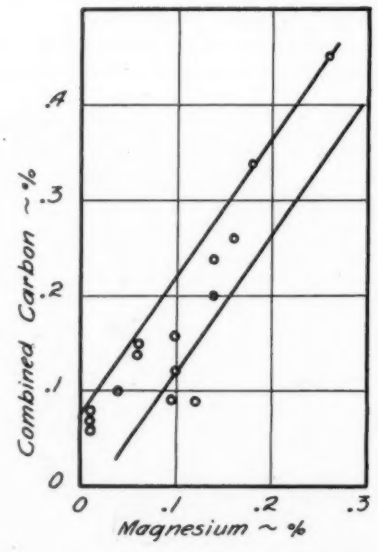


Fig. 9—Effect of magnesium on combined carbon content.

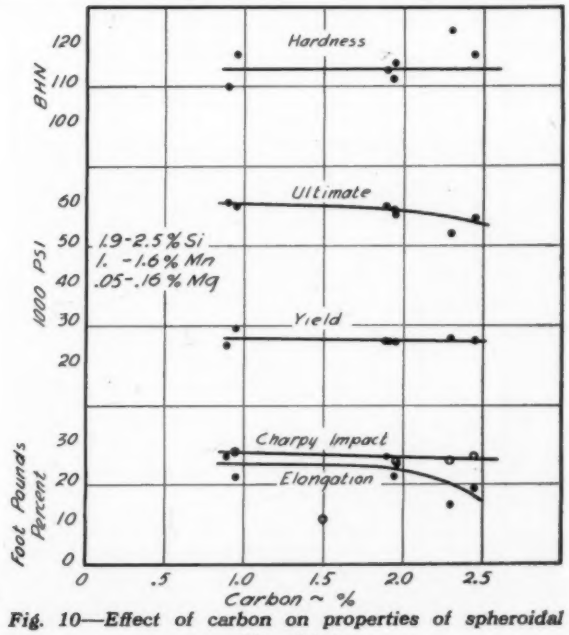


Fig. 10—Effect of carbon on properties of spheroidal graphite nickel.

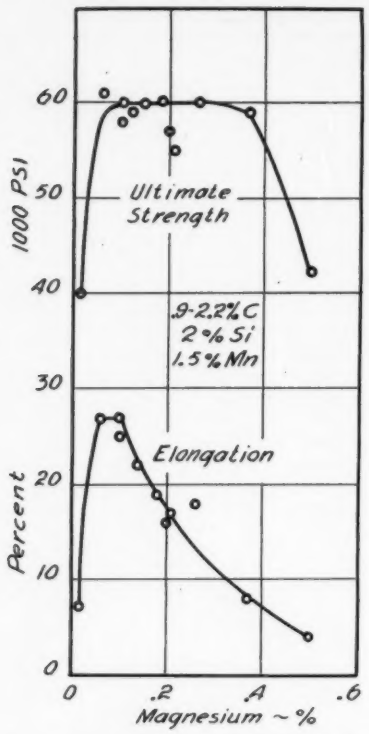


Fig. 11—Effect of magnesium in excess of sulfur on tensile properties.

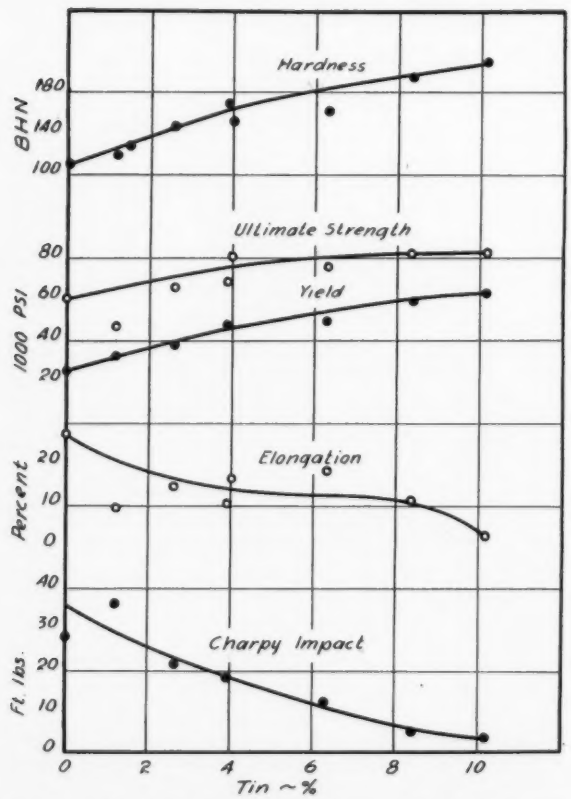


Fig. 12—Effect of tin on properties of 1 pct carbon cast graphitic nickel.

in Fig. 11 due to the intergranular beta phase. For best results the magnesium should be held between 0.05 to 0.10 pct in excess of sulfur with higher magnesium being more desirable at the high carbon contents.

Silicon and manganese have a strengthening effect in the graphitic nickel. An increase from 0.7 pct silicon 0.5 pct manganese to 2.3 pct silicon and 1.3 pct manganese raised the strength from 55,000 to 60,000 psi as shown by melts 17 and 5 in Table 1. These elements do have a marked effect on castability as they are of considerable aid to fluidity. In heats that had been made with just carbon and manganese, the drossing tendency of the latter was harmful and a silicon content in excess of 0.5 pct was needed to overcome this effect as shown in melts 18 and 19.

All of the melts containing over 0.9 pct carbon, 1 pct manganese, 1 pct silicon and .05-.15 pct magnesium gave pressure tight bushings when poured in the range of 2650-2750 F.

The properties of graphitic nickel can be further improved by the addition of tin which has a hardening effect on the matrix.[5] Figure 12 shows that the yield and ultimate strengths increase progressively to 10 pct tin in the 1 pct carbon alloys; however, the ductility decreases appreciably at the greater amounts. The tin also causes the beta phase to occur as a more massive constituent instead of as an eutectic. Figure 13 shows the microstructure of a typical tin-containing alloy.

Impact Resistance

Impact tests were made using V-notched Charpy specimens which showed the flake graphite nickel castings to have 9-13 ft-lb impact resistance as compared to 26-28 ft-lb for the spheroidal graphite nickels. Variation in per cent carbon has little effect on toughness, as shown in Fig. 10.

Fatigue Strength

The endurance limits of these alloys were determined on R. R. Moore rotating beam machines using both smooth and notched specimens. The smooth bars were machined to a radius of $3\frac{53}{64}$ in. and a minimum diameter of 0.300 in. The other specimens

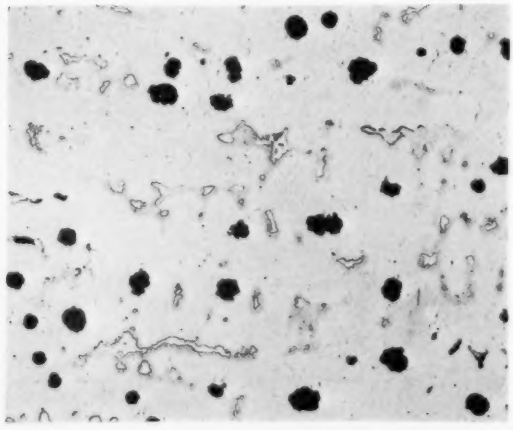


Fig. 13—Structure of 4 pct tin graphitic nickel alloy No. 23. Cyanide persulfate etch. Mag. 100x.

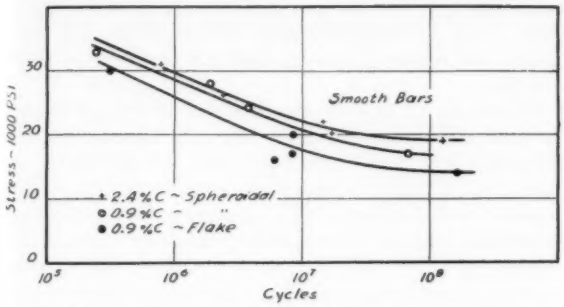


Fig. 14—Fatigue strength of graphitic nickel castings.

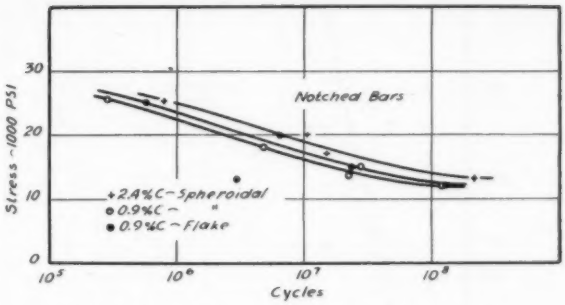


Fig. 15—Fatigue strength of graphitic nickel castings.

had a square notch ¼ in. wide reducing the section from ½-in. maximum diameter to 0.300-in. diameter at the base with a fillet of 0.002-in. radius.

The results of the fatigue tests are shown in Figs. 14 and 15. The smooth bars with spheroidal graphite exhibited an endurance limit of about 20,000 psi and the flake graphite specimens about 14,000 psi. For the square notched samples, those with spheroidal graphite had a fatigue limit of about 13,000 psi and the flake graphite type 12,000 psi indicating a greater notch sensitivity for the higher strength material.

Gall Resistance

When one undertakes the problem of determining the wear or gall resistance of a metal, he is immediately confronted with the question of the kind of test to be used. Due to the complex nature of the factors affecting wear, there is no single test applicable to all conditions. The best that can be done is to employ a method simulating the specific conditions of service, and some say that the final answer can be obtained only by an actual field test.

In making studies of the anti-galling characteristics of numerous alloys without any particular application in mind, several methods of test have been tried. One that we have found to be the most useful in distinguishing between alloys whose gall resistance is not of the highest order is known as the plunger test. Results from this method have been found to agree with the conclusions derived from other procedures and with service experience.

The plunger test involves the use of three flat specimens pressed together under a known side load in a fixture and moving the center specimen between the two outer ones. The load required to move the sliding sample between the two fixed specimens under a preset side load is used as a measure of galling tendency. The fixture and specimens used are shown in Fig. 16. In conducting the test, the fixed side specimens A, ¼ in. x ½ in. x 2½ in., were carefully degreased and placed in slots in the upper and lower plates B of the degreased fixture. The movable specimen C, ¼ in. x ½ in. x 1 in., was also degreased and sandwiched between the two fixed specimens perpendicular to them, permitting about ½ in. travel of the movable specimen through the fixed specimens. This results in a contact area of ½ in. square or ¼ sq in. The nuts D on the spring studs, E, were closed finger-tight to hold the specimens in position during preloading. The partly assembled fixture was placed in the Amsler machine and the springs, F, loaded to a predetermined value by pressure on the loading plate, G. The nuts, D, were tightened finger-tight under load. The Amsler load was then released. In making the test, the preloaded fixture was laid on its side on the Amsler and the movable specimen pushed through the side specimens by means of a short ram, H. The displacement was read on the dial indicator, J, attached to the Amsler, and the load at various displacements was read on the Amsler dial, K. The specimens were finished by grinding in the direction parallel with the direction of motion in the test, using a carborundum wheel No. AA-46-H7-V-10, 10-in. diam x ¾ in. wide. Grinding was done dry using a 0.0005-in. cut at 60 ft/min and about 0.032-in. transverse feed per stroke.

Specimens of the same material were usually run against each other as this has been found to give a good indication of the anti-galling properties of an alloy. When dissimilar metals are mated together, the characteristics of the poorer usually predominate.

In conducting the tests to be described, the fixture was assembled under a side load of 250-1000 lb (1000-4000 psi) and the load-displacement curve obtained for a motion of about ½ in. The slope of the curve gave a relative measure of the gall resistance of the alloy. The appearance of the specimens was studied

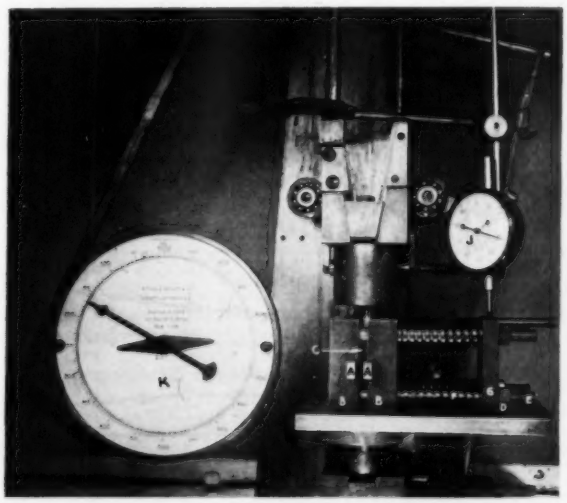


Fig. 16—Plunger test apparatus.

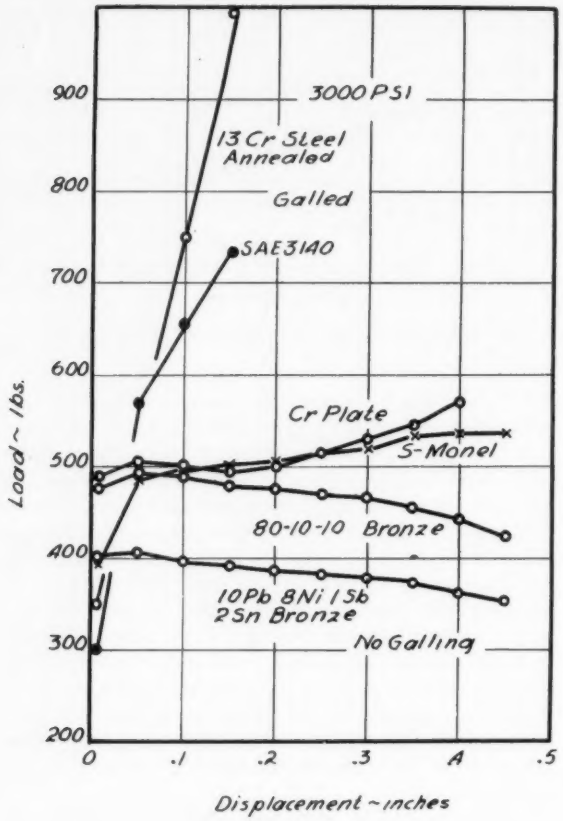


Fig. 17—Plunger gall tests on known alloys.

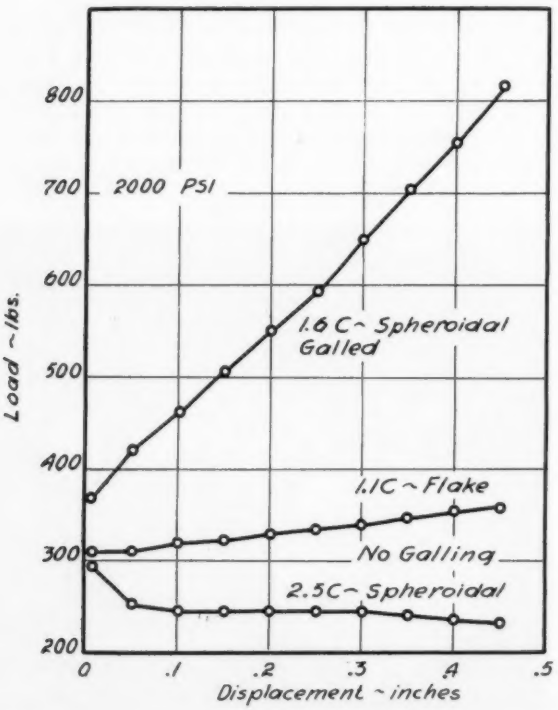


Fig. 18—Plunger gall tests on graphitic nickel.

after the test and in general severely galled samples had steep load-displacement curves while the best alloys for resisting galling had flat or negative slope curves.

Examples of the results obtained with several well known alloys are shown in Fig. 17. Leaded bearing bronze, chromium electroplate and cast silicon monel are used in many commercial applications requiring resistance to galling. The plunger test shows these materials to have flat load displacement curves. On the other hand, 0.40 pct carbon steel at 300 Brinell hardness and annealed 13 pct chromium steel are reputed to have comparatively lower gall resistance. The plunger curves for these latter metals have steep slopes and the test specimens showed considerable galling.

With the addition of carbon to cast nickel, its resistance to galling is markedly improved due to the lubricating action of the graphite; however, control of amount and form of the latter is important. The results of plunger tests in Fig. 18 show that a greater amount of spheroidal graphite is needed than flake to obtain comparable anti-galling properties. The flake graphite is more widely dispersed and has a greater surface area and thus provides more uniform lubrication. Since the mechanical properties of the nodular graphite nickels are not affected appreciably by variations in the carbon between 1 and 2 pct, there is no objection to the use of 2-2.5 pct carbon to obtain superior resistance to galling.

The gall resistance of the tin-containing alloys is

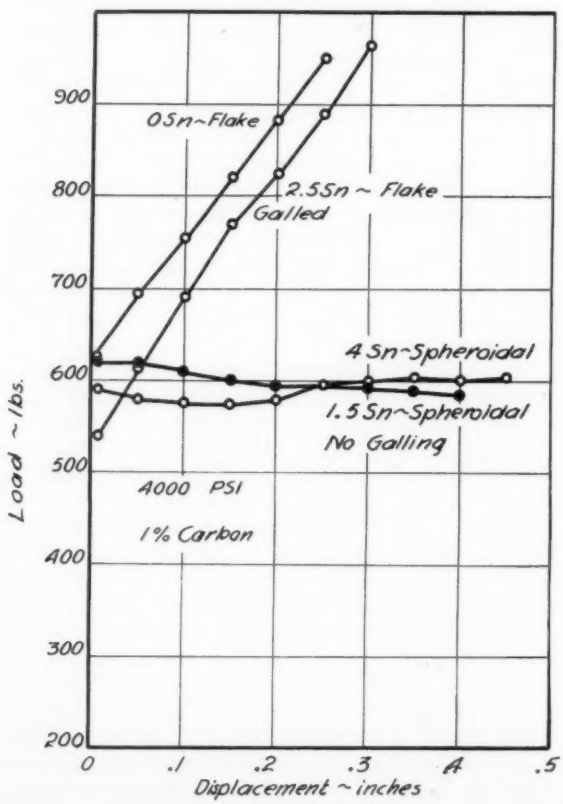


Fig. 19—Gall tests on tin-containing graphitic nickel.

Table 2—Amsler Wear Tests

Mating Materials		Total	440 RPM Disc		400 RPM Disc		
440 RPM Disc	400 RPM Disc	Revolutions	Diameter Change, in.	Weight Change, Mg	Diameter Change, in.	Weight Change, Mg	Appearance of Discs
Tin Graphitic Ni	Steel	1,959,500	—.0001	— 2.8	0.0	—27.1	Smooth
2.5% C Graphitic Ni	Steel	1,649,300	0.0	+ 2.0	0.0	— 1.4	Smooth
Tin Graphitic Ni	Tin Graphitic Ni	2,009,600	—.0003	— 4.9	— .0008	—16.9	Smooth
Tin Graphitic Ni	S-Monel	1,924,900	—.0003	—104.6	— .0002	—23.8	Smooth
Tin Graphitic Ni	Nickel	280					Galled

	Nominal Compositions											
Name	C	Si	Mn	Sn	Al	Mg	Fe	Cu	Ni	Cr	Condition	BHN
Tin Graphitic Nickel	1	2	1.5	2.5	—	.06	—	—	Bal.	—	Cast	145
2.5% C Graphitic Ni	2.5	2	1.5	—	—	.06	—	—	Bal.	—	Cast	118
S-Monel	.09	4	0.9	—	—	.05	1.5	30.3	62.5	—	Cast	340
Nickel	.09	.02	.18	—	—		.15	.10	Bal.	—	Wrought	100
3140 Steel	.39	.20	.65	—	—	—	Bal.	—	1.25	0.60	Wrought	330

exceptionally good. In the plunger test under a load of 4000 psi, the 1.5 and 4 pct tin alloys were resistant to scoring and galling and had flat loading curves similar to the bearing alloys as shown in Fig. 19.

The data in Figs. 18 and 19 show that the combination of spheroidal graphite and tin are very beneficial in producing a nickel alloy resistant to galling under the higher bearing loads. The 1 pct carbon flake graphite nickel was resistant to galling at 2000 psi; however, under 4000 psi it gave poor results, and even the addition of 2.5 pct tin to this composition was not beneficial. Likewise a tin-free 1.6 pct spheroidal carbon nickel was not satisfactory. But with 1.5-4 pct tin and 1 pct spheroidal carbon together, a gall resistant alloy was obtained. The combination of a strong and ductile matrix, harder beta particles and graphite lubricant existing in this material gives the requirements for good anti-galling properties.

Amsler Wear Tests

Additional information regarding the wearing characteristics of these alloys was obtained from a series of Amsler wear tests made under lubricated conditions. The procedure consisted of rotating together on their peripheries two discs 2.325 in. diam x 0.394 in. wide under a pressure of about 18,000 psi. The graphitic nickel was placed on the shaft of the machine rotating at 440 rpm while the mating specimen on the other shaft rotated at 400 rpm. The contacting surfaces moved in opposite directions. A turbine oil (specification TS-916) having a Saybolt viscosity of 305 at 100 F was used as a standard lubricant. Periodic inspections and measurements of the disc diameter were used for determining the amount of wear. A fine machine finish was placed on the nickel alloys, but specimens of SAE 3140 steel hardened to 330 Brinell were ground with a Norton 38120-J wheel. The test results are summarized in Table 2.

The tin-containing graphitic nickel in contact with itself and S-Monel performed excellently and showed very little wear. When run against annealed wrought nickel, seizure occurred quite rapidly, which demonstrates the disadvantage of a soft homogeneous metal in applications involving rubbing contact. Both the tin-free and tin-containing alloys were equally satisfactory against hardened steel.

These tests confirm the indications obtained from the plunger method experiments. One can expect both types of graphitic nickel casting alloys to have generally good resistance to galling; however, it is believed that the tin-containing material will be able to withstand greater bearing pressures due to its higher yield strength.

Conclusions

1. Cast nickel containing 1-2.5 pct carbon, 2 pct silicon, 1.5 pct manganese and 0.05-.15 pct magnesium in excess of sulfur has good mechanical properties and excellent anti-galling characteristics.

2. The above alloy has a tensile strength of about 60,000 psi with an elongation of 22 pct. Since the graphite is present in spheroidal form, moderate variations in analysis have a minor effect on the mechanical properties.

3. The addition of tin to this material causes an increase in the strength, a decrease in toughness, and an increase in resistance to seizure on rubbing contact.

4. The tin-free alloy should contain 2-2.5 pct carbon to obtain the best anti-galling properties.

5. An optimum combination of mechanical properties and gall resistance is obtained in a tin-containing nickel composition of 1 pct carbon, 2 pct silicon, 1.5 pct manganese, 2.5-4 pct tin, and 0.1 pct magnesium in excess of sulfur.

6. These alloys can be easily cast into pressure tight shapes having sharp definition and good surface quality.

Acknowledgment

The authors wish to thank Mr. N. B. Pilling, Director of the Research Laboratory, for his support in conducting this research and permission to publish the results.

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4. H. Morrogh and W. J. Williams, "Graphite Formation in Cast Irons and in Nickel-Carbon and Cobalt-Carbon Alloys," *Jnl. Iron and Steel Institute*, vol. 155, p. 321 (1947).

5. J. T. Eash and G. L. Lee, "Graphitic Nickel Tin Alloy and Method of Making Same," U. S. Pat. 2,568,014.

DISCUSSION

Chairman: F. L. RIDDELL, H. Kramer & Co., Chicago.

Co-Chairman: J. G. DICK, Canadian Bronze Co., Ltd., Montreal, Canada.

Recorder: W. L. RUDIN, Elesco Smelting Corp., Chicago.

WM. HORAN: [1] What advantages has this alloy over 'S' Monel and what type fracture does it exhibit?

MR. LEE: Both 'S' Monel and the graphitic nickels are excellent alloys for their corrosion and galling resistance. 'S' Monel is of course a much harder alloy, but the graphitic nickel alloys can be used where those containing copper cannot, such as in handling caustic and certain foods. The fracture is a dull gray color. With higher magnesium contents it exhibits a cup-type fracture and with low magnesium it looks about like gray iron.

B. A. MILLER: [2] What are the shrinkage characteristics as compared to 'S' Monel or Monel?

MR. LEE: These alloys have a lower solidification shrinkage due to the higher carbon content, than the Monels, but the pattern or lineal shrinkage is the same at 1/4 in. per ft. Castings made with these alloys must be fed generously.

G. H. BRADSHAW: [3] Did you use gas or oil type furnaces in making these alloys?

MR. LEE: We made a few heats in an oil type crucible furnace and found fairly short crucible life, a thing that most foundrymen complain about due to the quality of the crucibles and the high temperatures needed. High quality carbon bonded crucibles are advocated.

CHAIRMAN RIDDELL: What is the effect of pouring temperature on pressure tightness?

MR. LEE: All of the bushing type castings poured in the range of 2750-2450 F were pressure tight. A few poured between 2450 and 2350 F had laps and folds on the surface but were pressure tight on test.

G. P. HALLIWELL: [4] The amount of galling and the per cent of carbon seems to be inconsistent in the chart shown in Fig. 18.

MR. LEE: A 1 per cent level, the flake graphite alloys have better gall resistance than those with spheroidal graphite due to the wider distribution of the flakes; however, by increasing the amount of spheroidal graphite an alloy of superior gall resistance is obtained. For example the 1.6 per cent carbon spheroidal graphite alloy in Fig. 18 was inferior to the 1 per cent carbon flake graphite alloy, but on increasing the carbon to 2.5 per cent in the spheroidal form excellent gall resistance was obtained.

MR. HALLIWELL: Does graphite or magnesium impart the anti-galling?

MR. LEE: The graphite is responsible for the non-galling and the magnesium is responsible for the distribution of the graphite. Undoubtedly the beta phase resulting from the magnesium content also contributes to the anti-galling properties.

J. T. MACKENZIE: [5] What is the weldability of this material?

MR. LEE: A limited amount of experience has been obtained on the welding of this material with satisfactory results. Based on these tests, no particular difficulty is expected.

E. F. TIBBETTS: [6] What about the magnesium additions on remelting?

MR. LEE: Since only a small amount is recovered on remelting, a full addition is made on each melt or remelt.

---

[1] American Crucible Products Co., Lorain, Ohio.
[2] Baldwin-Lima-Hamilton Corp., Eddystone, Pa.
[3] U. S. Naval Shipyard, Philadelphia.
[4] H. Kramer & Co., Chicago.
[5] American Cast Iron Pipe Co., Birmingham, Ala.
[6] Wollaston Brass & Aluminum Foundry, Hingham, Mass.

WAGE INCENTIVE FOR COST AND PRODUCTION CONTROL FOR INDIRECT OPERATORS

By

C. J. Pruet*

In this paper the application of wage incentives to direct production operations has been purposely omitted. Only the application of a complete indirect wage incentive plan for cost, quality and production control is considered.

Definition of Indirect Work

Indirect Work can be defined as that type of work which is required to indirectly assist, or service the employees who perform direct work, for example: an oiler, sweeper, repair man, janitor, clerk, stock man, supervision, etc.

The Need for Indirect Wage Incentives — With more foundries becoming highly mechanized and with rising labor costs, it is becoming increasingly important for foundry management to give more of their time and attention to their maintenance and other indirect operations. It must be realized that any deficiencies on the part of indirect workers will lower the efficiencies of direct workers, if not stop them completely.

Years ago management was content to apply wage incentives only to such operations as molding, core making, etc. However, with the rapid trend toward mechanization, there has been a tremendous increase in the importance and number of indirect operations. As a result of these technological improvements modern foundry management demands a cost and production control incentive plan which can be applied effectively to all types of operations. Furthermore, enlightened production management demands an incentive plan which provides a common yardstick by which it can compare accurately the performance of all indirect and direct operations within the company.

One of the best known means of reducing indirect labor costs, which in many cases is 50 per cent or more of the total labor cost, and at the same time improve the quantity of indirect service and maintenance is by the application of a sound wage incentive plan. The establishment of task standards for this type of work is a difficult task; however, experience has shown that the benefits are worth the extra trouble.

A sound indirect wage incentive plan for cost and production control is one which will provide management with a yardstick by which they can control indirect labor costs and services by more effective utilization of indirect man-hours, preventive maintenance, and supervision. This indirect incentive plan must improve the performance of both indirect and direct operators by reducing delays caused by breakdowns, faulty operating equipment, waiting for material, etc. Obviously the indirect incentive must be tied in very closely with direct operations and performances. For modern cost and production control purposes it is an important part of the direct incentive plan.

The indirect workers and supervision have an opportunity to increase their earnings by giving better than standard performance and service to the direct operations. This increase in earnings will eliminate the dissatisfaction caused by the difference in take-home pay between production incentive workers and indirect workers who have not had the opportunity to earn incentive pay in the past.

One Type of Indirect Wage Incentive Plan Designed for Cost and Production Control — This incentive plan is an entire production and cost control system. More benefits and cost reduction are accomplished by the control features than from the incentive features. The incentives feature is the oil which is added to make the production, costs and quality controls work smoothly and with real effectiveness.

This incentive plan was first applied in the writer's plant in 1937 in production departments. All direct and indirect operations in these departments were covered by the plan. This resulted in a substantial increase in wages for the employees and a decrease in unit costs. In 1949, this plan was extended to our machine shop and general plant maintenance groups. In these groups there are normally about 75 employees. The result of this application was a 15 per cent savings of the entire maintenance payroll after the payment of premium earnings. This was achieved by the elimination of overtime which was

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about 30 per cent of the maintenance payroll.

The incentive plan is a standard minute plan. The merit of the plan lies entirely in the method of keeping track of work, delay time and performance of the various departments. The work to be performed is expressed in so many units of time, in this case a minute, becomes a common denominator for all performance in the company. Standard time or unit value per piece is derived from time study analysis.

Work Units Defined

The number of standard minutes, or work units, produced per hour is the rate at which the operator has performed. For example, if an operator produced 60 work units (standard minutes) per hour, he has performed a 60 work-hour or 100 per cent efficiency.

A standard indirect crew for a given operating condition and level of production is established. The required units of work and labor costs for the various classifications of this indirect crew is budgeted against the expected units of work and requirements of the direct operations. Therefore, the direct operations must be measured and incentive applied to them first.

It is then an easy matter for the supervision to schedule the proper man-hours of indirect labor for any amount of direct production or equipment to be serviced, as he has a common denominator for determining the proper ratio and amount of indirect labor of the various classifications for the requirements of the various direct operations.

Where quality and yield is important, the incentive earnings may be partly based on a percentage basis. That is, if a yield of 90 per cent is the expected amount from the raw materials used. The indirect crew can earn extra pay for increasing the yield above 90 per cent. If a scrap loss of 5 per cent is the expected or inherent loss for a certain operation or process, the crew can earn extra pay by holding the loss below 5 per cent. The correct percentage figure can be determined by statistical quality control analysis.

Basis for Supervisory Incentive

A part of the supervisory incentive is based on the level of performance of both the direct and indirect operators in the department. Lost time, rework, excess handling, overtime increment, and other excess costs over which the supervision has responsibility and control are charged against the supervision incentive. All supervisory and indirect operator incentive earnings are based upon the performance of the entire department both indirect and direct. This includes quality, quantity, cost of lost time, overtime increment, rework, excess handling, etc.

If the supervisory and indirect operator incentive plan is really effective, the incentive earnings for a really good performance must be sufficiently high to permit a bite in the take home pay of a poor performance to be really felt. Operators and supervisors cannot be expected to consistently give their best performance unless the reward is ample, tangible, fair.

Procedure of Applying Indirect Incentive

In a typical department, method and time studies are made on all indirect operations to determine the standard crew required and the most practical work load for each member of the indirect crew. The studies must be long enough in time, to cover average conditions.

Summary of Time Study Made of Harness Tester—8-hr Study

Operation	*Total Work Units*
Test Pipe Harness	82. W. U.
Record data	38. W. U.
Relief duties (3 other workers for lunch)	90. W. U.
Total	210. W. U.

Idle time during study 291 minutes
210 ÷ 8 = 25 work-hour or 42 per cent of a fair days work.

Summary of Time Study Made of Pipe Weighman—8-hr Study

Operation	*Total Work Units*
Roll and Weigh Pipe	204. W. U.
Record Weight	47. W. U.
Stencil Pipe	72. W. U.
Total	323. W. U.

Idle time during study 254 minutes
323 ÷ 8 = 40 work hour or 67 per cent of a fair days work.

Summary of Time Study Made of Pipe Inspector—8-hr Study

Operation	*Total Work Units*
Inspect Pipe	157. W. U.
Supervise Workers on line	6. W. U.
Total	163. W. U.

Idle time during study 333 minutes
163 ÷ 8 = 20 Work-Hour or 33 per cent of a fair days work.

Work load for one operator performing all three operations after the method of stenciling and recording was improved

Operation	*Total Work Units*
Roll and Weigh Pipe	217. W. U.
Record Number of Pipe and other data	50. W. U.
Stencil Pipe	22. W. U.
Inspect Pipe and Test Hardness	298. W. U.
Supervise Workers on Line	6. W. U.
Total	593. W. U.

593 ÷ 8 = 74 Work-hour or 123 per cent of a fair days work.

NOTE: Since 2 men were removed from the job there is now 60 minutes less relief work. The remaining 30 minutes relief work was given to another operator. By combining the jobs there was a savings of over 14 man-hours per day. The remaining operator can now earn 23 per cent more pay for the same number of pipe put through the line.

On many other indirect operations, it will be found that incentive rates can be applied in exactly the same manner as any typical direct operations. Such operations would be trucking pipe from one runway to another, hauling sand from bin to molder, etc.

On other indirect operations, because of the extreme variations it is difficult to measure the work and establish performance standards. This is the type of indirect work we will discuss.

The next step is to establish an indirect budget. This budget consists of an allowance of a specific amount of indirect service for a specific amount of direct production and for a specific operating condition. It must be emphasized that this budget must be kept current because no incentive plan is better than its maintenance. This budget is based on data obtained from time-study analysis of all indirect and

SUPERVISOR _____ WEEKLY ANALYSIS SHEET DEPARTMENT _____

WEEK ENDING	EMPLOYEES		DIRECT LABOR SUMMARY							INDIRECT LABOR						EFFECTIVENESS		UNMEASURED WORK			TOTAL LOSSES
	Total	Below 60 Work Hour	Hours on Work Values	Lost Hours: Dept. Resp.	Lost Hours: Dept. Not Resp.	Total Hours	Direct Work Units	Penalty Units	Net Total Work Units	Direct Work Hour	Total Hours	Total Indirect Work Units (.165)	Indirect Work Hour	Total Work Hours	Cost Per Standard Dollar	Dept.	Supt.	%	Hours	Cost of Lost Hours	
	60	1	1925	15	20	1960	150,700		148,300	76	400	24,866	62	73	1.05	70		2	40	$ 54.50	$ 212.07
						1940				77				74	1.04		71				182.41
	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20	21

WEEK ENDING	DIRECT LABOR: Earned	DIRECT LABOR: Unearned	Indirect Labor	Total Pay Roll	Std. Direct Cost	COST PER 1000 WORK UNITS: Direct	Indirect	Total	ANALYSIS OF INDIRECT HOURS														TOTAL	
						25.47	3.50	28.97	Actual	Allowed .204	Actual	Allowed .612	Actual	Allowed .408	Actual	Allowed .219	Actual	Allowed .219	Actual	Allowed .204	Actual	Allowed .204	Actual	Allowed 3.070
	3841.65	60.00	814.08	4715.73	25.47	25.90	4.50	30.40	40	31	120	92	80	61	40	33	40	33	40	31	40	31	400	3.12
	2990.00		678.40	3728.40		25.70		30.20																
	22	23	24	25	26	27	28	29	30	31	32	33	34	35	36	37	38	39	40	41	42	43	44	45

Fig. 1—Weekly Analysis Sheet

production operations. To establish this budget, a factor or ratio, is calculated by which the weekly earned allowance of standard indirect work units (standard minutes) of work, the standard indirect cost, and standard allowed hours, for the various indirect classifications is determined.

It must be emphasized again that this ratio must be realistic and have real meaning. For example, if it is determined that 8 man-hours of maintenance work is actually required to properly service a production unit for a given level of production this is a true ratio. However, on the other hand, incentive for washing windows probably could not be properly applied on a ratio basis. The reason being that the amount required of man-hours per week for washing windows will vary little regardless of the level of production by the direct operators.

The window cleaning operation for incentive wage payment purposes would be applied in the same manner as a direct production operation i.e., a standard time is established for cleaning the windows on a per occurrence basis, with the number of occurrences per week or per month specified.

Example of how an Indirect Budget is established— If in a department there are 50 direct employees and 10 indirect employees, the expected output of the direct operators is 160,000 work units or standard minutes of work per week. The required amount of indirect labor to service the direct operators from time-study analysis is 26,400 standard minutes of work.

The Standard Indirect Factor is .165 Indirect Work Units for each expected direct work unit. This factor is multiplied by the total direct work units actually produced and shown in Column 7 of the analysis sheet (Fig. 1) to get the standard earned indirect work units.

CALCULATION OF FACTOR FOR DETERMINING AMOUNT OF EARNED INDIRECT WORK UNITS FOR WEEK

Classification	Required Standard Indirect Units		Expected Direct Units		Standard Indirect Factor
Lead Man	2,600	÷	160,000	=	.0163
Electrician	2,600	÷	160,000	=	.0163
Millwright	2,600	÷	160,000	=	.0163
Flask Man	2,600	÷	160,000	=	.0163
Oiler	2,600	÷	160,000	=	.0163
Cupola Liner	2,600	÷	160,000	=	.0163
Cupola Helper	2,600	÷	160,000	=	.0163
Ladle Liner	2,800	÷	160,000	=	.0175
Bed Burner	2,600	÷	160,000	=	.0163
Clean up Man	2,800	÷	160,000	=	.0175
			Total	=	.1654

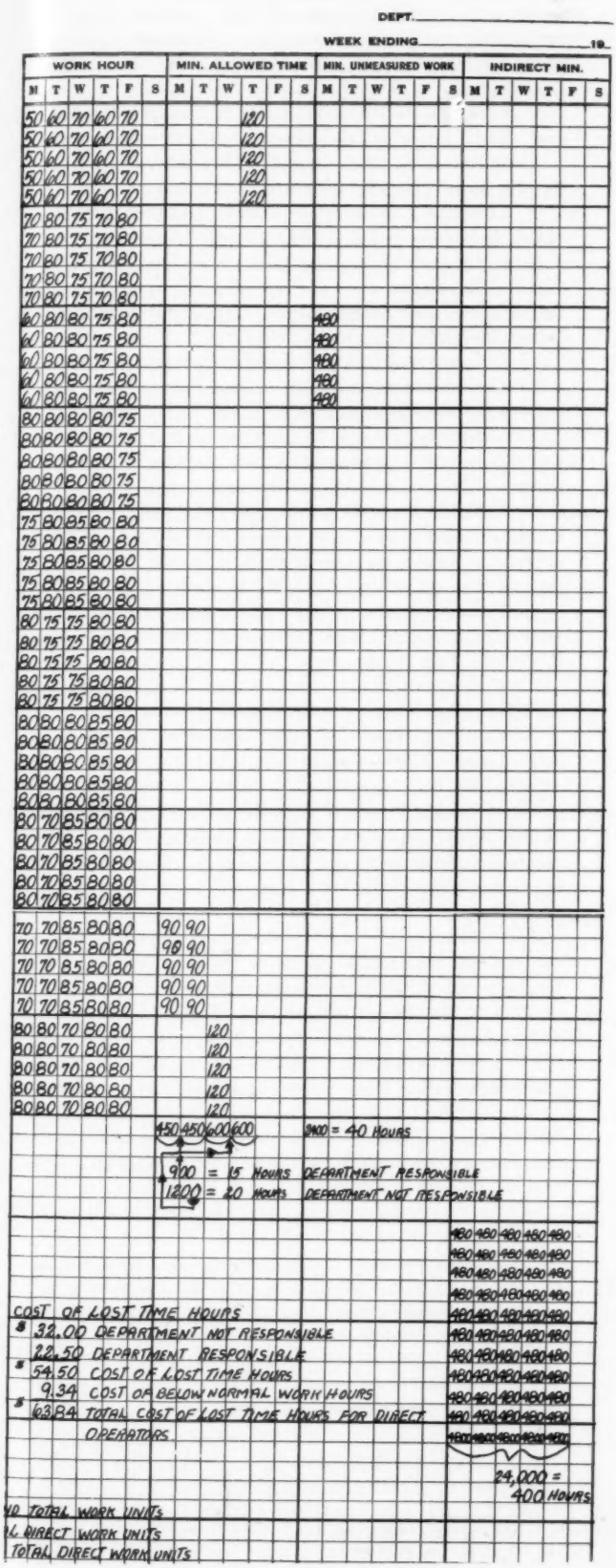
NOTE: The 160,000 direct work units used in the above calculation of the standard indirect factor is an 80 work-hour performance for the direct operators. This is also the average level of expected performance, and is, therefore, used in calculating the standard indirect cost per 1,000 direct work units, and the factor for determining the standard allowed indirect hours.

Time Keeping Procedure

In order to calculate the performance and earnings of direct and indirect workers, and the supervisory force, all direct and indirect production, hours worked, delay time, day work, rework, etc., are reported to the payroll department on daily time cards by time and production checkers, who actually count pieces produced, check and report lost time, scrap, rework, etc.

Fig. 2—Posting and Pay Roll Sheet

NAME	POSTED HOURS	BASE RATE	WEEKLY EARNINGS: BASE PAY	DIRECT PREM.	ADJ	IND. PREM.	TOTAL	TOTAL DAYS PAY: M	T	W	T	F	S	PREMIUM MONEY: M	T	W	T	F	S	HOURS: M	T	W	T	F	S	TOTAL WORK UNITS: M	T	W	T	F	S
1	40	1.40	56.00	3.74			59.74	11.20	11.20	13.07	11.20	13.07					1.87	1.87			8	8	8	8	8		400	480	560	480	560	
2	40	1.40	56.00	3.74			59.74	11.20	11.20	13.07	11.20	13.07					1.87	1.87			8	8	8	8	8		400	480	560	480	560	
3	40	1.40	56.00	3.74			59.74	11.20	11.20	13.07	11.20	13.07					1.87	1.87			8	8	8	8	8		400	480	560	480	560	
4	40	1.40	56.00	3.74			59.74	11.20	11.20	13.07	11.20	13.07					1.87	1.87			8	8	8	8	8		400	480	560	480	560	
5	40	1.40	56.00	3.74			59.74	11.20	11.20	13.07	11.20	13.07					1.87	1.87			8	8	8	8	8		400	480	560	480	560	
6	40	1.50	60.00	15.00			75.00	14.00	16.00	15.00	14.00	16.00		2.00	4.00	3.00	2.00	4.00			8	8	8	8	8		560	640	600	560	640	
7	40	1.50	60.00	15.00			75.00	14.00	16.00	15.00	14.00	16.00		2.00	4.00	3.00	2.00	4.00			8	8	8	8	8		560	640	600	560	640	
8	40	1.50	60.00	15.00			75.00	14.00	16.00	15.00	14.00	16.00		2.00	4.00	3.00	2.00	4.00			8	8	8	8	8		560	640	600	560	640	
9	40	1.50	60.00	15.00			75.00	14.00	16.00	15.00	14.00	16.00		2.00	4.00	3.00	2.00	4.00			8	8	8	8	8		560	640	600	560	640	
10	40	1.50	60.00	15.00			75.00	14.00	16.00	15.00	14.00	16.00		2.00	4.00	3.00	2.00	4.00			8	8	8	8	8		560	640	600	560	640	
11	40	1.50	60.00	15.00			75.00	12.00	16.00	16.00	15.00	16.00			4.00	4.00	3.00	4.00			8	8	8	8	8		480	640	640	600	640	
12	40	1.50	60.00	15.00			75.00	12.00	16.00	16.00	15.00	16.00			4.00	4.00	3.00	4.00			8	8	8	8	8		480	640	640	600	640	
13	40	1.50	60.00	15.00			75.00	12.00	16.00	16.00	15.00	16.00			4.00	4.00	3.00	4.00			8	8	8	8	8		480	640	640	600	640	
14	40	1.50	60.00	15.00			75.00	12.00	16.00	16.00	15.00	16.00			4.00	4.00	3.00	4.00			8	8	8	8	8		480	640	640	600	640	
15	40	1.50	60.00	15.00			75.00	12.00	16.00	16.00	15.00	16.00			4.00	4.00	3.00	4.00			8	8	8	8	8		480	640	640	600	640	
16	40	1.40	56.00	17.72			73.72	14.93	14.93	14.93	14.93	14.00		3.73	3.73	3.73	3.73	2.80			8	8	8	8	8		640	640	640	640	600	
17	40	1.40	56.00	17.72			73.72	14.93	14.93	14.93	14.93	14.00		3.73	3.73	3.73	3.73	2.80			8	8	8	8	8		640	640	640	640	600	
18	40	1.40	56.00	17.72			73.72	14.93	14.93	14.93	14.93	14.00		3.73	3.73	3.73	3.73	2.80			8	8	8	8	8		640	640	640	640	600	
19	40	1.40	56.00	17.72			73.72	14.93	14.93	14.93	14.93	14.00		3.73	3.73	3.73	3.73	2.80			8	8	8	8	8		640	640	640	640	600	
20	40	1.40	56.00	17.72			73.72	14.93	14.93	14.93	14.93	14.00		3.73	3.73	3.73	3.73	2.80			8	8	8	8	8		640	640	640	640	600	
21	40	1.45	58.00	19.36			77.36	14.50	15.47	16.45	15.47	15.47		2.90	3.87	4.85	3.87	3.87			8	8	8	8	8		600	640	680	640	640	
22	40	1.45	58.00	19.36			77.36	14.50	15.47	16.45	15.47	15.47		2.90	3.87	4.85	3.87	3.87			8	8	8	8	8		600	640	680	640	640	
23	40	1.45	58.00	19.36			77.36	14.50	15.47	16.45	15.47	15.47		2.90	3.87	4.85	3.87	3.87			8	8	8	8	8		600	640	680	640	640	
24	40	1.45	58.00	19.36			77.36	14.50	15.47	16.45	15.47	15.47		2.90	3.87	4.85	3.87	3.87			8	8	8	8	8		600	640	680	640	640	
25	40	1.45	58.00	19.36			77.36	14.50	15.47	16.45	15.47	15.47		2.90	3.87	4.85	3.87	3.87			8	8	8	8	8		600	640	680	640	640	
26	40	1.60	64.00	19.21			83.21	17.07	16.00	16.00	17.07	17.07		4.27	3.20	3.20	4.27	4.27			8	8	8	8	8		640	600	600	640	640	
27	40	1.60	64.00	19.21			83.21	17.07	16.00	16.00	17.07	17.07		4.27	3.20	3.20	4.27	4.27			8	8	8	8	8		640	600	600	640	640	
28	40	1.60	64.00	19.21			83.21	17.07	16.00	16.00	17.07	17.07		4.27	3.20	3.20	4.27	4.27			8	8	8	8	8		640	600	600	640	640	
29	40	1.60	64.00	19.21			83.21	17.07	16.00	16.00	17.07	17.07		4.27	3.20	3.20	4.27	4.27			8	8	8	8	8		640	600	600	640	640	
30	40	1.60	64.00	19.21			83.21	17.07	16.00	16.00	17.07	17.07		4.27	3.20	3.20	4.27	4.27			8	8	8	8	8		640	600	600	640	640	
31	40	1.70	68.00	23.97			91.97	18.13	18.13	18.13	18.13	18.13		4.53	4.53	4.53	5.67	4.53			8	8	8	8	8		640	640	640	680	640	
32	40	1.70	68.00	23.97			91.97	18.13	18.13	18.13	18.13	18.13		4.53	4.53	4.53	5.67	4.53			8	8	8	8	8		640	640	640	680	640	
33	40	1.70	68.00	23.97			91.97	18.13	18.13	18.13	18.13	18.13		4.53	4.53	4.53	5.67	4.53			8	8	8	8	8		640	640	640	680	640	
34	40	1.70	68.00	23.97			91.97	18.13	18.13	18.13	18.13	18.13		4.53	4.53	4.53	5.67	4.53			8	8	8	8	8		640	640	640	680	640	
35	40	1.70	68.00	23.97			91.97	18.13	18.13	18.13	18.13	18.13		4.53	4.53	4.53	5.67	4.53			8	8	8	8	8		640	640	640	680	640	
36	40	1.40	56.00	17.73			73.73	14.93	13.07	15.87	14.93	14.93		3.73	1.87	4.67	3.73	3.73			8	8	8	8	8		640	560	680	640	640	
37	40	1.40	56.00	17.73			73.73	14.93	13.07	15.87	14.93	14.93		3.73	1.87	4.67	3.73	3.73			8	8	8	8	8		640	560	680	640	640	
38	40	1.40	56.00	17.73			73.73	14.93	13.07	15.87	14.93	14.93		3.73	1.87	4.67	3.73	3.73			8	8	8	8	8		640	560	680	640	640	
39	40	1.40	56.00	17.73			73.73	14.93	13.07	15.87	14.93	14.93		3.73	1.87	4.67	3.73	3.73			8	8	8	8	8		640	560	680	640	640	
40	40	1.40	56.00	17.73			73.73	14.93	13.07	15.87	14.93	14.93		3.73	1.87	4.67	3.73	3.73			8	8	8	8	8		640	560	680	640	640	
41	40	1.50	60.00	17.00			77.00	14.00	14.00	17.00	16.00	16.00		2.00	2.00	5.00	4.00	4.00			8	8	8	8	8		560	560	680	640	640	
42	40	1.50	60.00	17.00			77.00	14.00	14.00	17.00	16.00	16.00		2.00	2.00	5.00	4.00	4.00			8	8	8	8	8		560	560	680	640	640	
43	40	1.50	60.00	17.00			77.00	14.00	14.00	17.00	16.00	16.00		2.00	2.00	5.00	4.00	4.00			8	8	8	8	8		560	560	680	640	640	
44	40	1.50	60.00	17.00			77.00	14.00	14.00	17.00	16.00	16.00		2.00	2.00	5.00	4.00	4.00			8	8	8	8	8		560	560	680	640	640	
45	40	1.50	60.00	17.00			77.00	14.00	14.00	17.00	16.00	16.00		2.00	2.00	5.00	4.00	4.00			8	8	8	8	8		560	560	680	640	640	
46	40	1.80	72.00	21.60			93.60	18.20	18.20	18.80	18.20	18.20		4.80	4.80	2.40	4.80	4.80			8	8	8	8	8		640	640	560	640	640	
47	40	1.80	72.00	21.60			93.60	18.20	18.20	18.80	18.20	18.20		4.80	4.80	2.40	4.80	4.80			8	8	8	8	8		640	640	560	640	640	
48	40	1.80	72.00	21.60			93.60	18.20	18.20	18.80	18.20	18.20		4.80	4.80	2.40	4.80	4.80			8	8	8	8	8		640	640	560	640	640	
49	40	1.80	72.00	21.60			93.60	18.20	18.20	18.80	18.20	18.20		4.80	4.80	2.40	4.80	4.80			8	8	8	8	8		640	640	560	640	640	
50	40	1.80	72.00	21.60			93.60	18.20	18.20	18.80	18.20	18.20		4.80	4.80	2.40	4.80	4.80			8	8	8	8	8		640	640	560	640	640	
			$3,050.00	851.65			$3,901.65																									
INDIRECT CREW																																
LEADMAN	40	2.10	84.00				$ 84.00	16.80	16.80	16.80	16.80	16.80									8	8	8	8	8							
MILLWRIGHT	40	1.90	76.00				76.00	15.20	15.20	15.20	15.20	15.20									8	8	8	8	8							
ELECTRICIAN	40	2.00	80.00				80.00	16.00	16.00	16.00	16.00	16.00									8	8	8	8	8							
OILER	40	1.40	56.00				56.00	11.20	11.20	11.20	11.20	11.20									8	8	8	8	8							
CLEAN UP MAN	40	1.25	50.00				50.00	10.00	10.00	10.00	10.00	10.00									8	8	8	8	8							
FLASK MAN	40	1.90	76.00				76.00	15.20	15.20	15.20	15.20	15.20									8	8	8	8	8							
CUPOLA LINER	40	1.80	72.00				72.00	14.40	14.40	14.40	14.40	14.40									8	8	8	8	8							
CUPOLA HELPER	40	1.50	60.00				60.00	12.00	12.00	12.00	12.00	12.00									8	8	8	8	8							
BED BURNER	40	1.46	58.40				58.40	11.68	11.68	11.68	11.68	11.68									8	8	8	8	8							
LADLE LINER	40	1.65	66.00				66.00	13.20	13.20	13.20	13.20	13.20									8	8	8	8	8							
			$ 678.40				$ 135.68	$ 81.408													480	480	480	480	480		29,000	30,200	31,400	30,800	31,400	
																					2,400 TOTAL HOURS						152,800 = GRO				
																					2,000 DIRECT HOURS						150,700 = TOT				
																					400 INDIRECT HOURS						148,300 = NET				
							COST OF BELOW 60 WORK HOURS = $ 2.14																											



Calculation of Standard Indirect Cost per 1,000 Direct Work Units

Classification	Std. Ind. Factor		Base Rate		Expected Department Performance		Work Units		Std. Ind. Cost
Lead Man	.0163	x	$2.10	÷	80	x	1,000	=	$ .499
Electrician	.0163	x	2.00	÷	80	x	1,000	=	.407
Millwright	.0163	x	1.90	÷	80	x	1,000	=	.387
Flask man	.0163	x	1.90	÷	80	x	1,000	=	.387
Oiler	.0163	x	1.40	÷	80	x	1,000	=	.285
Cupola Liner	.0163	x	1.80	÷	80	x	1,000	=	.367
Cupola Helper	.0163	x	1.50	÷	80	x	1,000	=	.306
Ladle Liner	.0175	x	1.65	÷	80	x	1,000	=	.361
Bed Burner	.0163	x	1.46	÷	80	x	1,000	=	.298
Clean Up Man	.0175	x	1.25	÷	80	x	1,000	=	.273
							Total	=	$3.499

$3.50 = Standard Indirect Cost per 1,000 direct minutes (Work Units)

This Standard Indirect Cost figure is posted on the weekly analysis sheet (Fig. 1) at the top of Column 28 under the heading "Cost Per 1,000 Work Units."

Calculation of Factor for Determining the Allowed Standard Indirect Hours for the Various Classifications for 1,000 Direct Work Units

Classification	Std. Indirect Factor		Expected Performance		Work Units		Std. Ind. Factor
Lead Man	.0163	÷	80	x	1,000	=	.204
Electrician	.0163	÷	80	x	1,000	=	.204
Millwright	.0163	÷	80	x	1,000	=	.204
Flask Man	.0163	÷	80	x	1,000	=	.204
Oiler	.0163	÷	80	x	1,000	=	.204
Cupola Liner	.0163	÷	80	x	1,000	=	.204
Cupola Helper	.0163	÷	80	x	1,000	=	.204
Ladle Liner	.0175	÷	80	x	1,000	=	.219
Bed Burner	.0163	÷	80	x	1,000	=	.204
Clean Up	.0175	÷	80	x	1,000	=	.219
					Total	=	2.070

The standard indirect factors are posted on the weekly analysis sheet (Fig. 1.) under the proper classification headings, which are under the general heading analysis of indirect hours. The factors for the various classifications are multiplied by the total direct work units in Column 7 (Fig. 1) to get the allowed hours.

Operators who produced scrap castings are not allowed credit for producing them. The base pay and premium earnings of the direct operators along with the base pay of the indirect operators are then calculated. This detailed data is recorded on a Payroll Posting Sheet (Fig. 2) under the proper headings.

Posting and Payroll Sheet

The Posting and Payroll Sheet (Fig. 2) is a summary sheet showing the daily performance and earnings of each operator in a department. At the end of the week, the daily earnings are totaled up to get weekly earnings. The Posting Sheet is displayed in a prominent place in the department each day to show the previous day's earnings and performance.

It gives the operator a daily opportunity to check his performance against the standard of 60 units of work per hour. It also gives the operator the opportunity to compare impartially his performance with the performances of other operators.

A worker quickly realizes that he has no standing with his fellow workers if his is in the red. That is a powerful incentive to keep operators from going in the red.

The Posting Sheet is also a service to Supervision—It allows the supervision to pick out those operators who do not measure up to standard. Frequently, these operators can be assigned to other operations to which they are better suited thus accomplishing the double service of salvaging a job for them and eliminating a labor loss for the company. The Posting Sheet is also a permanent record of the operators daily performance.

Lost time expenses such as "waiting for work," breakdowns, re-work, process allowance, excess handling, etc., can be quickly spotted and measures can be taken to correct these losses whenever possible.

At the end of the week or pay period, the data on the Posting Sheet (Fig. 2) are totaled and transfered to the Analysis Sheet (Fig. 1), on which calculations are made to determine actual indirect incentive earnings, performance of both direct and indirect labor and effectiveness of supervision for the period.

Weekly Departmental Labor and Cost Analysis Sheet

The Weekly Departmental Analysis Sheet (Fig. 1) is a summary sheet compiled for each department. It shows accurately and in detail the departmental direct labor and indirect labor performances together with the direct and indirect costs. Excess costs and lost time are also shown on the analysis sheet. It serves as a barometer in that it enables management and supervision to note the effectiveness of departmental performance, where labor costs can be controlled and presents on an equitable basis a comparison of the performances of different departments and divisions of the company.

Excess costs and lost time are the responsibility of the foreman and management. It is with these costs, the analysis mainly deals.

Intelligent use of the analysis by the supervision for controlling labor costs and waste is absolutely essential for good management.

By refering to the supervision effectiveness shown and then tracing variations back through the cost percentages to the totals used and, thence to the Posting Sheet (Fig. 2) and daily time cards (Fig. 3), the foreman can find out exactly what occasioned the conditions indicated and take steps to encourage or prevent their recurrence.

All this should be done in any incentive plan but here it is a part of the plan itself and hence, never omitted.

Technique in Using Weekly Departmental Analysis for Cost Control—The use of the analysis (Fig. 1) for noting the performance in a department can be made by studying the data shown under the various headings:

1. Column 1 shows the total department employees. This figure is calculated by dividing the total hours worked by both direct and indirect operators by the hours in the standard work week.
2. Column 2 shows the average number of direct operators in the red or below standard each day.
3. Column 3 shows the hours spent on measured work. This figure does not include idle time, rework, indirect, or unmeasured time. These data are taken from the daily time cards, Fig. 3.
4. Column 4 shows the number of lost time hours for which the supervision is responsible. These data are taken from the daily time cards.
5. Column 5 shows the number of lost time hours for which the supervision is not responsible. These data are taken from the daily time cards.
6. Column 6 shows the total of columns 3, 4 and 5. The difference in the two figures is column 5; it is not included in the bottom figure. The above figures are taken from the daily time cards.
7. Column 7 shows the total Direct Work Units and Unmeasured Minutes. These figures are taken from the daily time cards.
8. Column 8 shows rework and excess handling and cleaning time in minutes.
9. Column 9 shows the total direct measured work units only. These figures are taken from the daily time cards.
10. Column 10 shows the direct work hour. This is the average number of direct work units produced per man-hour. It is the net total work units shown in Column 9, divided by the total hours in Column 6.
11. Column 11 shows the actual indirect hours worked by all classifications of indirect labor. This figure is taken from the daily time cards.
12. At the head of Column 12 is shown the indirect factor. The total direct work units in Column 7 is multiplied by this factor to get the standard earned indirect work units shown in this column.
13. Column 13 shows the indirect work hour. The indirect work hour is the average number of indirect work units produced per indirect man hour. This

Fig. 3—Daily Time Cards

Clock No. 200 Name John Doe B. R. 1.80 Dept. S.F. Date 11-5-52

Sales Order No.	Shop Order	Mach. No.	Operation No.	OPERATION	TIME		Quan.	Work Value	WORK UNITS Meas.	A/c	U.M.	Ind.	Direct Labor Cost
					6:00	S							
		B		Make 4" AB Top Core	8:50	F	60	4.25	255				7.65
		B		Make 6" AB Cross Core	1:10	F	34	9.00	306				9.18
		B		Make 8" Solid Sleeve	2:30	F	10	8.19	82				2.46
						F							
						F							
						F							
						F							
						F							
						F							

Total Hrs.	Total Work Units	Work Hour	Premium Minutes	Premium Money	Total Days Pay
8	643	80	165	$4.89	$19.29

TOTALS 643 $19.29

Clock No. 210 Name John Jones B. R. 1.60 Dept. Joint Date 11-5-52

Sales Order No.	Shop Order	Mach. No.	Operation No.	OPERATION	TIME		Quan.	Work Value	WORK UNITS Meas.	A/c	U.M.	Ind.	Direct Labor Cost
					7:00	S							
		159		Wait on pipe	8:00	F				60			1.60
		159	150	Face 6" M.J. Pipe	2:15	F	80	5.2	416				11.09
		159		Clean out water pit	2:55	F						40	1.07
		159	2	Get oil & reamer	3:10	F	1	15.	15				.40
		159		Repair Machine	3:30	F						20	.53
						F							
						F							
						F							
						F							

Total Hrs.	Total Work Units	Work Hour	Premium Minutes	Premium Money	Total Days Pay
8	551	69	72	$1.89	$14.69

TOTALS 431 60 60 $14.69

figure is arrived at by dividing the indirect work units shown in Column 12 by the actual hours worked shown in Column 11.

14. Column 14, Total Work Hour. The Total Work Hour is the total of direct and indirect work units divided by the total direct and indirect hours. The figures in Columns 9 and 12 are added and divided by the total hours in Columns 6 and 11. This index figure is a weighted average performance of both indirect and direct.

15. Column 15 shows the cost per standard dollar. This is a unit figure showing the excess cost existing in the total payroll over what should have been paid under standard or correct operation. A cost per standard dollar of $1.05 indicates that on each dollar which was paid in the payroll, an excess of 5 cents was actually paid out. This figure is arrived at by dividing the actual costs in Column 29 by the standard cost figure shown at the top of the column.

16. Column 16—The Department Effectiveness is an index figure indicating the overall effectiveness, which is the total work hour shown in Column 14, corrected by percentage of excess cost shown in Column 15. For control purposes this is important because it is possible for a department to operate at a high work hour and yet have a large excess cost in the form of process allowance, rework, lost time, excess cleaning, etc. The department effectiveness is calculated by dividing the total work hour by the cost per standard dollar. The top figures in Columns 14 and 15 are used in making the calculation.

17. Column 17 — Supervision Effectiveness. The only difference in this index figure and the department effectiveness index figure is that the supervision is not charged with the lost hours in Column 5. This is the figure on which the supervision and indirect operators are paid incentive earnings. This index is calculated by dividing the total work hour by the cost per standard dollar. The bottom figures shown in Column 14 and 15 are used in making the calculation.

18. For control purposes, management should compare department and supervision effectiveness figures with preceding periods and note whether there has been an improvement or retrogression. If there has been a retrogression in Department Effectiveness, look to Columns 14 and 15 showing the total work hour and the Cost Per Standard Dollar respectively, and note which of these two items show the greater loss. (In some cases both are losers.)

19. A lower tendency of the total work hour shown in Column 14 can be traced either to the direct or indirect by noting the Direct Work Hour in Column 10 and the Indirect Work Hour in Column 13. The occasion for a lower direct work hour can be identified by noting Column 2, showing the employees below 60 work hour and Columns 4 and 5 showing lost hours. The low indirect work hour can be further traced by noting the distribution of the various indirect functions in Columns 30 to 43. In Columns 44 and 45, a comparison is shown between the total indirect hours actually worked and the standard hours allowed.

20. A retrogression in the Cost Per Standard Dollar shown in Column 15, can be traced in Columns 27 and 28 showing the actual direct and indirect labor costs. The reasons for the Actual Direct Labor Cost Per 1000 Direct Units being greater than the Standard Direct Labor Cost are as follows:

(a) Operator performance being less than 60, resulting in cost per unit to equal 60. See Column 2.

(b) Lost hours shown in columns 4 and 5. These lost hours are caused by waiting for material, breakdowns, improperly operating equipment, poor quality material, rework, etc.

(c) Base rates of operators being greater than the base rates set up for the jobs.

(d) Payment of overtime increment.

The reasons for the Actual Indirect Labor Cost Per 1000 Direct Units being greater than the Standard Indirect Labor Cost are as follows:

(a) Indirect Work Hour being less than expected 80.

(b) Indirect operator base rates being greater than the base rates set for the job.

(c) Payment of overtime increment.

(d) Actual indirect hours shown in Column 44, being greater than allowed indirect hours shown in Column 45.

21. Column 18 — Percent of Unmeasured Time. As the effective results of this time is unknown and frequently is the source of loss, it should be kept as low as possible. The per cent of unmeasured time is calculated by dividing the unmeasured hours by the total department hours.

22. Column 19—Unmeasured Hours are bonafide direct labor hours for which incentive standards have not been established. This time is taken from the daily time cards.

23. Column 20 on the Analysis Sheet shows the actual cost of lost time hours shown in Column 4 and 5.

24. Column 21 shows the amount paid out in the total payroll above the amount which should have been paid. With the department working normally, no lost time, all workers producing at a 60 work-hour or above, all jobs assigned to workers who bear the base rates for those classes of work, and the indirect functioning within its correct limits in relation to the direct, there will be no losses. (Upper line = Column 29 upper line — Std. Cost per 1000 direct work units $\times$ Column 9). (Lower line = Column 29 lower line — Std. Cost per 1000 direct work units $\times$ Column 9).

25. Column 22 shows actual cost of all direct operations on incentive. Includes base pay (which covers money paid for lost time), and premium pay. The difference in the two figures in this column is the amount of direct premium.

26. Column 23 shows cost of unmeasured time. This cost is taken off the daily time cards.

27. Column 24 shows indirect labor cost. The top figure includes the premium for the previous week. Lower figure does not.

28. Column 25 shows the total payroll for the department, Columns 22 plus 23 plus 24. Upper figure includes all premium. Lower figure shows base pay only.

29. Column 26 shows the standard direct cost for 1,000 direct work units. Column 22 upper line — (Cost of Columns 2 + 4 + 5) ÷ Column 9 × 1,000 = Column 26.

30. Column 27 shows the actual direct cost for 1,000 direct work units. This figure is calculated by dividing the cost of measured work shown in Column 22 by net work units in Column 9 × 1,000.

31. Column 28 shows both the standard and actual indirect cost for 1,000 work units. The standard cost ($3.50 in this case) is shown at the head of the column. The actual cost ($4.50) is calculated by dividing the cost of indirect labor shown in Column 24 (lower figure) by the total direct work units shown in Column 7. (Note: The Difference in the upper and lower figure in Column 24 is the top figure includes premium earnings for the previous week. The lower figure shows base pay only.)

32. Column 29 shows the total standard and actual cost for 1,000 direct work units (both direct and indirect). At the head of the column is shown the standard cost, ($28.97 in this case). Under this heading are shown two actual cost figures. The upper actual cost figure, ($30.40 in this case) includes the cost of lost hours shown in Columns 4 and 5. The lower figure ($30.20) does not include the cost of lost hours shown in Column 5. The department supervision is responsible only for the lower cost figure. The upper actual cost is calculated by adding the actual direct cost of $25.90 shown in Column 27 to actual indirect cost $4.50 shown in Column 28. This equals $30.40. The lower figure is calculated by adding actual direct cost of $25.70 shown in Column 27 to actual indirect cost of $4.50 shown in Column 28. This equals $30.20. (Note: In all cases where two figures are shown in a column the lower figure is the one which the department supervision is responsible for.)

33. Columns 30, 32, 34, 36, 38, 40 and 42 show the actual hours worked by the various indirect classifications.

34. Columns 31, 33, 35, 37, 39, 41 and 43. At the head of each column, the indirect factor is placed. The factor is the measure of the amount of indirect hours required per 1,000 direct work units. This factor multiplied by the direct work units shown in column 7 equals the allowed indirect hours for that classification of labor. The allowed hours are shown under the various headings.

35. Column 44 shows the total indirect hours worked by the various classifications.

36. Column 45 shows the total allowed indirect hours for the various classifications. At the top of the column is shown the total standard factor. This factor multiplied by the total direct work units in column 7 equals the total allowed hours.

The advantages of the Wage Incentive Plan can be summarized as follows:

(a) It is a yardstick for measuring and comparing performances of all types of operations and departments in the company.

(b) It provides controls over quality, quantity, rework, lost time, safety, good housekeeping, maintenance, supervision, etc.

(c) It legitimately eliminates the complaints of indirect employees about the difference in their take home pay and that of the direct employees working on incentive jobs.

DISCUSSION

Chairman: DEAN VAN ORDER, Burnside Steel Foundry Co., Chicago.

Co-Chairman: J. J. FARKAS, Cincinnati Milling Machine Co., Cincinnati.

JOHN TAYLOR:[1] How many hours per week is required to post all these analysis reports?

MR. PRUET: This work requires three full time clerks in the Payroll Department. We also have eight Production and Time Checkers in the plant whose duties are to count and record all pieces produced, record all lost time and the cause of the lost time, etc.

RICHARD WALTHER:[2] Any Incentive Plan necessarily has to be simple so the men can understand it. Did you have a selling plan in making the men understand how to make out their own bonus earnings?

MR. PRUET: The direct incentive earnings are easy to determine. It is the rate per piece times the good pieces produced. However, the indirect incentive is not so simple. Premium pay for indirect workers is earned only when both the direct and indirect workers have performed efficiently. This was a little difficult to explain at first. However, the men soon learned the level of output and performance at which the department must operate for a given amount of incentive pay.

CO-CHAIRMAN FARKAS: Since indirect incentive earnings under your plan are a function of production effectiveness, what measures do you take to protect indirect workers against poor performances by producers?

MR. PRUET: Since our indirect incentive is a group plan, it has all the bad features of group plans, that is, if some members of the group do not produce, the entire group suffers accordingly. The only action we take is to try to eliminate the cause of low performance by the production operators. If production is low, no incentive pay is earned. If costs are too high no incentive pay is earned. Any sound incentive plan is based on the principle of payment for results. Indirect workers and supervision earn incentive pay only when the entire department, both direct and indirect, has performed at an incentive pace.

LAURENCE HANSEN:[3] How is a mechanical breakdown charged in this system?

MR. PRUET: Lost time hours and costs of all mechanical breakdowns are charged against the indirect worker's and supervisory budget. If a breakdown occurs on a production machine the lost time is charged against the production department and the Maintenance Department. They are held jointly responsible. This prevents "buck passing" and promotes cooperation.

L. C. GETSINGER:[4] (1) How far up is supervision affected by the incentive plan? (2) Does this incentive encourage supervisors to fight for loose standards?

MR. PRUET: (1) The incentive plan covers all levels of supervision below the plant superintendent. (2) Supervision is jointly responsible with the Industrial Engineering Dept. for correct incentive rates. If a rate is loose it must be corrected and the supervisor is the one who has to deal with the worker. After the supervisor has had to clear up two or three cases like this we have no more trouble with that supervisor, along this line.

J. A. WESTOVER:[5] Can it be justified to charge overtime to supervision and indirect operators?

MR. PRUET: Yes, because usually when overtime is worked, it is the result of poor planning and scheduling, or low performance. We do not like to work our people overtime, and therefore discourage it. Supervision are in effect, operating their own business. If they operate their departments efficiently they earn extra pay.

G. H. ROCKWELL:[6] What allowances are made for poorly conditioned sand, poor cores, excess fins on castings, etc.?

[1] Vice-President, Lester B. Knight & Associates, Chicago.
[2] Foreman, Dayton Steel Foundry, Dayton, Ohio.
[3] General Foreman, International Harvester Co., Hamilton, Ont., Canada.
[4] Assistant Superintendent, Aluminum Company of America, Massena, N. Y.
[5] Partner, Westover Engineers, Milwaukee, Wis.
[6] Foundry Superintendent of Planning, Caterpillar Tractor Co., Peoria, Ill.

MR. PRUET: We do not pay for scrap. We do not pay for work which does not pass specifications. Such production is not recorded on the man's time card. If the sand or core is no good and as a result the casting is scrap, the casting is not considered as having been made. For example, on one production unit we have a group of 53 men. This includes melting, molding, core making, core setting, pouring, and shakeout. If the core is no good and the casting is scrap, no one in this group is paid for the casting. However, we have determined by statistical analysis what the per cent loss should be for good operation and this percentage is added to the incentive rates.

On other jobs where we have individual incentive rates, the operator who made the scrap is not paid for it, but other operators who may have worked on the same item, are paid for their work. The work which is paid for, is considered as lost time if the casting is scrap. This lost time and the resulting cost is charged against the Supervision Incentive Budget.

When castings have excessive fins, gates, etc., the extra chipping time above standard which is required, is charged back against Foundry Supervision, up to as much as the standard time value established for the chipping operation. If the chipping time exceeds twice the standard time value the casting is scrapped and the molder is not given credit for making the casting.

L. C. PARRISH: [7] Did the savings of 15 per cent include the increase in time keeping cost? If not what was this increase?

MR. PRUET: The savings of 15 per cent is the man-hours saved in the Mechanical Department. We did not increase the number of clerks in the Payroll Department. It requires only about four extra man-hours per week to handle the analysis sheets and compute the incentive earnings for the Maintenance Department. We have three different groups and therefore have to have a different analysis sheet for each group in the Mechanical Department.

R. F. EVERT: [8] Do your indirect people ever question the ratio, and if so how do you solve the problem?

MR. PRUET: Yes, we have had our incentive applications questioned on several occasions. We have always been able to solve these questions by explaining what we had done and what we were trying to do. If we are in error the necessary corrections are made. So far, we have not had much trouble with our maintenance incentive. The Supervisory Staff is the key to the success of any good incentive plan. Our incentive plan is an entire production, quality and cost control system. Supervision earn incentive only if they make the plan work. We get more benefits from the control features than from the incentive features. The incentive features are added to make the controls work smoothly and with real effectiveness.

WM. PITTS: [9] Were all of your standards on maintenance employees including those who repair breakdowns, developed by Time Study and if so, how is the earned time measured?

MR. PRUET: All incentive standards in our plant are derived from Time Study and Statistical Analysis. The average amount of work required to properly maintain and service the production units for a specific level of production and for a specific operating condition is determined. A factor or ratio is set up for this condition. The Weekly Analysis Sheet is used to show all lost time, excess costs, and direct work units produced. From this data, the performance of both direct and indirect workers and the supervision is calculated. The ratio of indirect standard work units to direct standard work units is shown on this sheet. The indirect factor (ratio) is multiplied by the total direct work units produced, this equals the standard earned indirect work units for the indirect crew for the week.

LAURENCE POHYOLA: [10] (1) How do you measure the amount of unmeasured work to arrive at figure in Column 19? (2) What procedure do you use to arrive at a representative work load for indirect operators (e.g. Ratio Delay)? (3) Who arbitrates disputes on figures in Column 4 (lost hours)? (4) How do you handle fractional crews for maintenance requirements in setting rates?

MR. PRUET: (1) The unmeasured work shown in Column 19 is reported to the Payroll Department as such by Time and Production Checkers. This time is totaled for the week and posted in Column 19. (2) All work loads are determined by Time Study Analysis. We have used ratio delay to determine the amount of delays on production operations, but not on maintenance work loads. (3) We have defined in detail the type of delays which are charged against the supervision. This time is shown in Column 4. Supervision are charged with any delay or rework which originates within their department, with the following exceptions: (a) Training new employees. (b) Experimental work. (c) Meetings for the purpose of handling Company business such as safety, insurance, new company policy, etc. In the event of a delay not specifically defined, the decision is made by the author. In the event the decision is questioned, it is referred to the Plant Manager for final disposition. (4) All indirect and maintenance incentives are on a group basis. In some cases where a member of an indirect crew does not have a full work load he is scheduled direct production work to make up the difference. An effort is always made to schedule a full work load to the operator. When it is not possible to do this the group will have a low performance. We do not guarantee incentive pay, it must be earned. Therefore, if performance is low, incentive earnings will be low.

J. A. FORBES: [11] (1) Do you use your work unit standards for cost estimating? (2) Do you use your actual work units for job costs?

MR. PRUET: (1) Yes. (2) The work units are actually the standard cost for the casting. Any cost other than this would be in excess of standard.

G. L. BREUNIG: [12] With a variation in scheduling, do you find that your ratio of indirect to direct varies at constant, a decreasing or increasing rate?

MR. PRUET: The ratio does not increase or decrease at a constant rate. For example, some indirect and maintenance work will not vary regardless of the amount of direct production, some will vary with production but not in exact proportion. When these factors are known, indirect man-hours can be scheduled for any level of production and operating condition.

MEMBER: How large a staff is needed to handle the clerical work involved in keeping these records?

MR. PRUET: We have four Time Study Engineers, eight Time and Production Checkers, and three Clerks in the Payroll Dept. These people plus two department heads handle the entire Industrial Engineering and Payroll Department duties.

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[7] Foundry Superintendent, J. B. Ehrsam Co., Enterprise, Kansas.
[8] Production Manager, Eaton Manufacturing Co., Vassar, Mich.
[9] Standards Foreman, Oklahoma Steel Castings Co., Tulsa, Okla.
[10] Central Staff, Ford of Canada, Windsor, Ont., Canada.
[11] Vice-President and General Manager, Gunite Foundries Corp., Rockford, Ill.
[12] Chief Industrial Engineer, National Presto Industries, Inc., Eau Claire, Wis.

POSITIVE BENEFITS OF A SAFETY PROGRAM

By

F. W. Shipley*

Everyone is familiar with the phrase "The Safe Way Is The Best Way." If we would just change one word in that phrase we will be well on our way to a manner of thinking that is absolutely necessary if accidents are to be prevented. Let us change it to read:

"The Safe Way Is The Only Way"

If we who are interested in the welfare of our people really assume the responsibility which is ours as managers, there can be little doubt but that accidents can be very greatly reduced.

The experiences of a great number of companies including our own over the past ten years or so, show that industrial accidents can be reduced. The records prove without question that there are positive benefits to be gained. However, in order to attain a worthwhile program of safe practices it is absolutely essential that a *real desire* for a reduction of accidents be present. It is not enough to give lip service alone to the idea of a safety program because accidents cannot be prevented by putting up signs to the effect that tomorrow we start a safety program and therefore we do not expect any more accidents from that day on. We must believe it, really want it, and think about it 24 hours a day.

Safety Comes First

To set up a program for safety that really works we must first start with the premise that "Safety Is Actually The Most Important Thing We Have To Do." It must come ahead of everything else, yes, even production. This was brought out forcefully in our own company just recently by our President, L. B. Neumiller. While discussing the problems ahead for the coming year, at a Management Meeting attended by all supervision, he said, "The most important job for all of us in the year ahead is safety," and judging from the safety record of the company over a long period of time you know full well that he meant it.

Outline of a Safety Program

Perhaps the best way for me to outline a safety program is to describe some steps we have taken and enumerate the results. Approximately 20 years ago when our accident rate was far from good we set up a system whereby each accident, regardless of severity, was recorded on a First Aid IBM card. On the front of this card the injury is described from a medical standpoint. On the back, space is provided for the foreman to describe in detail the *"how, when* and *why"* of the accident. The records from these cards are compiled by the Safety Division who issue monthly reports by departments.

Off shift monthly safety meetings attended by all levels of supervision are held around the clock. Arrangements are made for a different supervisor to lead each monthly meeting of his own shift. This means that three different supervisors would lead safety meetings each month. This fact alone helps further the safety program because one cannot help but be more safety conscious after having led one of the meetings. At these safety meetings lost time cases are discussed in detail with special emphasis on ways of preventing recurrences. Even minor accidents are also discussed for it is realized that given a right set of circumstances these too could have been lost time cases. A committee of supervisors was also set up to inspect the shop daily for cleanliness and orderliness.

The men on this committee inspect other departments than their own and make their reports individually at the regular monthly safety meetings. We have always felt that cleanliness and orderliness contribute greatly to accident prevention and that generally speaking, a clean shop is a safe shop. In addition to the monthly safety meetings, a committee of three supervisors (a different group each month) meet each day to thoroughly investigate the First Aid cases from the previous day and make recommendations to prevent recurrences. In connection with this whole program everyone is encouraged to go to the First Aid Station for even the most minor cut or scratch and supervisors are criticized if their people do not visit First Aid when they are injured. These investigations are simply another method of checking on the supervisor as a follow up on the manner in which the foreman had investigated the accident and what he had done to prevent further accidents

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of this kind. All this was designed as another step in a never ending campaign of education to make everyone, supervisors and all, conscious of accident prevention.

Protective Equipment

Safety glasses are furnished free of charge and if prescription lenses are necessary the company pays half the original cost. In the foundry the wearing of safety glasses was made a condition of employment. When this program was first introduced, it was very difficult to get everyone to accept it, but now it is accepted readily by everyone. In fact, everyone is so conscious of it that anyone, even visitors, walking into the foundry without safety glasses will be stopped by someone before he progresses very far into the building.

All iron pourers, cupola operators, grinders and chippers wear safety goggles which fit tightly against the face. These too, are furnished free of charge. Another vital part of the whole eye protection program is regular inspection and replacement of defective lenses, frames, etc. A qualified employee of the Safety Division makes periodic visits through all departments with his "goggle cart." He is equipped to examine all goggles and glasses and replace on the spot any defective parts which he finds. This program insures against the possibility of anyone not wearing glasses or goggles because they have become defective. There just is no excuse for not wearing eye protection when it is kept readily available and maintained in A1 condition. As a result of this program eye injuries in the foundry are kept at a minimum.

Another step in the prevention of accidents is the protection of the feet. All employees are encouraged to wear safety shoes. As part of this program the Safety Division maintains a large well equipped stock of safety shoes which are sold to employees at cost. On this basis he is able to buy safety shoes at lower costs, generally, than other shoes so he has little sales resistance against the wearing of safety shoes. A concentrated sales program on safety shoes is given to the new hire who is entering the plant for the first time. As a result these new employees will be found to be practically 100 pct equipped with safety shoes. In connection with this same program of foot protection aluminum slip-over foot guards are furnished to those employees whose occupations are most hazardous in this respect. In the foundry all iron pourers must wear safety shoes with good substantial soles. Olive drab wool pants faced with chrome leather are also furnished these people. These are dry cleaned once a week and returned to the employee without charge. In addition, the iron pourer is required to wear chrome leather spats or leggings over his shoes and underneath the pants leg. We have found this method more successful than wearing leggings on the outside because splashed iron will not collect in the folds of the trousers and burn through to the body. As a result of these precautions we have reduced incidents of body burns among the group of iron pourers to a point where they are almost negligible.

Licensing Program for Motive Power Operators

Another vital step for prevention of accidents was the establishment of a training and licensing program for all power truckers, tractor drivers, overhead crane operators and drivers of mobile equipment. No person is allowed to operate any of the above equipment without a license. This license, in the form of a special badge, must be worn at all times while operating the equipment. Special instructors under the supervision of the Training Department are in charge of this program. Candidates for training, selected by the supervisor, are screened by the Medical Department for physical soundness with special emphasis on eyes (including depth of perception as well as good vision, reaction time and emotional stability). The trainee is taught the care of the equipment as well as the safe way to operate the particular machine for which he is to be licensed. At the conclusion of the course he is given an actual driving test under the watchful eye of the instructor at which time he is either passed or rejected. In our experience approximately 78 pct of the people who have been considered for these jobs have passed the tests and have been licensed. Of those who were not licensed, most were rejected because of poor eyesight, carelessness, or emotional instability.

What our experience might have been if the 22 pct who were rejected had been allowed to operate we do not know, but we do have proof positive that accidents of this category have been very materially reduced since the training program was inaugurated.

Constant Supervision Observation

The responsibility for the safe operation of this type of equipment does not end however when the license is issued. These operators are constantly observed by the foreman and if one should become careless he is warned by the supervisor and if, after this warning, he continues to operate carelessly, he is taken off the job and his license revoked. Relief or extra operators are trained and licensed to take the place of the regular operator in case of absenteeism, but it is stipulated that the relief operator must operate the equipment at least one day per month or he is to be given a refresher course before he is allowed to operate again. Although operators of battery-powered hand trucks are not licensed they are trained by the foreman on the job and given a booklet concerning the operation of this equipment. This booklet describes by illustrated cartoons the safe way to operate the truck and also shows what might happen if it is not operated properly. All accidents relative to these hand trucks either directly or indirectly, have dropped 30 pct since this program was inaugurated about a year ago.

Approximately 10 years ago Union Safety Meetings were set up. In the foundry there is one safety committeeman for each General Foreman. These safety committeemen meet once a month with the General Foremen of the foundry and discuss various safety problems. The Chairman of the Union Safety Committee then meets once a month with the Safety Di-

rector and his staff to discuss overall plant safety problems. This program, too, has helped make everyone more safety conscious and thus it becomes a vital part of safety education. These monthly meetings with the General Foremen and the Union Safety Committee are in addition to the Supervisory Monthly Safety Meetings which have been described before.

The Safety Division has also set up a staff of Safety Inspectors operating on all shifts whose duty is to be constantly on the lookout for unsafe practices. Whenever they observe anything unsafe they report it immediately to the Supervisor of the department and also make out a report to the Safety Manager who in turn calls it to the attention of the Factory Manager.

To a man who has been injured, or to his family who is losing his income as a result of an accident, an outstanding record of plant safety does not mean a thing. He only thinks of his own misfortune and the real hardship which confronts him at that time. For this reason we have always had uppermost in our minds the reduction of injuries to our employees and not the establishment of an outstanding record. Records in themselves do not prevent accidents but only tell us where we have been and where we are going. The record of our performance as regards safety is illustrated in the graph.

Here a comparison is made between our lost time accident record and that of the National Safety Council for the Foundry Industry in general. In 1952 while working 2,968,025 man-hours we had four lost time cases among approximately 1700 employees.

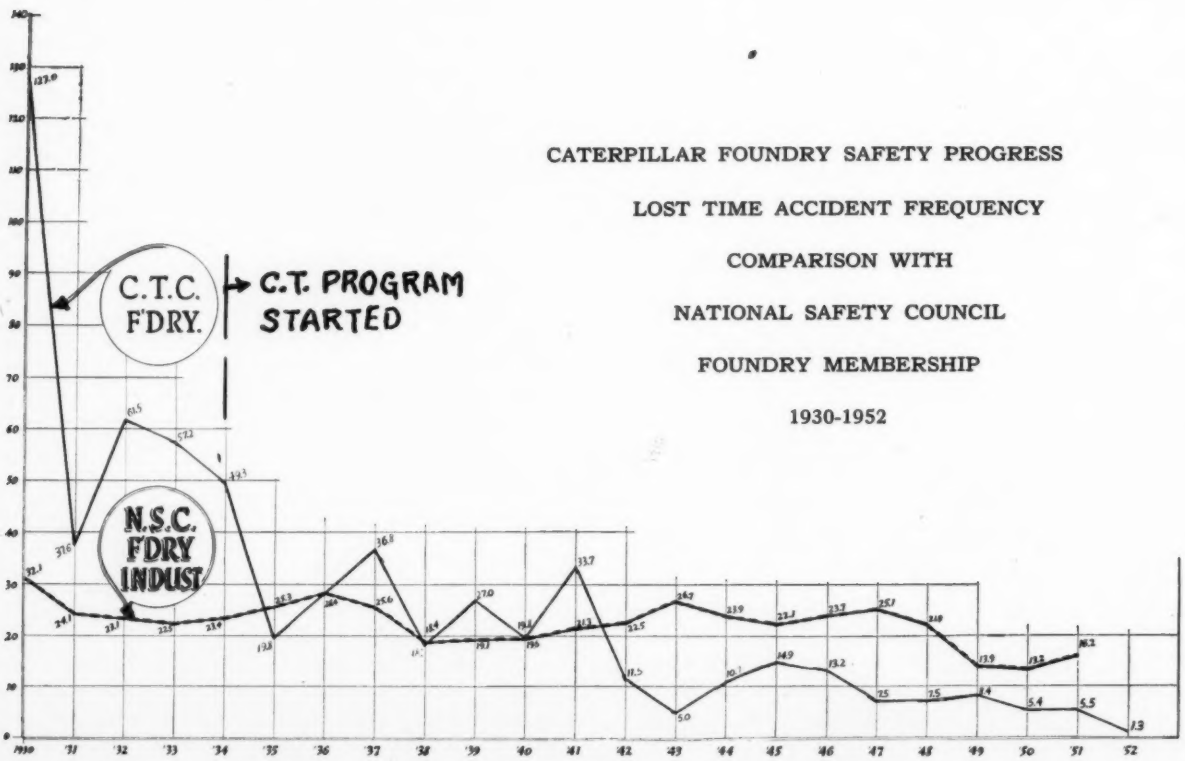
These cases in detail are as follows:

Case No. 1—A sandslinger helper slipped while he and another employee were rolling over the drag half of a mold (pop-off flask) which weighed approximately 100 lb. Another employee who was working with him did not notice that he had slipped. Injured employee did not report to First Aid or to Foreman until two days later. His back was injured and he lost ten days time.

Case No. 2—A swing grinder was grinding an axle housing. He had previously ground three of these castings and had placed them on a pile behind him. While grinding the next casting he inadvertently braced his foot against the pile of ground castings. The top casting slipped off because it had not been placed properly. It fell to the floor and rolled on his foot. Casting weighed approximately 300 lb and fell about 1 ft onto employee's foot striking back of steel toe of his safety shoes. Employee lost six weeks time.

Case No. 3—Shot blast operator was hanging casting on blast conveyor chain. An exhaust manifold, approximately 6 ft long and weighing 150 lb slipped loose at one end, fell to the floor and struck his foot. He was using the wrong kind of hooks. The foreman noticed it but the accident happened before he could reach the man and stop him. Employee lost six weeks time.

Case No. 4—A wood patternmaker noticed an irritation in right eye. He did not know the specific source of the small piece of steel found in his eye, nor did he recall an instance which could have caused the injury. He works about 10 ft from a wood sander which might have been the source but no one knows.



He did not report to First Aid until approximately 4 hr after he noticed irritation. Man lost seven days.

All of these accidents, expect possibly Case No. 4, would not have happened if the people had been thinking and had used the safe practice which they had been taught. The significant point here is that safety instruction alone is not enough. It is necessary to constantly be on the lookout for unsafe practices and call it to the attention of the people continuously.

Conclusion

Safety is more of a state of mind than an array of protective guards over a machine.

Elimination of accidents is a long time proposition. It cannot be accomplished in a month or even a year.

Safety is a never ending program of education and constant effort. We must be alert and stop unsafe practices on the spot. Do not wait to write a report about it after an accident occurs. Many times in the past we might have been justifiably discouraged for our results seemed to indicate little progress. However, the results on the graph indicate that our efforts of long ago were not in vain and eventually proved worthwhile.

Safety equipment must be provided and used.

"The Safe Way Is The Only Way."

"Safety is the most important job we have every day."

Any program of safe practices is no better than the *desire to make it real.*

Accidents can and must be prevented.

EFFECT OF SAND FLOWABILITY ON CASTING SURFACE FINISH

By

H. H. Fairfield* and James McConachie**

ABSTRACT

This paper reports results of some experiments made in an effort to measure flowability of molding and core sands and to relate flowability test values to surface finish. This is the third report on casting finish submitted by the writers to A.F.S.

■ During an investigation of grain size of sand and its effect upon casting finish[6], the data shown in Table 1 was obtained. Four different sands would produce castings of identical smoothness. However to get equivalent smoothness, one sand had to be jolted 32 times, another sand needed only 8 jolts. It was apparent that these sands differed in their resistance to compacting. This would seem to be a very important property of molding and core sands. In practical foundry operations, surface finish is more dependent upon flowability than on the grain size of the sand.

The mechanics of sand flowability have been studied by several investigators[1,2,3,4,5]. Up to the present time however, the American Foundrymen's Society has not adopted a sand flowability test. A quantitative test for sand flowability would help sand control technicians in developing mixtures that could be rammed to a smooth finish with the least amount of work. Experi-

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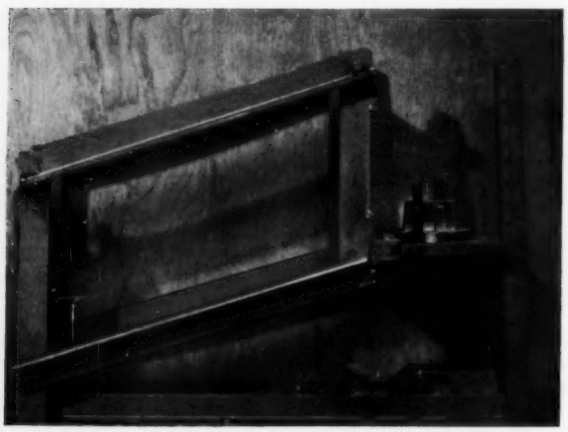


Fig. 1—Laboratory machine for measuring flowability.

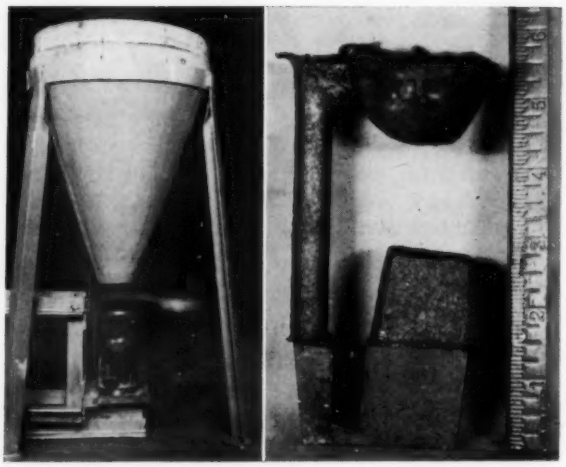


Fig. 2—At left, flowability machine set up for filling flask with sand. At right, test casting.



Fig. 3—Needle tipped dial indicator tests smoothness.

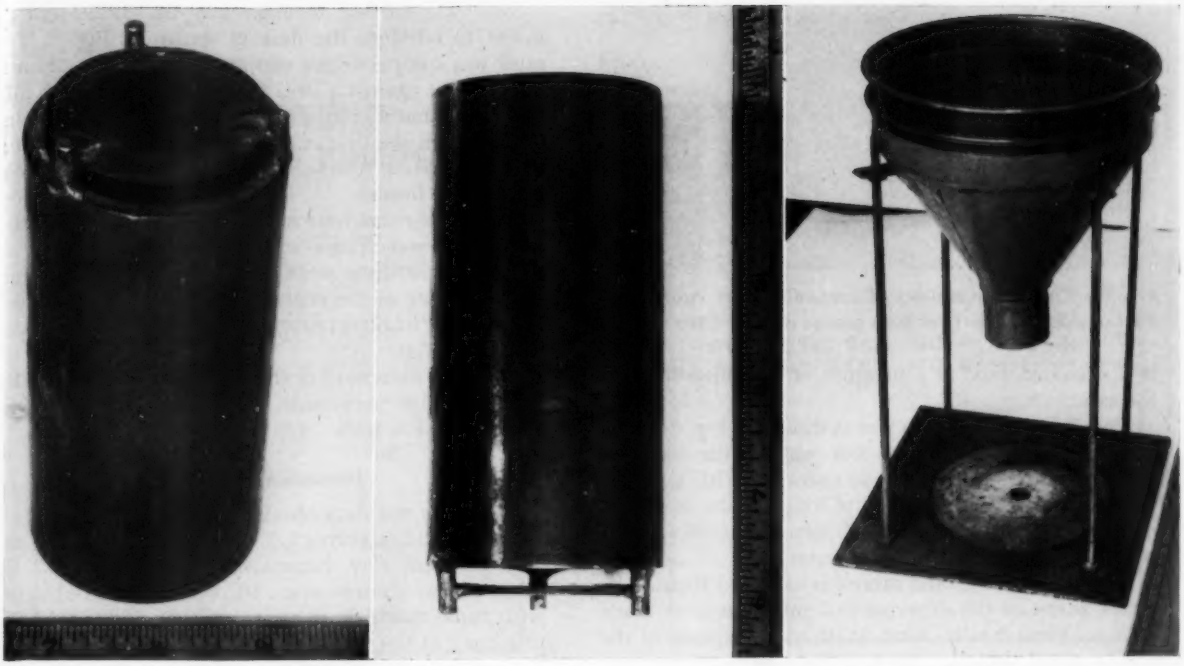


Fig. 4—Kennedy flowability tube (left and center) and hopper used to fill tube in making flowability measurement.

ments made to try and determine the value of three flowability test devices are described in this report.

Figure 1 shows the hand operated jolt machine with a mounted steel pattern. The test casting is 1½ in. square and ⅝ in. thick. Figure 2 shows the flask and flask extension in place under the filling hopper and a test casting. To measure surface finish, a needle tipped dial indicator (Fig. 3) is used. This method was proposed by Hobman[7].

Three instruments were used in an attempt to measure the flowability of sands. These are the Dietert, the Kennedy, and the Ramability Test. In the Dietert test, a standard 2-in. sample of sand is rammed in a specimen tube. A fourth ram is given the sand, then a dial indicator is placed to measure the length of the sand specimen. The sand is then given a fifth ram. The reduction in length of the specimen is recorded in thousandths of an inch. If the specimen is shortened 0.015 inches the flowability is reported as 85 per cent (100-15).

Disc Fastened to Ramming Tube

The Kennedy flowability test is shown in Fig. (4). A 2-in. diameter disc is fastened to a standard ramming tube by means of three pins so that a 3/16-in. space is left for the escape of sand. To make the test, the specimen tube is loaded by rubbing the sand through a 6-mesh riddle into a hopper. The sand is struck off level and the specimen tube is placed under the A.F.S. sand rammer. The rammer plunger-raising device is adjusted until the ramming head is just inside the end of the specimen tube, then the lever is released. A pan placed under the specimen tube catches the sand pushed through the orifices at the bottom of the specimen tube by the free fall of the rammer. The weight of

TABLE 1—MOLDING EFFORT VS. CASTING SMOOTHNESS[6]

Sand Mix	Number of Jolts Required to Get Optimum Casting smoothness	Dietert Flowability Number
5	8	82
2	16	83
1	24	83
6	32	80

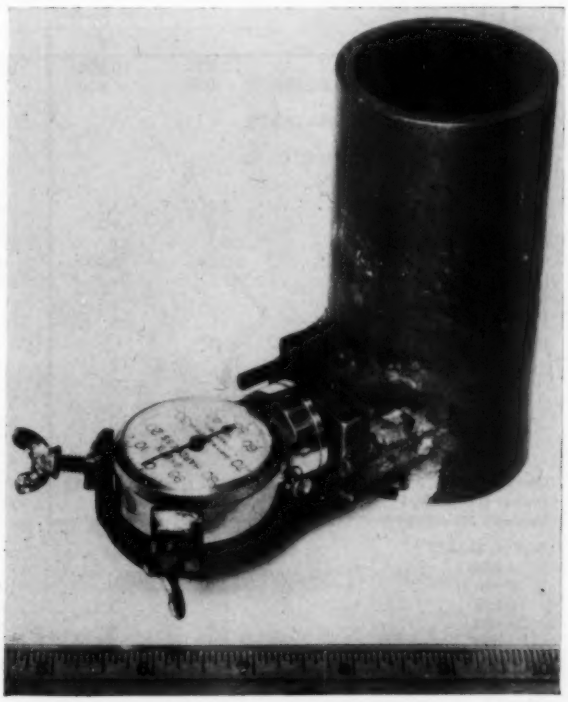


Fig. 5—Ramability tube and indicator as assembled.

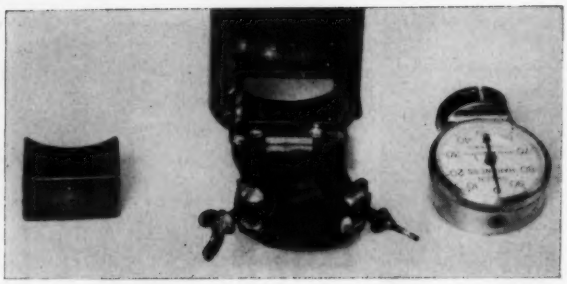


Fig. 6—Three components of ramability test equipment. Part of side wall (left) of tube moves as sand flows.

this extruded sand is a measure of the flowability of the sand.

The Ramability test device is shown in Fig. 5 and 6. Note that a section of the side wall of the standard specimen tube has been made movable. This movable piece is ¾ x 1⅜ in. and ¾ in. long. As the weight is carefully lowered by hand, displacement of sand is indicated on a mold hardness tester.

Table 2 gives the sand mixtures used and their properties. Seven of the experimental mixtures were made with a 100-mesh silica sand. With the exception of the rotten wood powder, all materials were commercial grades in common use. Table 3 shows the grain size of the sands used.

Experimental Procedure

In mixing, 10 kilogram batches of sand were run in a laboratory sand muller for 1 minute dry and 3 minutes wet. In preparing the core mixtures, the oil was added last. Tests were on a single batch of sand.

Sand was rubbed through a ⅛-in. riddle and allowed to fall into the flask as shown in Fig. 2. The sand was compacted by raising the mold 1½ in. and dropping it against a steel block. The number of jolts given each mold is shown in Table 2. The test casting was made in the drag. Copes were rammed up in core sand and baked. All molds were poured at 2100 F with 85-three-5's bronze.

Gates and risers were cut off in a bandsaw and each test surface was cleaned with 20 strokes of a wire brush. All surface readings were taken on the vertical surface of the casting at the center of the 1½ x 1½ in. sides. The surface finish is reported as the standard deviation of 25 readings.

With the exception of the three different flowability tests described previously, all of the other tests were standard A.F.S. tests.

Discussion of Results

All of the test data obtained are shown in Table 2. Molding sand mixtures 1, 2, and 3 contain 3.8, 7.4, and 10.7 per cent clay, respectively. Figure 7 shows the effect of clay content upon the surface finish obtained with these sands. It is apparent that with increasing clay content the flowability of the sand is reduced and a rougher surfaced casting is obtained. In this set of experiments, the Dietert flowability test and the Kennedy flowability test are correlated with casting finish results.

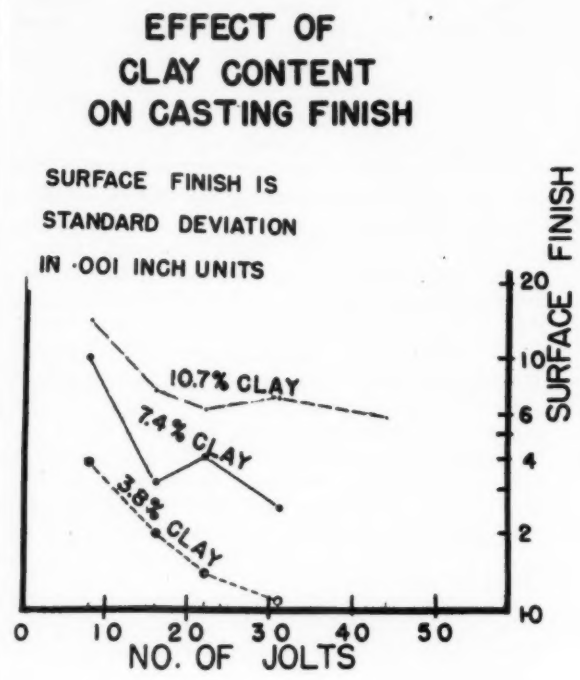
Core sand mixtures 5, 6, and 7 have green compression strengths of 1.6, 3.5, and 5.7 psi. Surface finish readings on castings made in these sands are shown in Fig. 8. Mixture 5 required only 4 jolts to get a smooth finish. Mixture 7 required 8 jolts, and mixture 6 re-

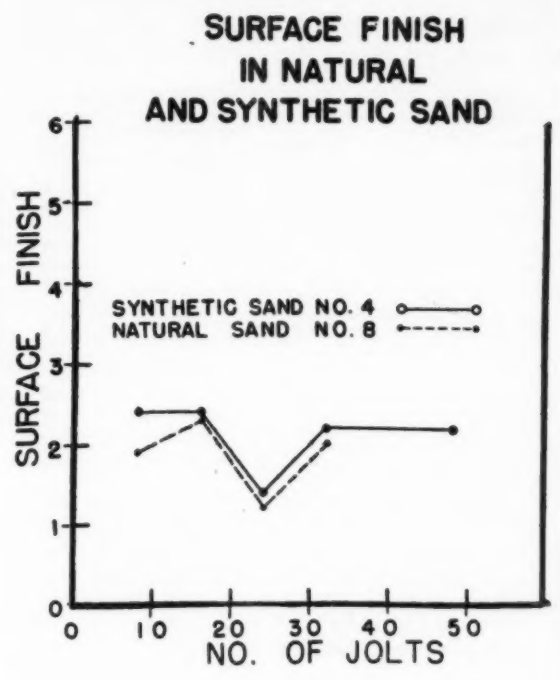
TABLE 2—SAND MIXTURES AND TEST RESULTS FOR SURFACE FINISH EXPERIMENTS

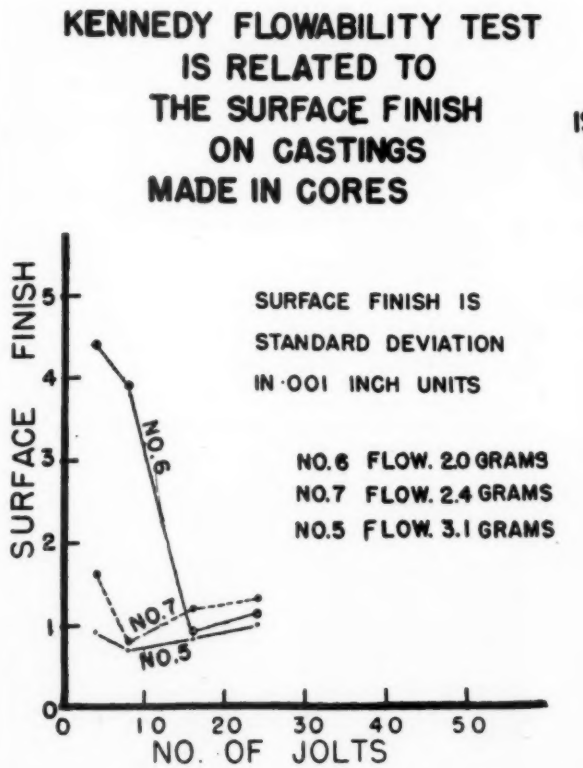
Sand Mix	Clay Bonded			Oil Bonded			Synth.	Nat.
	1	2	3	5	6	7		
No. 111 Sand, grams*	10,000	10,000	10,000	10,000	10,000	10,000	9,500	
South. Bentonite	400	800	1,200		200	350		
Core Oil				150	300	400		
Cereal Flour				50	50	150	50	
Iron Oxide							50	
Rotten Wood Pwdr.							30	
Silica Flour							500	
Moisture, %	5.4	5.3	5.0	4.4	5.4	5.0	5.2	8.0
Green Comp., psi	4.0	7.3	16.0	1.6	3.55	5.7	3.1	8.3
Permeability	38	37	37	39	27	24	35	9.5
Green Def., in./in.	0.025	0.031	0.027	0.021	0.025	0.028	0.025	0.019
Density, lb/cu ft	93	91.7	89	93½	94.5	95	94	105.5
Dry Permeability				50		43		
Dry Comp. Str., psi				60		1350		
Mold Hardness								
4 jolts	19	12		12	15	11		
8 jolts	24	20	20	17	21	19	23	35
16 jolts	38		31	20	33	30	33	43
24 jolts	44	30	35	30	36	36	43	
32 jolts	50	40	37					56
48 jolts			42				50	
Ramability, In.	0.027	0.0365	0.0312	0.019	0.023	0.016	0.0256	0.018
Dietert Flowability	85	78.8	70	89	85	82	82	75
Kennedy Flowability	1.65	0.97	0.55	3.1	2.0	2.4	2.0	2.8
Surface Finish**								
4 jolts				0.9	4.4	1.6		
8 jolts	3.9	10	14	0.7	3.9	0.8	2.4	1.9
16 jolts	2.0	3.2	7.3	0.82	0.9	1.15	2.4	2.3
24 jolts	1.4	4.0	6.2	0.98	1.1	1.29	1.4	1.2
32 jolts	1.1	2.5	6.8				2.2	2.0
48 jolts			5.8				2.2	

*All weights in grams.

**Surface finish in 0.001 in. units standard deviation.







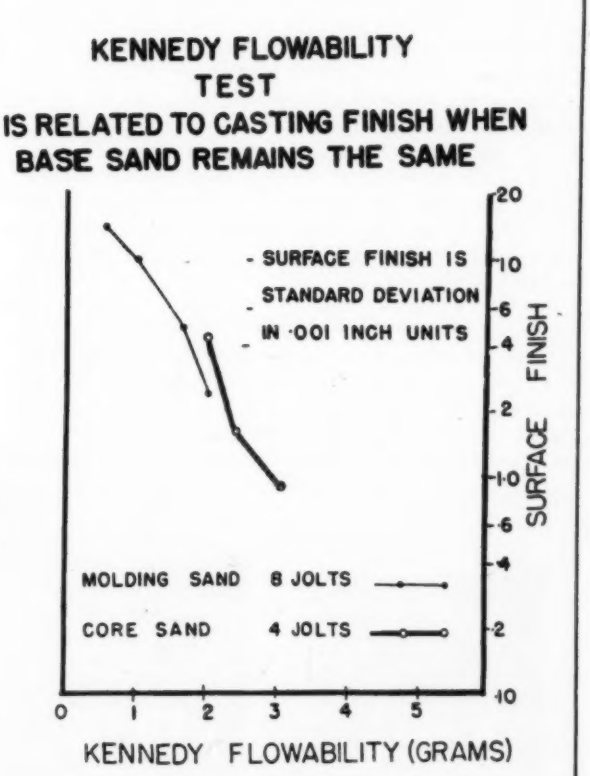


Fig. 7, 8, 9, and 10—Surface finish variations with sand type and composition and relation to Kennedy flowability test. Upper left, Fig. 7; Fig. 8, lower left. At right, Fig. 9 (upper) and Fig. 10.

quired 16 jolts. Note that the Kennedy flowability test is directly related to the number of jolts required.

As Fig. 9 shows, the high flowability of the natural sand tested was almost duplicated in the synthetic sand. However the green compression strength of the synthetic sand was reduced to 3.1 psi. to get the desired flowability. It would be helpful to know how to duplicate the high flowability of natural sands and at the same time obtain high green compression strength.

TABLE 3—SCREEN ANALYSES OF TEST SANDS

Size	Per Cent Retained	
	No. 100 Silica	Natural
10 Mesh	0.1	0.2
20 "	0.3	0.4
30 "	0.4	0.6
40 "	0.5	3.8
50 "	0.7	4.6
70 "	3.5	9.0
100 "	26.7	12.4
140 "	40.7	9.8
200 "	22.3	10.8
270 "	3.3	10.4
Pan	0.9	21.6
Clay	0.6	16.4

Test Swelling Tendency

Many of the castings were oversize or swelled. These castings were measured for thickness at the center of the 1½ x 1½-in. surface. These measurements, shown in Table 4, indicate that the tendency of the mold to swell and produce an oversize casting appears to be related to the ramability test.

Within the scope of this investigation, it seems possible to obtain a quantitative measure of flowability. Figure 10 indicates the correlation of Kennedy flowability test values with surface finish. Since surface finish of castings depends to a great extent upon the flowability of molding sands and cores, a quantitative test for flowability would be of great help to sand control technicians.

Practical Value of Kennedy Test

In the 1952 Progress Report of the A.F.S. Flowability Committee, a visual test for flowability is recommended. J. B. Caine suggested that the Kennedy flowability test be compared to the committee's test. For this comparison four sand mixtures were prepared,

TABLE 4—CASTING THICKNESS AND SAND RAMABILITY

Sand Mix	Thickness, In.	Ramability, In. Displacement
3	1.13	0.0315
2	1.06	0.0365
1	0.744	0.027
4	0.668	0.026
8	0.626	0.018

Pattern size, 0.622 in. In all cases mold was jolted 8 times.

and the results after running are shown in Fig. 11.

Sand specimens were rammed by slowly lowering rammer until full weight rested on sand. After one minute specimen was removed from tube. The Kennedy flowability test result is shown. Note that amount and size of surface supervoids are related to the Kennedy flowability test result. The sand which had a flowability of 3.2 grams contains no visible supervoids. The specimen of sand which had a flowability of 0.5 grams has many supervoids in the surface.

It would appear therefore that the two testing methods are measuring the same property of sands—the tendency to form supervoids on the rammed surface.

A description of the four sands shown in Fig. 11 is given in Table 5. Conclusions drawn from these tests are given on page 132.

TABLE 5—COMPARISON OF FLOWABILITY RESULTS

Sand Mix	Mixture	Mixing Procedure	H_2O %	Flowability Kennedy, Grams	Flowability A.F.S. Rating
1	10,000 grams No. 100 silica sand with 400 grams southern bentonite.	Dry 1 min., wet 3 min. in lab muller. Dried out, retempered, rubbed through sieve 3 times.	5.4	3.2	9 Trace of supervoids in corner away from rammer.
2	10,000 grams No. 100 silica sand with 800 grams southern bentonite.	Same as above.	5.4	1.9	7 Small supervoids in bottom third of specimen.
3	Same as above but 1,200 grams southern bentonite.	Same as above.	5.0	0.8	5 Larger supervoids in at least half of specimen.
9	10,000 lb No. 57 silica sand, 5% western bentonite, 1% corn starch.	Dry 1 min., wet 3 min., in foundry muller. Pass through aerator.	4.8	0.5	3 Large supervoids in about all of specimen.

For method of determining A.F.S. flowability rating see Report of A.F.S. Flowability Committee, in this volume.

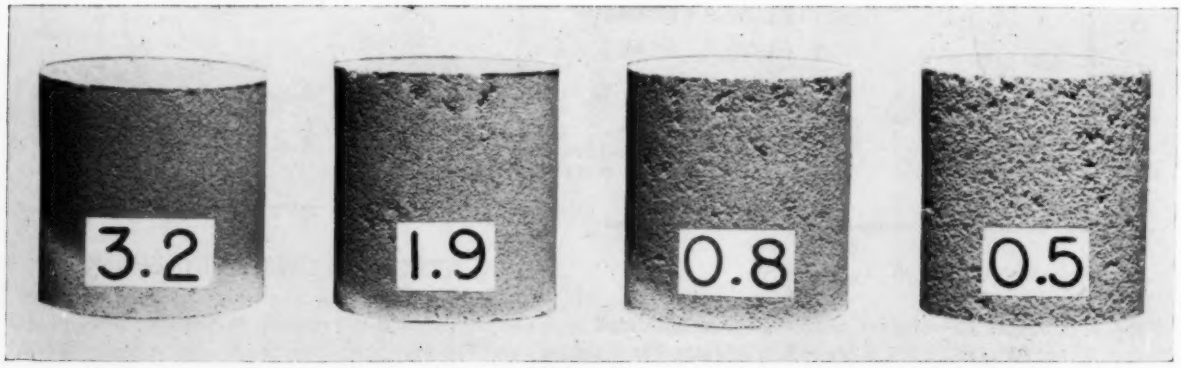


Fig. 11—Flowability test specimens showing relation between Kennedy flow value and number of supervoids.

Within the limits of this investigation the following conclusions can be drawn:

1. Casting finish depends in part upon the ability of sand to flow under ramming.

2. The Kennedy flowability test was shown to be related to casting finish on 8 different sand mixtures.

3. The A.F.S. Flowability Committee test procedure was shown to correlate with the Kennedy flowability test procedure on 4 different sand mixtures.

Acknowledgment

The support and encouragement of Neil Kennedy of William Kennedy & Sons, Ltd., is appreciated by the authors.

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7. A. Hobman, "How to Measure Surface Roughness of Castings," AMERICAN FOUNDRYMAN, Oct. 1949, p. 46.

DISCUSSION

Chairman: J. B. CAINE, Foundry Consultant, Cincinnati.
Co-Chairman: W. S. PELLINI, Naval Research Laboratory, Washington, D. C.
Recorder: P. E. KYLE, Cornell University, Ithaca, N. Y.

MR. FAIRFIELD: Mixing time has a great effect on surface flowability. A mixing time of 30 sec results in a high flowability. Longer mixing times will lower flowability and tend to produce rougher castings.

J. F. WALLACE: [1] Would you expect to get the same effect by lowering the clay and water contents?

MR. FAIRFIELD: Less clay and water will result in better flowability.

G. J. GROTT: [2] If you are using only one sand, it is immaterial which flowability test is used.

W. S. PELLINI: [3] Is it not true that anything which increases bond decreases flowability?

MR. FAIRFIELD: Yes.

D. C. WILLIAMS: [4] What was the order of mixing?

MR. MCCONACHIE: The dry binder is added and mixed for 30 sec. The water is then added and mixing continues for another minute.

MR. WILLIAMS: The order of mixing might have an effect on the results.

MR. GROTT: Was the moisture content constant during mixing?

MR. FAIRFIELD: Yes.

H. W. DIETERT: [5] An increase in strength may lower flowability for some binders, but some of the newer materials being introduced develop high flowability at high bond strengths.

D. J. JONES: [6] If sand is tempered beyond the moisture required for maximum green compressive strength will the same weight of the mixture required at optimum temper still give a 2-in. high specimen?

MR. FAIRFIELD: Wet sand rams more densely in the center but there will be more voids in the surface.

D. C. EKEY: [7] How was the statistical study made?

MR. FAIRFIELD: Using the methods described in the A.S.T.M. Manual on Quality Control of Materials, Standard Deviation was determined on no less than 20 readings.

MR. EKEY: To what was the deviation reported referred?

MR. FAIRFIELD: The surface roughness was determined by taking 25 readings and the average of these readings was taken as the average surface. Deviation was from this average surface.

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[1] Watertown Arsenal, Watertown, Mass.
[2] Unitcast Corp., Toledo, Ohio.
[3] Naval Research Laboratory, Washington, D. C.
[4] Ohio State University, Columbus, Ohio.
[5] Harry W. Dietert Co., Detroit.
[6] New Jersey Silica Sand Co., Millville, N. J.
[7] Pennsylvania State College, College Station, Pa.

RESEARCH IN PATTERN COATING PRODUCES QUALITY CASTINGS

By

Charles J. Berg*



Checking surface finish of patterns is an important step in producing quality castings, whether they be street car crossings (above) or smaller production units.

ABSTRACT

Types of coatings commonly used as pattern coatings are briefly reviewed and various data are presented comparing the package, application and dry film characteristics of shellac, crankcase sealer and seven other pattern coatings. These data are discussed and the shortcomings and virtues of the different coatings are pointed out. Cost factors are considered and the inherent economies of higher solids coatings are stressed. Application techniques and shop conditions are discussed and several suggestions concerning these problems are offered.

■ Even before a pattern is made, much thought and effort go into its design and layout. The best woods are carefully selected with cost a secondary objective. A pattern is then made from these woods by highly skilled men with years of training and experience. The result is a product of great precision and high value.

Since the pattern represents a sizable investment, the selection of an organic coating to protect this product merits some consideration. A coating can be selected that has been carefully formulated for this specific end use, one that will conform to the exacting standards set up for the pattern itself. It is also possible to select an inferior coating, one that offers no practical protection at all.

Today, shellac is commonly used as a pattern coating and it has been used for this purpose for a good many years. Shellac unquestionably has some merits or it wouldn't continue to be popular. However, through years of development and research many advances have been made in organic coatings which have relegated shellac to a position of inferiority by comparison. There are many types of coatings available that meet the demands of a pattern coating much more fully.

Crankcase sealer is also used as a pattern coating. Actually crankcase sealer is a good organic coating for finishing crankcases, but not necessarily for protecting patterns. As its name implies, it has been formulated for a specific end use. This type of product has some virtues, among them initial low cost per unit of volume. However, in the final analysis, crankcase sealer

* Industrial Project Chemist, Sherwin-Williams Lacquer Laboratory, Chicago.

TABLE 1—PACKAGE AND APPLICATION CHARACTERISTICS

Pattern Coating	Build[1]	Total Solids	Brushability	Drying[2] Time	Drying[3] Time Over Wax	Gloss[4]	General Appearance	Odor
Shellac	0.8	35.0	7	60 min	Same	68	4	9
Crankcase Sealer	0.3	25.0	6	15 min	Same	27	6	6
A	2.5	47.0	10	30 min	Same	93	10	9
B	1.0	33.0	8	30 min	Same	60	7	7
C	1.3	37.0	9	40 min	Same	53	8	6
D	1.0	42.0	6	10 min	Same	26	5	6
E	2.0	54.0	9	30 min	Slower	78	9	5
F	0.8	43.0	7	24 hr	Much Slower	76	6	8
G	0.7	41.0	7	15 min	Same	62	7	7

1. Build expressed in thousandths of an inch obtained from one coat applied by brush.
2. Drying to handle at room temperature, approximately 70 F.
3. Equivalent to drying over wood or slower.
4. Gloss reading obtained on wood using Gardner Gloss Meter. A gloss of 100 is equivalent to a mirror surface and a gloss of 0 is equivalent to a perfect lustreless surface.

will cost just as much, if not more, than a coating formulated specifically for the protection of patterns and it does not provide the maximum protection available.

Several unfavorable comments have now been made regarding shellac and crankcase sealer. One of the purposes of this paper is to present factual data showing why and how these, and other protective coatings, are unsuited for use as pattern coatings. The data contained in this paper were obtained from a series of tests conducted on a representative cross section of pattern coatings now available. Just because a paint can has a label stating that it is a pattern coating is no guarantee that it is good for this purpose.

It will be seen that among the coatings being represented as specifically designed for use on patterns, some are quite obviously better than others. The series of tests referred to above, was correlated as closely as possible to the actual conditions that a pattern is subjected to in use. It is hoped that the results of these tests, as presented in this paper, will help the patternmaker differentiate between the good and the bad and aid in the selection of a pattern coating that offers the maximum in protection and quality.

Background of Research on Pattern Coatings

The paint industry has the knowledge and experience to formulate various protective coatings, but before any coating can be properly formulated the user must be consulted concerning the special properties desired. For this reason the patternmaker and foundryman were consulted before good pattern coatings could be developed. It was found that past experience could be relied upon to provide many of the properties desired by the users of pattern coatings. However, some of the properties desired were unique and considerable research was required before such properties were successfully incorporated into a pattern coating.

Before presenting the results of the tests that were conducted, a brief discussion concerning the various basic film formers used in pattern coatings is in order. Previously shellac and crankcase sealer have been mentioned. Shellac is a type of coating itself, being a natural resin of animal origin. It is fairly expensive, soluble in alcohol, and releases its solvent slowly.

Crankcase sealer falls within a group that is known as nitrocellulose lacquers. This type of coating is the most widely used for the protection of patterns. It is interesting to note that cellulose lacquers, when properly formulated, are among the best pattern coatings, even though crankcase sealer falls within this classification. Since nitrocellulose lacquers are so widely used as pattern coatings several of them have been included in the evaluation.

Some varnishes and enamels are also used as pattern coatings, but because of the long drying times of this type of finish they are generally considered to be unsuited for this use. However, one pattern coating employing this type of basic film former has been included in the evaluation. Other types of pattern coatings have rubber or a rubber derivative as the basic film former and it was felt this type of coating was worthy of consideration in the evaluation. A close cousin of nitrocellulose, ethyl cellulose has been used in pattern coatings and one such product has also been evaluated through the series of tests.

Vinyl coatings have recently received considerable publicity. Vinyls do have very good abrasion and chemical resistance. However, it is generally conceded that vinyls have very poor adhesion to wood unless an undercoat is employed. The solvents used in vinyl coatings also have strong unpleasant odors. A sample of a vinyl pattern coating was not available for this

TABLE 2—DRY FILM CHARACTERISTICS

Pattern Coating	Pencil Hardness	Flex-[1] ibility	Abrasion[2] Resistance Mg Lost in 500 cyc.	120 F Print Resistance	Linseed Core Sand Resistance	Water Soluble Core Binder Resistance	Gasoline Resistance	Water[3] Penetration % Water Absorbed 24 Hr	Water[3] Penetration % Water Absorbed 120 Hr
Shellac	HB	½ in.	66	*V. Poor* Bad Print	*V. Poor* Sticking & Softening	*V. Poor* Softening & Sticking	*Good* Sl. Softening	3.1	8.6
Crankcase Sealer	1H	1 in.	45	*Excellent* No effect	*Excellent* No effect	*Poor* Softening & Sticking	*Fair* Softening	1.2	4.0
A	3H	¼ in.	35	*Excellent* No effect	*Excellent* No effect	*Excellent* No effect	*Excellent* No effect	0.88	2.9
B	1H	⅛ in.	40	*Fair* Sl. Print	*Good* Sl. Stick.	*Poor* Softening & Sticking	*Good* Sl. Softening	1.82	7.6
C	F	1 in.	47	*Good* V. Slight Print	*Excellent* No effect	*Poor* Softening & Sticking	*Fair* Softening	0.85	2.8
D	1H	3 in.	70	*Excellent* No effect	*Excellent* No effect	*Excellent* No effect	*Excellent* No effect	5.1	Coating Cracked and Peeled
E	1H	1 in.	67	*Poor* Print	*Poor* Sticking & Softening	*Fair* Softening	*Excellent* No effect	0.34	1.5
F	HB	⅛ in.	30	*Fair* Sl. Print	*Excellent* No effect	*Poor* Sticking & Softening	*Fair* Softening	1.98	6.2
G	2B	6 in.	67	*Poor* Print	*Poor* Sticking & Softening	*Fair* Softening	*Good* Sl. Softening	0.75	2.5

1. Diameter bend at which cracking occurs.
2. 500 cycles on Tabor Abrasor.
3. Mahogany blocks coated and immersed in water.

evaluation, but this is not considered as a serious omission.

Package and Application Characteristics

The first properties to consider are those of the coating while in the package and those encountered in the application of the coating. Among these properties are solids, odor, brushability, drying characteristics, build, gloss and appearance. Such factors will determine, among other things, how many coats are required and how soon the pattern can be used. These properties are presented in Table 1.

Certain characteristics cannot be evaluated by counting up the results on an adding machine. The human element must be relied upon to judge some things, for example, brushability. The only possible way to evaluate brushability is to take a brush and apply the coating to a representative surface. Results obtained in this manner are presented as a numerical rating, starting with ten as the highest rating and going down to one as the lowest rating. Such ratings are not based on the judgement of one person, but are the average rating obtained by a number of experts, each making an individual evaluation.

In reviewing Table 1 it will be found that many of these characteristics are related to each other. Build is a good starting point for this discussion. The figure given in this table is the dry film thickness in thousandths of an inch (mils) obtained with one coat applied by brushing. No matter how good any pattern coating is, there has to be enough of it present before protection will be obtained. Some coatings have high enough build to give adequate protection with one coat, others do not have near enough build for one coat to suffice. The recommended minimum film thickness necessary for protection is one thousandth of an inch (1 mil). For good protection of wood it is best to have even higher build, preferably two thousandths of an inch (2 mils) or more.

Build is related directly to solids. It is quite obvious that no matter how much of a coating is applied to a surface there will be little left when the solvents evaporate unless the coating has a reasonable amount of solids. High solids contribute much to high build, good appearance and adequate protection.

Brushability is also related to build. It is that quality of a coating which pertains to the ease with which it brushes and how much can be applied before sagging or running occurs. Water certainly brushes easily, but how much water could be applied to a vertical surface before it would run? To have good brushability a coating should definitely brush well, but it must also have

proper body so that a reasonably thick film can be applied before it starts to sag. On the other hand, too much body will cause a drag or pull and the coating will be hard to brush. Final build is dependent on the amount of solids in the coating and on the thickness of the wet film applied.

Too Fast Drying Reduces Brushability

Drying time is the next logical consideration, for drying time ties in with brushability. Naturally, rapid drying is usually desired so that a second coat can be applied soon after the first coat, if a second coat is needed. Rapid drying is also desired so the pattern can be moved and put into use as soon as possible. However, drying that is too fast definitely interferes with ease of brushing. The film must stay wet for a reasonable amount of time so that areas can be lapped into each other without leaving obvious ridges. A nitrocellulose lacquer can be made to dry within a matter of a few minutes, but in a good brushing lacquer the drying is deliberately retarded to promote good brushability. Thirty minutes is about as fast as any coating can dry and still have good brushing qualities.

Another problem to be considered in a pattern lacquer is drying over wax. The ability of a coating to dry over wax is dependent upon the type of solvents used in the coating. If the proper solvents are selected no trouble will be encountered. However, the properties of some film formers are such that certain solvents are mandatory and these solvents sometimes do not permit good drying over wax.

The problem of application over wax calls attention to the fact that pattern coatings are at times applied over surfaces other than wood. Examples of such surfaces are plastics, plaster, and metal. There is little drying problem connected with these surfaces, but there may be a problem of adhesion. The property of adhesion over wood has not been discussed because all the coatings evaluated were found to have excellent adhesion to wood and any attempt to differentiate between them in regard to this property would be extremely difficult.

Some coatings were tested for adhesion over plastic fillets and metal with excellent results. A general comparison was not made, but it is very probable that adhesion problems may be encountered when some coatings are applied to surfaces other than wood. However, pattern coatings are available that will give satisfactory adhesion to these surfaces.

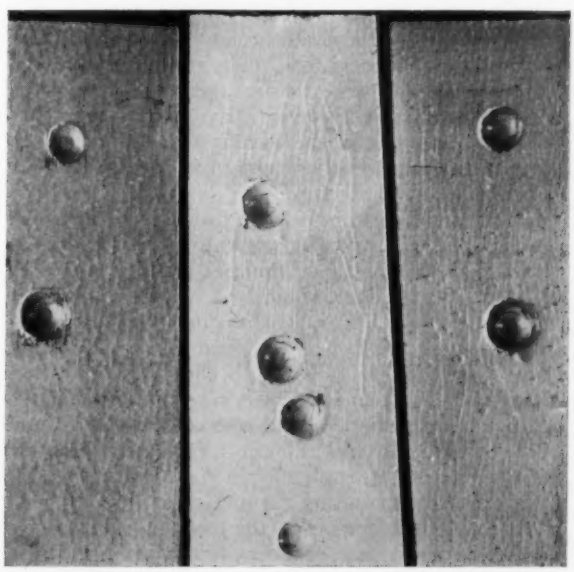
Gloss is not important from the standpoint of protection, but when a quality product is produced and finished most people prefer that it have a pleasing appearance. Hence, from the standpoint of appearance gloss is very important. There is no reason why a good organic coating should not have a high gloss and it will usually be found that good coatings do have good gloss unless they are deliberatedly "flatted" for a purpose. The gloss readings presented in the table were made on the Gardner 60° Gloss meter and a reading of 100 would represent a perfect gloss.

General appearance is a composite of build, gloss, leveling, holdout and that property of not raising the grain. Build and gloss have been discussed. Leveling is the ability of a finish to flow out uniformly and leave no brush marks. Holdout is quite important. Some strike-in is desired to obtain satisfactory adhesion, but the problem is usually to keep the coating from penetrating the wood excessively and giving the appearance of nothing on the surface. Finally there is grain raising, which is related to holdout. The better the holdout the less tendency there is to raise the grain. Grain raising is actually a swelling of the wood fibers and is caused by absorption of the thinners in the coating. Some thinners are definitely worse than others in their grain-raising tendencies.

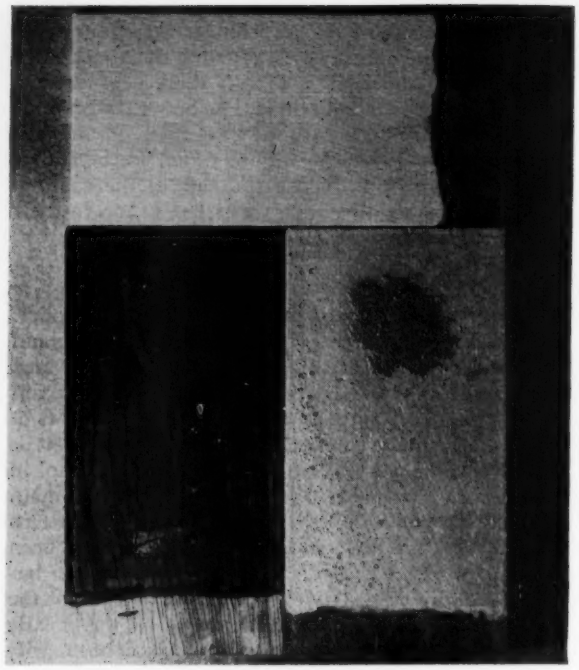
Shellac Used as Odor Standard

The final characteristic presented in Table 1 is odor. In a pattern coating odor is a very important thing and the paint formulator is very conscious of this fact. Much research work has been done to improve paint odors, not only in pattern coatings, but in all types of coatings. If shellac has one outstanding virtue, it is odor. Very few people consider the odor of alcohol in shellac as disagreeable. Odor is probably the main reason why shellac is still widely used as a pattern coating. For this reason shellac has more or less been established as a standard for odor in this pattern lacquer evaluation. In arriving at the ratings on odor the number of persons making the evaluation was greatly increased and the opinions of others, not necessarily paint experts, were also sought.

From the data presented in Table 1 it can now readily be seen that both shellac and crankcase sealer along with several other pattern coatings, have inferior build. Several others have acceptable build, but there are only two coatings that have high enough build to insure good protection with one coat application, Coating A and Coating E. The drying time of shellac is slow in comparison to the majority of these coatings. Actually the drying time of shellac will be further retarded by humid conditions due to its affinity for moisture. The drying time of crankcase sealer is too



Adhesion and flexibility under impact are shown by these wood panels finished with coating G (left), coating A (center) and coating D (right).



Mahogany panels finished with shellac (lower left), coating E (lower right) and coating A (top), illustrating effects of linseed oil-core sand resistance test. Shellac and coating E show serious sand sticking.

fast for proper leveling and good brushability. Crankcase sealer is low in gloss and shellac has only fair gloss. Shellac and crankcase sealer both rate below most other coatings in appearance. As stated previously, shellac does have a very acceptable odor, but Coating A has an odor that is as good.

Dry Film Characteristics

The dry film characteristics are presented in Table 2. This includes hardness, flexibility, abrasion resistance, heat resistance, water resistance, and resistance to core and and core sand binders. The dry film characteristics are a measure of the coating's ability to protect the pattern from the various external conditions it may be subjected to.

Hardness is commonly determined by means of pencils. The film receives the rating of the hardest pencil lead which will crumble rather than dig into the film. The hardest pencil used in such determinations is usually a 7H and the hardness then decreases down through the H's to 1H, then F, then HB then 1B, and up through the B's to 7B, the softest. Hardness is desirable in a paint film if it is not attained at the sacrifice of flexibility. A hard inflexible film will tend to chip, flake, and crack. Flexibility for this evaluation was determined by applying the coating to a flexible surface and then bending the surface to see how small a bend could be made before the coating cracked.

From flexibility and hardness, toughness or abrasion resistance can generally be predicted. A good balance between hardness and flexibility must be attained before a coating will have good abrasion resistance. The results presented in Table 2 were obtained with a Tabor Abrasor. The coating is first applied to a metal plate and allowed to dry 72 hours. The metal plate is then rotated in a circular motion against an abrasive wheel which is held against the coating at a constant pressure. By weighing the metal plate before and after the test the loss of weight because of the coating being abraded away can be determined. The smaller the loss in weight the better the abrasion resistance. For this test each coating was run for 500 cycles against a CS 10 abrasive wheel. This test can be compared to the abrasive action of sand and from the results, the wearing properties of these pattern coatings in relation to each other can be predicted.

The print resistance test conducted for this evaluation is a measure of the ability of the coating to withstand pressure at elevated temperatures without softening. This test is conducted at a temperature of 120 F which certainly cannot be considered extremely high. The film is allowed to dry 48 hours before being subjected to the test, which is simply subjecting the film in contact with cheesecloth to a pressure of 1 psi for one hour at the elevated temperature. The severity of the resulting impression, if any, is then observed. A coating that softens at 120 F is not practical for use on patterns.

Two very interesting tests were conducted with core sand mixtures. One using the common linseed oil binder and the other using the urea-formaldehyde, water-soluble core binder. These mixtures were applied against the dried films under pressure and the resulting amount of sticking, and film softening observed. These tests are very important because any worthy pattern coating should stand up very well under such conditions. Softening will result in ultimate film breakdown, and sticking, can very easily ruin the core itself.

Pattern coatings may be expected to come in contact with kerosene or gasoline and for this reason gasoline resistance tests were made. Coated wooden panels were immersed in gasoline for 24 hours and any softening, blistering or wrinkling was noted. No severe failures were observed, but several coatings softened noticeably.

Test Resistance to Moisture

One of the most important tests is the water resistance test. In this test small mahogany blocks were completely coated by dip application. These blocks were first weighed and then completely immersed in water. After 24 hours the blocks were removed and weighed again. They were then put back in water for 120 hours and then weighed a third time. In this manner the water penetrating the coating and being absorbed into the wood was determined. This test is a measure of the ability of a coating to seal wood against water. The importance of this test to the patternmaker is self evident. Some coatings not only failed as effective moisture barriers, but cracked and peeled from the surface of the wood.

A good pattern coating should show up well in all these tests. Just being outstanding in one particular test does not qualify a coating as being good. Particularly important are abrasion resistance, print resistance, the core sand resistance tests, and the ability to seal wood against water. Shellac makes a very poor showing in all these important properties. Crankcase sealer actually has better resistance to some of the tests than

some of the special pattern coatings. However, crankcase sealer falls below par on water-soluble core binder resistance, gasoline resistance and water penetration. Coating F has the best abrasion resistance but fails many of the other tests. Coating E has the best moisture sealing properties but shows poor abrasion resistance and a bad tendency to soften under any adverse conditions. Coating B also has good abrasion resistance, but is only fair in most of the other tests and has poor sealing properties. Coating A has very good abrasion resistance, very good moisture sealing ability and excellent resistance to all other tests. Coating C also has good abrasion resistance and very good sealing properties, but does not compare as well on the other resistance tests. Coating D fails badly on abrasion resistance and moisture sealing, but shows excellent results in all the other tests. Coating F is inferior in almost every respect, except as a moisture barrier.

Cost Considerations

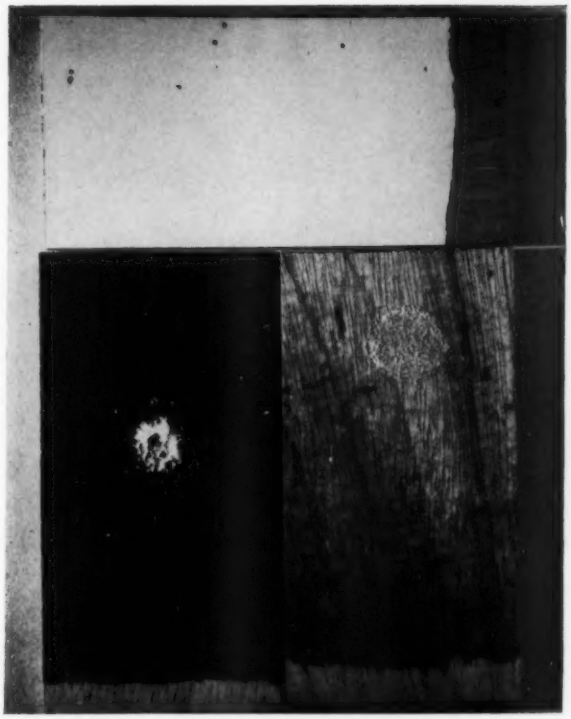
When considering the cost of a coating, one of the most important factors to take into account is the amount of solids or film-forming ingredients. The solids are the costly part of any coating. When the coating is applied and dries, all that will be left is the solids, and the thinner that was paid for will all be gone. If the coating was low in solids and high in solvent, most of what was paid for is lost. Naturally, the lower the solids the lower the cost. However, more of the low solids material will be required to equal the film thickness and protection attained with the higher solids coating. All the excess thinner and paint cans accompanying the low solids material represent costs of material for which the pattern maker does not receive any protective coating.

Another important factor is application time. High solids coatings permit a pattern to be properly covered with one coat. This cuts application time in half. or in some cases to one third. Time is money and therefore costs are again decreased through the use of higher solids coatings.

Last, but not least, is quality. It stands to reason that in a quality coating better raw materials must be used and this fact tends to raise the cost of higher quality coatings. However, it is well worth a few additional pennies to obtain the best protection available. After all, the cost of the pattern coating is but a small fraction of the cost of the entire pattern, but the function that it performs in protecting a product of considerable worth can be invaluable.

Shop conditions are important. There are no practical protective coatings that are absolutely odorless. For this reason the best ventilation possible should be employed. The painting area should be kept as clean as possible. In dusty areas keeping the floor moist or oiled will be of considerable help in keeping the air clean. Any contamination in the air will naturally adhere very well to a freshly painted surface and the result is a rough finish. Here again proper ventilation will be of great assistance in keeping air borne contamination to a minimum. The use of a fast drying coating will also minimize the effects of poor shop conditions.

Most pattern coatings are applied with brush and the selection of this brush is quite important. A coarse, short bristle brush will leave brush marks and cause streaking, sagging, and bubbling. The purchase of natural bristle brushes of reasonable bristle length will prove very worth while and the cost of such brushes is not excessive. A decent brush produces a much better appearing finish and permits the application of heavier coats without sagging. When applying the coating to a vertical surface the tendency to sag is also minimized if the final brush strokes are made verticallv.



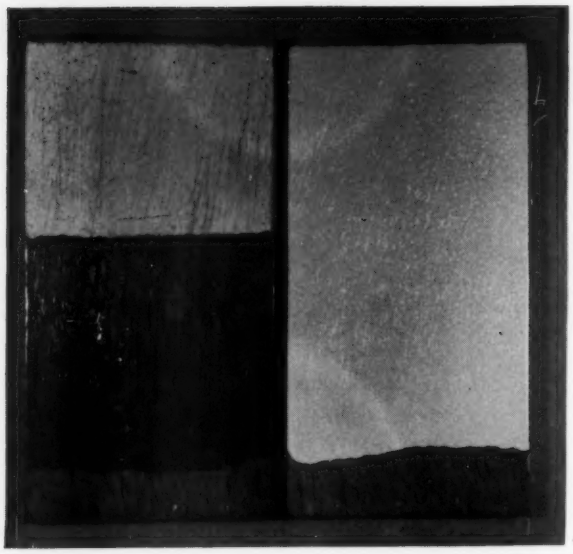
Mahogany panels finished with coating B (lower left), coating F (lower right) and coating A (top), demonstrating effects of water soluble core binder-core sand resistance test. Coating B shows moderate sticking and coating F shows very bad sticking in this test; A shows no effect.

At present, spray application is of minor importance in the pattern industry. However, it might be profitable for some of the patternmakers to investigate this possibility, especially the bigger shops. Spraying does require a spray booth, but the exhaust system of the booth will carry away most of the odors. Spray application is faster, permits faster drying and heavier application, gives better coverage on corners and results in better appearance.

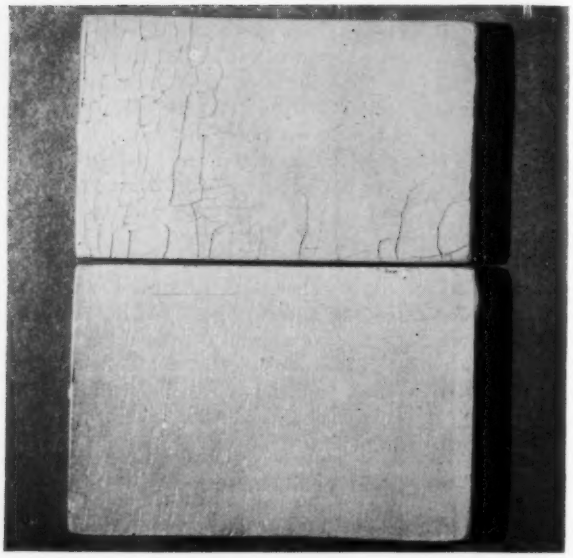
Guide to Choosing Pattern Coating

The purpose of this paper has been to show the members of the pattern and foundry industries what is available in a pattern coating. The inferior qualities of commonly used coatings have been stressed and facts have been presented to substantiate these claims. It can be seen that no pattern coating is perfect.

In the selection of a pattern coating the following items should be carefully considered as a guide to



Wood panels with shellac (left) and coating A (right) after water immersion. Note severe discoloration of the shellac because of noticeable affinity for moisture.



Wood blocks finished with coating D (top) and coating A (bottom) after 24-hour water immersion. The failure of coating D, through cracking, is readily apparent.

obtaining the best available product:

1. The solids should be high and brushability good so that sufficient build can be obtained with one coat to provide ample protection.
2. The drying time should be reasonably fast, in the neighborhood of 30 to 45 minutes. The coating should dry equally well over all surfaces.
3. The gloss should be high, grain raising tendencies low, and the coating should level out well if good appearance is desired.
4. The odor should be mild and acceptable.
5. The film should be hard and flexible so that abrasion resistance is high, providing long life before recoating is necessary.
6. The film should be unaffected by reasonably high temperatures.
7. The film should be resistant to core binders and release agents and should not cause sand sticking at reasonable pressures.
8. The film should be unaffected by gasoline or kerosene.
9. The film should be an effective seal against water penetration.

It is hoped the data and comments presented will enable the users to select a pattern coating, such as Coating A, that will do a quality job for them, for such pattern coatings have been made available through extensive research. While the paint industry is justifiably proud of such coatings, there is still some room for improvement. The paint industry is highly competitive and patternmakers can demand and obtain from it pattern coatings as they want them.

A TECHNIQUE FOR CASTING TITANIUM

By

O. W. Simmons* and Hugh R. McCurdy**

ABSTRACT

An electric arc furnace has been developed at Frankford Arsenal for making castings of titanium which weigh about 1½ lb. Melting and casting are carried out in an enclosed water-cooled, gas-tight copper box to prevent contamination. Heat is introduced directly into the metal by a direct current arc maintained between a water-cooled, tungsten-tipped electrode as the cathode and the bath as the anode. The charge consists of a 12½ lb slug of titanium. The heat introduced by the arc melts a pool of metal weighing about three lb. The unmelted titanium "skull" serves as a crucible to hold the molten metal. The skull must be of the same alloy composition as that desired in the bath.

An inert atmosphere of purified argon stabilizes the arc and protects the hot titanium from atmospheric contamination. The casting is formed by pouring into a mold by machined graphite.

■ The Army Ordnance Corps in recent years has been vitally interested in developing titanium alloys so they may be substituted for the more common metals in ordnance applications. This interest is encouraged because titanium is relatively abundant in the United States, being the fourth most prevalent structural element after aluminum, iron and magnesium. Furthermore, many titanium alloys are light, strong, ductile, corrosion resistant and have good mechanical properties over the temperature range between—75F and 800F (—60C and 427C).

Titanium products are now available from several commercial producers in this country, but these products are entirely in the wrought form and are limited to plate, sheet, forgings, and a small amount of tubing and wire. No commercial source of titanium castings is known although there is some work directed toward development of casting techniques[1]. The problems of casting titanium are particularly difficult because titanium has a high melting point and is extremely reactive in the molten condition. Although the casting of titanium is a complex process it may have economic justification because of the difficulties in machining titanium, and titanium alloy parts from wrought bars.

Frankford Arsenal Arc Furnace

Figure 1 is a cross-sectional view of the arc furnace used to melt and cast titanium alloys at Frankford Arsenal. The essential features of the furnace include the gas-tight water-cooled copper crucible and cover, the water-cooled tungsten-tipped electrode which has a 22 degree bend at its melting end, the electrically insulated electrode guide bushing in the cover, the inert argon atmosphere, the mold chamber, the graphite mold, the water-cooled copper mold-door and the trunnions for tilting the entire assembly. Figure 2 shows the furnace (with all the parts identified) open

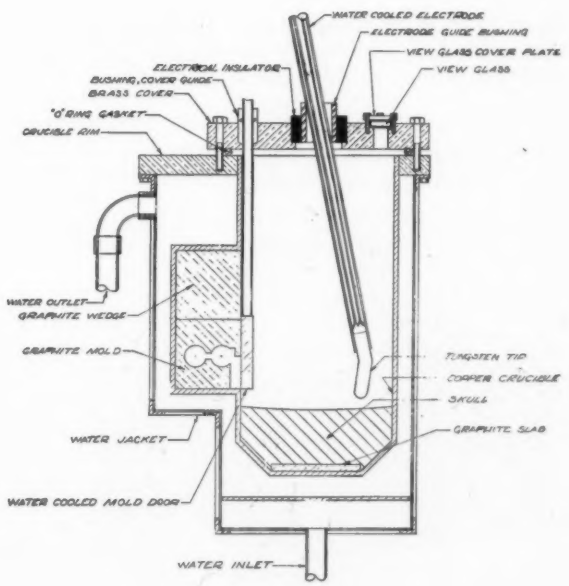


Fig. 1—Diagram of the tilting arc-casting furnace assembly for melting and casting titanium.

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for loading; and Fig. 3 tipped forward for pouring.

Two sight glasses for observation of the melting operation are mounted in the brass furnace cover which is tightly sealed to the crucible rim by means of a rectangular O-ring. The moveable water-cooled electrode passes through a water-cooled stainless guide bushing which is sealed into, but electrically insulated from the cover by a micarta ring. Sleeves of gas mask tubing, not shown in the drawing, provide flexible seals for the electrode and the mold door. Heat is furnished by a direct-current arc maintained between the negatively charged electrode and the titanium skull acting as the anode. Power for the arc is provided by two 600-ampere direct-current welding generators connected in parallel.

The mold is made up of graphite sections which are machined to shape and incorporate adequate draft so that they may be readily stripped from the casting after use without damage to the mold (Fig. 4A).

Operation of Furnace

Arc Melting Atmosphere. The cover is bolted onto the crucible so that the O-rings make a gas-tight seal. The retractable water-cooled door is adjusted so that it covers the mold as shown in Fig. 5A. The melting chamber and its charge are evacuated to a pressure of about one centimeter of mercury, absolute, by means of a mechanical pump, filled with argon (99.96+ per cent pure), and re-evacuated and refilled two more times. During this final refilling operation the argon atmosphere is adjusted to a positive pressure of about two to three centimeters of mercury. The argon serves the dual purpose of providing an inert atmosphere to protect the molten titanium and of permitting the formation of a stable, easily controlled arc.

Fig. 2—Tilting arc-casting furnace opened for loading reveals the water-cooled mold door and tungsten-tipped electrode. Note extensive water cooling required.

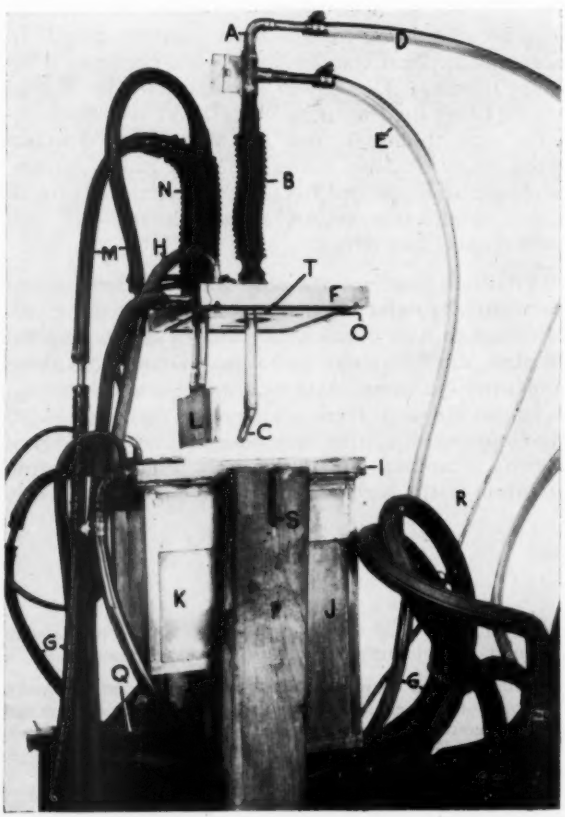
A. Electrode and terminal lug
B. Electrode flexible sleeve
C. Electrode tungsten tip
D. Electrode water inlet
E. Electrode water outlet
F. Furnace cover
G. Water hoses
H. Argon outlet and evacuating port
I. Crucible rim
J. Water jacket
K. Mold Chamber
L. Water-cooled copper mold-door
M. Water hose to door
N. Door flexible sleeve
O. "O" ring gasket
P. Furnace trunnion support
Q. Supporting block
R. Hose counterbalance rope
S. Furnace trunnion
T. Sight port

Preparation of Pigs. A titanium casting must be homogeneous and uniform in composition to be of maximum utility. The arc melting process is not conducive to making a uniform alloy from directly charged components, because only a small portion of the charge is molten at one time. Thus, it is necessary to provide melting stock which contains the desired alloy additions in uniform distribution. This is accomplished by making preliminary pigs which have the desired alloy content evenly distributed throughout. Each pig weighs about 2½ lb and is made in the following manner:

Melt Titanium Sponge

Approximately 0.6 lb of four to eight mesh Dupont process "A" titanium sponge is placed on the bottom of the crucible. No graphite insert is used during the preparation of pigs. Additional sponge of coarser size is charged over this bed until the furnace contains about 2½ lb of material. When it is desired, alloying elements can be added. These additions are made to the charge before closing the furnace. For carbon alloys, the desired amount of carbon is introduced as 40 to 60 mesh high purity carbon powder or CS grade graphite powder.

The amount of this powder required for the desired alloy is sprinkled on top of the sponge and during subsequent melting is thoroughly mixed into the titanium by means of the electric arc. When other alloying agents are to be used, they are mixed with the coarse titanium sponge. A few pieces of coarse titanium sponge are placed on this mixture so that the arc may



be struck against the sponge surface without contacting the alloy addition.

The furnace is sealed and flushed with argon as previously described. The generators are set at 90 volts, open-circuit, and 200 amperes. The arc is struck and the electrode withdrawn to produce an arc of 23 to 26 volts. As soon as a molten pool has formed the current is raised to about 600 to 700 amperes. This current is used to consolidate the alloy and the sponge into a shallow pig of the desired gross composition. After consolidation, the generators are shut off and the pig is allowed to cool for a half-hour before it is removed from the furnace. No special effort is made to insure complete solution of the alloy in the pig as this is accomplished during subsequent melting in the skull. Briquetting under pressure would accomplish essentially the same purpose as pigging if a suitable press and dies were available.

Melt Solid Block

Preparation of the Skull. Titanium alloy castings of the desired composition and free from contamination are made in this furnace by using a block of solid titanium alloy as the charge. The electric arc is directed against the top of the block to bring this portion to the desired temperature. Characteristically, in this process only the top layer of the charge is molten. Because this molten metal is actually part of the solid container it is necessary that the skull as well as the bath should have the same alloy composition as that desired in the final casting.

The block comprising the charge is made from five of the pigs described in the preceding section. These pigs are stacked on a ¼ in. thick slab of graphite on the bottom of the copper crucible as shown in Fig. 5A. The furnace is sealed, evacuated, and flushed with argon as previously described. The pigs are then consolidated by remelting with the arc into a skull weighing about 12½ lb. This requires about ten minutes at 600 to 700 amperes and 26 volts during which time the electrode is moved over the stacked pigs to effectively melt them into a single block.

No effort is made to completely re-melt all of the pigs so that numerous voids are left around the sides of the skull. These convoluted voids do not impair the skull in any way and actually may improve its efficiency

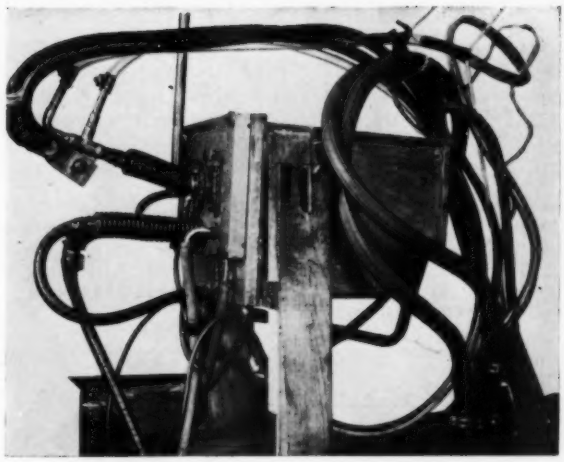


Fig. 3—Illustrating arc-casting furnace assembled and tipped forward as in pouring the casting.

as thermal insulation. The graphite slab remains as an integral part of the skull in subsequent operations (Fig. 5 and 6). Since the metal at the bottom of the skull does not become molten and the diffusion of carbon in titanium is extremely slow at the temperature of the graphite-skull interface, no carbon pick-up is realized. A separate skull of the correct composition must be provided for each alloy composition desired in the cast product.

Mold Baked Out

Preparation of the Mold. A high density, CS grade graphite is used for making the mold. The mold is made in sections machined to shape and incorporating adequate draft so that they may be readily stripped from the casting after use without damage. Prior to being used the first time for making a casting, the sections are packed in graphite powder for protection from the atmosphere and baked out for about an hour at 1200F. The baking operation removes residual hydrocarbons and occluded gases to preclude their evolution at the time of pouring which would contaminate the hot metal.

Whenever a new mold is used without the baking pretreatment a dark blue stain is formed on the sur-

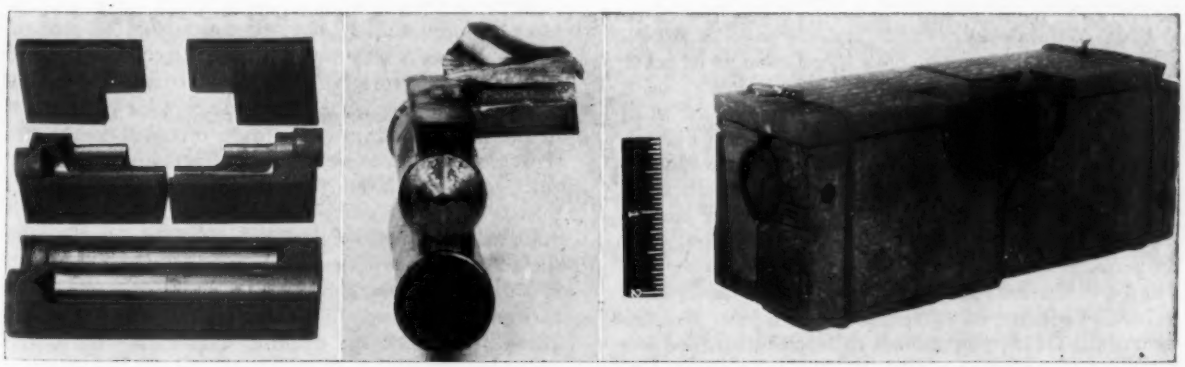
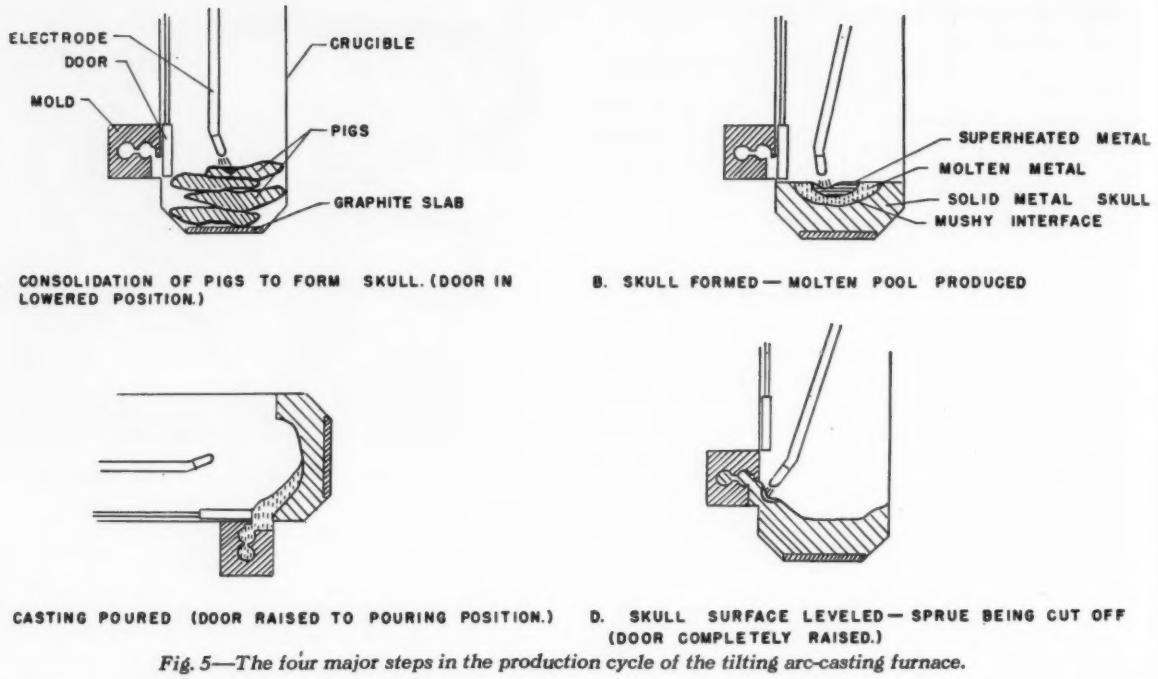


Fig. 4a (left)—Sections of the machined graphite mold ready for assembly. Fig. 4b (center)—A titanium casting after removal from the mold. Fig. 4c (right)—Graphite mold assembled and held together with soft iron wire ready for use. The wear from 15 previous heats is clearly evident in the spalled corners of the mold sections.



A. CONSOLIDATION OF PIGS TO FORM SKULL. (DOOR IN LOWERED POSITION.)

B. SKULL FORMED — MOLTEN POOL PRODUCED

C. CASTING POURED (DOOR RAISED TO POURING POSITION.)

D. SKULL SURFACE LEVELED — SPRUE BEING CUT OFF (DOOR COMPLETELY RAISED.)

Fig. 5—The four major steps in the production cycle of the tilting arc-casting furnace.

face of the casting and the skull during the casting operation. Obviously, this stain is a layer of metal contaminated by the gases given off by the mold during casting, because the metal is always bright and uncontaminated when the mold has been properly prebaked or previously used for casting.

To make a casting the sections of the mold are assembled and held together with soft iron wire (Fig. 4A and 4C). The assembled mold is placed in the mold cavity and secured in position with graphite wedges preparatory to being filled. The water-cooled copper mold door is adjusted so that it covers the mold to protect it from spatter and radiant heat until it is ready to be filled (Fig. 5A and 5B).

Door Protects Mold

Making a Casting. To make a casting in this furnace a skull of the desired composition is prepared from suitable pigs as just described. The mold door is closed to protect the mold from radiant heat and metallic spatter during melting.

After consolidation of the pigs, the heating is continued to form a molten pool large enough to make a casting. The electrode tip is moved slowly about in a 4-in. diameter circle over the center of the skull for about five to eight minutes (Fig. 5B). This operation at 600 to 700 amperes and 26 volts thoroughly preheats the skull and the preheating time is adjusted according to the fluidity of the alloy being melted. The current is then raised to approximately 1200 amperes. The voltage which in arc melting is directly related to the arc gap is maintained at about 23 to 26 volts. The increased current causes a deeper penetration of the arc into the molten bath so that an adequate quantity of properly superheated metal may be provided for the subsequent casting operation.

A considerable excess of molten metal in the bath is required over that needed to fill the mold. This excess provides for the spine attaching the skull to the casting (Fig. 5C), for any overflow around the sprue of the casting, and for the chilling that occurs when the arc is shut off prior to pouring.

High Current Will Suffice

When sufficient superheated metal has been obtained, usually about five minutes at the high current will suffice, the mold door is raised just enough to clear the mold entrance as shown in Fig. 5C; the supporting block, Item Q in Fig. 2, is removed; the current is turned off; and the metal is poured by tilting the entire furnace assembly on its trunnions (Fig. 3 and 5C). The final sequence of operations is done as rapidly as possible to minimize cooling of the metal and to eliminate any splatter entering the mold once the door is raised. Less than two seconds elapse between the raising of the door and the completion of the pour.

Cut Spine With Arc

This process produces a casting which is still secured to the parent skull by a metal spine (Fig. 5C and 6). The spine must be severed before the casting and the mold may be removed from the furnace. To accomplish this, the furnace is allowed to cool for six minutes in the pour position and is then returned to the upright position for cut-off. The arc is restruck using 90 volts open-circuit and 200 amperes. The current is raised to 350 amperes and the electrode spacing is adjusted to about 26 volts. The arc is first played on the surface of the skull to smooth out the edges, and then it is directed around the sides to melt down the metal accumulated there. This operation is usually completed in about six minutes depending upon the amount of excess metal and its position in the crucible. When most of the excess metal has been remelted into the skull, the arc current is reduced to about 250 amperes and the arc is used to sever the spine connecting

the skull with the casting (Fig. 5D). Figure 7 shows that the depression in the skull has been essentially filled in during the consolidation operation accompanying cut-off.

The cutting operation is facilitated by constructing the electrode with an inclined tip, (Fig. 1), so that the low-current arc may be directed against the actual metal to be removed. This final cutting operation requires less than two minutes for completion. A minimum of three hours is allowed for the skull and casting to cool before the furnace is opened to avoid atmospheric contamination of the skull.

Another casting can then be made using the same skull as the source of metal. In this case, however, since the starting material is a consolidated and cold skull, preheating time must be allowed. A casting can be poured about 15 to 20 minutes after striking the arc. During the last five minutes of this period the current is raised to about 1200 amperes in order to effect sufficient penetration of the heat into the bath to provide the required amount of superheated metal. The cavity in the skull of Fig. 6 is about 1⅜-in. deep which was the penetration obtained in making the attached 1½ lb casting.

Production Takes Time

After two castings are made from each skull, a 2½-lb pig of identical composition is added to replace the metal that has been cast. The present operating procedure requires two days to produce six pigs for a new skull of each alloy, and after that from one to two castings can be made each day.

Figure 4 shows a typical titanium casting and a graphite mold. The shrinkage during solidification is confined to the upper section of the modified keel-block casting so that test bars are machined only from the bottom bar. Between ten and 15 castings can be made in a mold before it must be replaced due to excessive spalling of the graphite. The shape of this test bar casting was selected because it is relatively easy to machine the graphite into this configuration.

Figure 8 is a cast titanium link which weighs about ½ lb. The slot and the holes were formed about solid graphite cores secured in the graphite mold. On the small end of the link is an embossed letter M formed in an impression in the mold. This casting was made to illustrate the good definition possible in casting titanium. This "commercially pure" titanium casting has a Brinell hardness of 156.

Design Considerations

The most important phase of the casting problem is the development of a suitable method for melting titanium. This method must provide molten metal in sufficient quantity to fill the molds in one continuous pouring of short duration and the metal must be uniform in composition and free from any contamination. These requirements pose many special problems, because molten titanium will react with oxygen and nitrogen of the air and will attack all known structural materials and refractories which are at the temperature of the bath[2]. Furthermore, the products of these reactions in the molten metal and even when present only in traces will seriously embrittle it for subsequent use.

Graphite is the only known refractory which has consistently shown any resistance to attack when containing molten titanium, but even then the amount of carbon dissolved exceeds 0.30 per cent and is so inconsistent it cannot be predicted in successive heats[3]. As little as 0.15 per cent carbon limits the amount of most other alloying elements which may be introduced without lowering the ductility and seriously impairing the weldability of titanium. No heat treatment has been reported which will notably alleviate these deficiencies.



Fig. 6—A titanium alloy casting attached to its parent skull by a metal spine formed from excess metal during pouring. Spine is normally severed with the arc before removal from the furnace. The cavity is 1⅜ in. deep and the skull is 6 in. by 7 in. by 2⅜ in. thick.

Induction melting presents a compact method of melting under a protective inert atmosphere and the rapid stirring caused by the electrical field insures uniform composition. However, no suitable crucible material other than graphite is available for use with titanium, so that this method is inadvisable where the inherent carbon pick-up is likely to be detrimental to the application of the product.

Avoid Metal Contamination

Arc melting provides a method for introducing the heat directly into the bath without contamination from the container or surroundings[4,5]. The arc is maintained between an electrode and the molten metal contained in an inert crucible which is water-cooled to prevent reaction with the molten titanium. Contamination by the air is prevented by melting in a vacuum or an inert atmosphere of argon or helium.

Either an alternating current or a direct current arc may be used for melting, but each type of power has its own characteristics which adapt it for special applications. For example, when the direct current arc is used the positive anode receives almost twice the amount of heat that is received by the negative cathode. Obviously, when alternating current is used each electrode then is alternately anode and cathode. Thus, it is advisable to use the direct current arc for melting with an inert electrode, whereas either direct current or alternating

current may be used for the consumable electrode.

The consumable electrode is very efficient with the high melting point metals such as molybdenum[5], but an inert electrode is normally used for melting titanium except in special applications. The thermal emission efficiency of the arc increases rapidly as the electrode temperature is increased[7]. Titanium melts at a temperature below this optimum emissivity so that when made of titanium, the electrode melts off very rapidly without introducing sufficient heat into the bath to meet it completely.

Prefer Inert Electrodes

Inert electrodes are usually provided with water cooling and are tipped with graphite or tungsten to enable them to withstand the extremely high temperature of the electric arc. Furthermore, they are used with direct current and the electrode is connected as the cathode and the bath as the anode to benefit from the large difference in heat distribution between these two polarities.

The problem of producing titanium castings is further complicated by the need for a mold material which not only will resist attack by the molten titanium flowing into it, but will also be practical for the production of castings. For instance, conventional sand molds suitable for use with steel have been utterly unsatisfactory for titanium because of the rapid reaction with the silica. Bonded refractories, shell molds, and investment molds are made by techniques which might be developed satisfactorily for use with titanium, but permanent molds of graphite were deemed the most practical for immediate use.

The final requirement in satisfactorily making castings is only slightly less important than those preceding. This is the means by which the molten metal is transferred into the mold from the bath. Bottom pouring and centrifuging methods are possible, but lip-pouring by tilting the complete apparatus was considered to be the simplest for casting titanium.

Conclusions

As now developed, the tilting arc furnace is a useful tool for the laboratory production of small castings of titanium alloys for test purposes. This furnace is particularly suited to the production of small castings up to 1½ lb in weight without contamination from the atmosphere, crucible material, or mold material.

The principle of skull melting should be applicable to the production of larger castings, but probably would require a relatively cumbersome apparatus. Several advantages should accrue through the use of a larger furnace. For example:

1. A larger furnace should be much more efficient because a proportionately smaller skull may be used than is required for the 1½ lb casting. The surface to volume ratio of the skull in the large furnace is less, so that heat losses through the bottom and by radiation from the surface would be less.

2. The larger furnace should facilitate the use of more flexible molding techniques such as the shell type of plastic bonded refractory sands if they prove to be practicable for titanium. Thus much more complicated shapes may be cast than are economically possible with the machined graphite molds.

3. A larger furnace could be used to cast several small units at one time from a single runner.



Fig. 7—The cavity in the skull of Fig. 6 has been filled in during the consolidation operation accompanying cut-off of the spine. Cut-off is final step in furnace.

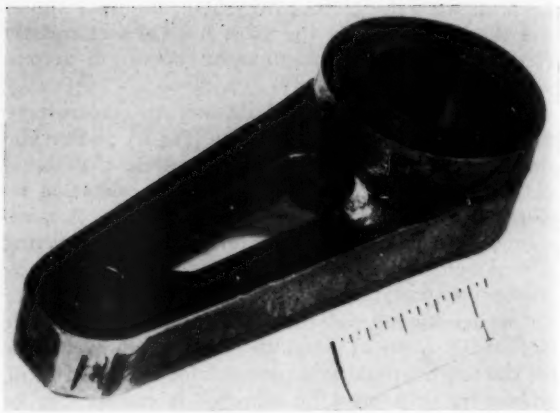


Fig. 8—A titanium link cast in the tilting arc-casting furnace. The mold and cores were made from solid graphite. This casting weighs about ½ lb, has a hardness of 156 Bhn.

Acknowledgements

The authors wish to express their appreciation to Harold Markus, director of metallurgical laboratory, and to Lt. Col. Daniel J. Murphy, former chief, Pitman-Dunn Laboratories, for providing the opportunity and assistance required in carrying out this development.

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ADVANTAGES OF INSULATING AN ACID ELECTRIC FURNACE ROOF

By

Charles C. Spencer*

ABSTRACT

By using standard materials which are readily available, the author has affected substantial savings in the initial cost of furnace roofs, in uninterrupted service derived from it, and rarely has to shut down the furnace for roof repairs.

For many years the roof on the Electric Steel Casting Co. furnace was constructed of special shaped 13-in. silica bricks. The performance of these bricks was fairly satisfactory but they would occasionally spall, especially when operation was intermittent. The spalled pieces of brick would have to be raked off the slag before tapping, otherwise they would plug up the tapping hole.

Various ways to correct this condition were tried, such as cooling the roof off after the last heat was tapped, raising the doors as soon as possible, and not charging up immediately after the bottom and side walls were repaired but charging the scrap into a cold furnace the next morning. This did not correct the spalling nor did it prolong the life of the roof.

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The next method was to repair the bottom and side walls as soon as possible, charge up the furnace, and seal the doors and spout with sand. This seemed more satisfactory and it did decrease spalling, but roof life was still somewhat erratic. Minimum was approximately 120 heats, the maximum approximately 225 heats, averaging throughout the years approximately 170 heats. This difference, it was believed was not caused by any great change in the production schedule, but rather due to spalling.

Tried Insulation

In 1940, experiments were started with a roof constructed of 9-in. standard silica brick. After the bricks were laid up, they were covered with a proprietary insulating material that was being used successfully and extensively in open hearth furnace shops. This is made with expanded vermiculite in a special mesh assortment ranging from ¼-in. to fines. It is a light fluffy aggregate, which readily mixes with water to form a smooth troweling, insulating plaster of great spreading power.

It was troweled smooth to a thickness of 1-in., which is equal to approximately 7-in. of fire brick. The roof



Furnace roof made up of 9-in. standard silica brick.



Insulation 1 in. thick more than doubles roof life.

ran 490 heats, and the experimental roof construction was adopted as standard practice. The method is:

Build the circles that make the electrode openings first, wrapping a wire around the bottom and the top of the bricks to hold them in place. The wires are not removed from the brick but are allowed to become a part of the roof. The rest of the roof is laid up, starting from the water-cooled roof ring. The first row is 9-in. standard super duty clay brick. The remaining rows are 9-in. standard silica brick, consisting of straights, wedges, and No. 1, 2, and 3 arch.

To cut bricks to form a tight and perfect circle is both tedious and costly, so where a whole brick will not fit, the opening is rammed with a mixture of ganister, fire clay and sulphite liquor water. When the opening is too small to ram, ground silica fire clay mixed with water is poured in the small openings.

After the roof is laid with brick and all the holes filled in with ganister or ground silica fire clay, the insulating material is put on top of the brick and troweled smooth.

Roof life has been doubled in the last 12 years, using the above construction. One contributing factor to this longevity, in the author's opinion, is that the roof is insulated. From a maximum of 225 heats on the old type of roof using 13-in. brick, which in initial cost is more expensive than the 9-in. brick, the furnace went to a maximum of 525 heats on the new insulated roof. The experiment of using insulating material on the 13-in. brick was not carried out because the 9-in. brick serves very well. Average over the 12 years has been approximately 400 heats per roof.

Insulation of the furnace roof performs a number of functions, which the writer believes justify the cost of the material and the labor to apply it.

1. The 1 in. of insulation is equivalent to 7 in. of fire brick. This gives a tremendous weight saving since 1¼ lb of the vermiculite mix will cover 1 sq ft of roof surface, 1 in. thick. A 9-ft diameter roof requires only about 80 lb.

2. It gives more uniform power consumption.

3. It keeps iron oxide away from the bricks, and doesn't allow it to work down in the cracks where it would tend to erode the bricks faster.

4. It serves as an electrical insulator between cooling rings, for it is a non-conductor of current.

5. It gives a smooth seal and provides a good surface from which iron oxide and other dust can easily be removed by sweeping or blowing with an air hose.

6. It helps to hold cooling rings in place if flanges are embedded ¼ in. in the material.

7. It reduces spalling by protecting the refractory from the effect of sudden high temperatures in the initial service. This serves to allow expansion to occur at a slower rate, avoiding the stresses set up between hot and cold faces which otherwise results in spalling.

DISCUSSION

Chairman: C. H. WYMAN, Burnside Steel Foundry Co., Chicago.

Co-Chairman: R. A. WITSCHEY, A. P. Green Fire Brick Co., Chicago.

Recorder: R. A. WITSCHEY.

CHAIRMAN WYMAN: What insulating material do you use?

MR. SPENCER: It is a vermiculite-base insulating castable refractory. One inch of this material is equivalent to about 7 in. of brick. After we operate for about 200 heats and the roof has worn down to about 5 in., we add ½ in. of insulation. After 300 heats we add another ½ in. of insulation.

CHAIRMAN WYMAN: How is it applied?

MR. SPENCER: We use a wooden concrete float. We dry slowly with wood fire for about 15 hr.

D. S. BOSMA:[1] We are using this procedure too. Would you clarify the thickness of the arch?

MR. SPENCER: It is 9 in. of refractory and 1 in. of insulation to start with.

B. A. LAWSON:[2] Are you using oxygen? What is the immersion time? Are you using special shapes?

MR. SPENCER: We are using oxygen. Our immersion time varies but it probably averages 1½ min. We are using standard 9-in. series.

MEMBER: What size heat do you melt, how many heats total and how do uninsulated roofs compare?

MR. SPENCER: Ours is a 3-ton Heroult furnace but we melt 5 to 6 tons. We have never tried an uninsulated 9-in. roof.

W. A. ZEUNIK:[3] How do you use standard shapes in the center? Do you lay up the standard arch brick around the electrodes and wire them?

MR. SPENCER: We start the skew with clay brick and lay up to the center circle. The space in between is rammed with a mixture of silica-fire clay-water mix.

MEMBER: Does not laying up these roofs require a brick mason?

MR. SPENCER: No, we do it with ordinary labor.

E. E. JOHNSON:[4] Do you lay brick dry?

MR. SPENCER: Yes, we lay dry and then coat over with a silica fire clay slurry.

MEMBER: Do you coat the inside of the roof too?

MR. SPENCER: Yes, but the coating disappears after the first heat.

A. J. REID:[5] Do you ram fire clay gannister mix between the crevices to get rid of cutting and fitting?

MR. SPENCER: Yes, we do.

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[1] Bucyrus-Erie Co., So. Milwaukee, Wis.
[2] Harrison Steel Castings Co., Attica, Ind.
[3] National Malleable & Steel Castings Co., Indianapolis, Ind.
[4] Harbison-Walker Refractories Co., Milwaukee, Wis.
[5] General Refractories Co., Indianapolis, Ind.

PATTERNS FOR SHELL MOLDING

By

Ray Olson*

■ What's so mysterious about shell molding patterns? From the remarks occasionally heard, there must be a feeling of mystery concerning them. It is likely that this feeling is fostered by little or no knowledge of the action that takes place, both chemically and physically, in the making of a shell mold. When there is a rough understanding of this action, many of the pattern requirements are apparent, clearing away most of the mystery.

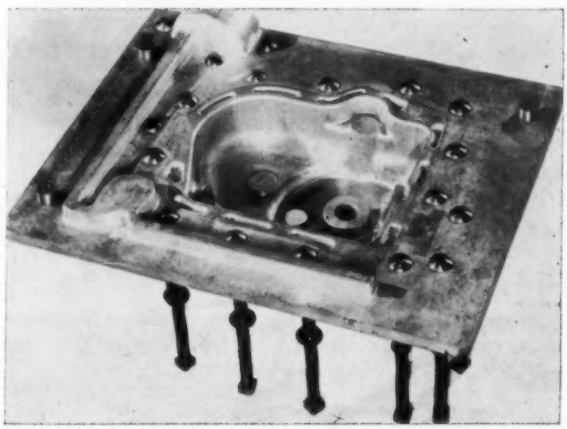
A possible definition of a shell molding pattern is, one or more patterns located on one or two pattern plates. This equipment is constructed of a material which will stand repeated heating to a maximum of about 600 F. Pattern or patterns to be arranged on plates so that both halves, or cope and drag, match with locators established on the plate surface. The locators are usually of a male and female type, set into and projecting above the plate surface, so as to create a corresponding condition on the joint face of the two halves of a shell mold. Provision is usually made in the plate area around the pattern for the installation of ejector pins, which push the shell from the pattern after curing.

We could elaborate on this definition. Reference could be made to special requirements of shell core boxes. However, the general specifications above are the basic needs for producing most shell molds.

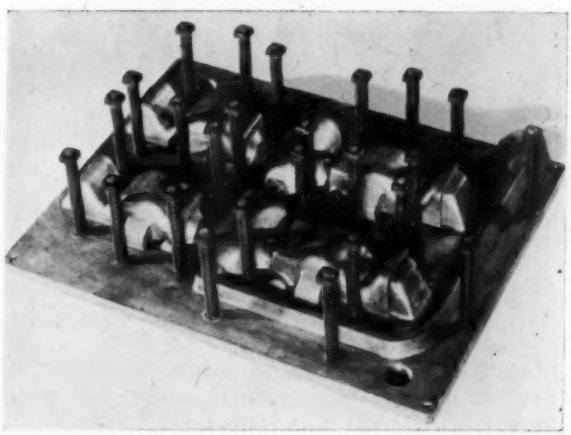
Patterns used in this process usually consist of two fundamental types; those made of aluminum and those made of cast iron. Aluminum stands more or less alone as a material which can be processed at reasonable cost to produce a shell molding pattern which is suitable for experimental and medium production usage. With some care and attention, aluminum patterns and plates will produce 5,000 or more shells before they need replacing. Very good results have been obtained with the use of aluminum plates ½ in. thick, producing 15 in. by 20 in. shells. Aluminum plaster cast plates are particularly adapted to irregular partings at the joint of the two shell mold halves.

It is also profitable in some cases to replace aluminum shell patterns as many as three times, and still not exceed the cost of initial cast iron equipment. This is particularly true if master patterns and pressure cast aluminum plates are used.

In many cases, master patterns are made of aluminum completely machined and carefully finished for

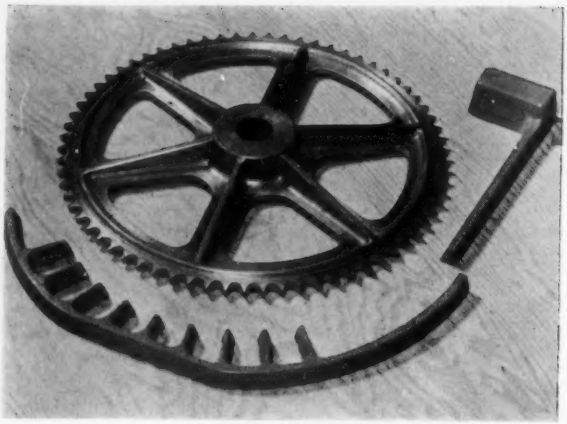


Aluminum pattern produces good, low cost shell molds.

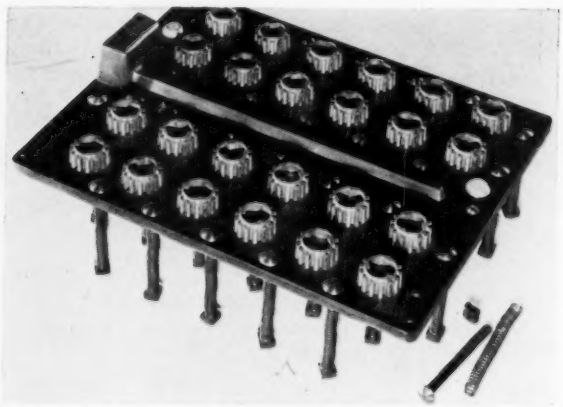


Plaster casting permits close matching of partings.

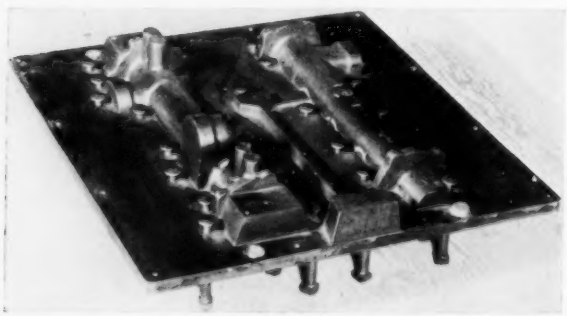
* Production Pattern & Foundry Co., Chicopee, Mass.



Aluminum master pattern has accurately machined teeth to correct shrinkage. Wood gating supplied with master, to be cast integrally with aluminum plate.



Intricate patterns can be duplicated by using an aluminum master pattern and shell casting brass or iron patterns.



Cast iron plate produces both halves of shell mold due to centerline parting.

use in pressure plate making. This permits working to closer tolerances and decreases the possibility of master damage in case of re-use.

Aluminum patterns can be machined and finished individually, and then secured to the pattern plate. Pattern dimensions can be accurately determined with this technique, and clean, sharp parting lines established. However, care must be exercised in fastening screws or bolts tightly, as repeated heating and cooling may tend to loosen them. This heat problem also promotes corrosion in the threads, causing the screws to break when they are removed. Coating the screws with high temperature lubricants retards this tendency. Patterns, plates and gating cast integrally eliminate these fastening problems.

In the cast iron class of shell patterns, two other materials are included: brass or bronze and steel patterns. These are included in the cast iron grouping, because first, the cost of producing patterns of either of these two metals is comparable to cast iron costs, and second, their usage is minor compared with cast iron.

Treatment for Cast Iron

Long wearing iron patterns that will take a high surface finish can be made on an analysis similar to: carbon, 3.10 per cent; silicon, 1.90; manganese, 0.80; sulphur, 0.10; phosphorous, 0.10. This iron can be annealed at 1,500 degrees for one hour, if improved machinability is required for thin sections. Stress-relieved flat iron plates, ground to ½ in. thickness, are used with excellent results.

Important savings can be made in the duplication of multiple impression iron, brass, or steel patterns. This is especially true if the shape of the patterns is somewhat complex, a type requiring a number of machining operations. First, if a wood master pattern is used, it is made with an additional metal shrinkage or extra machining allowance. From this is made an aluminum master pattern which is accurately machined and finished. This aluminum master is mounted on a small plate. This plate can be more or less standard and re-used indefinitely. From this plate-mounted master, small shells are made. These shells are then poured with the required pattern material. Usually, a small amount of finish is left on joint faces and extremely critical areas. The complicated surfaces of these shell cast patterns can be finished on the bench, eliminating much costly machining.

Fine finishing of vertical surfaces of any type metal shell pattern requires close attention. This should consist of strokes perpendicular to the parting line, being careful not to create backdraft.

Cause Pattern Damage

One of the hazards of the use of aluminum patterns is their susceptibility to nicks and bumps. One of the most common causes of nicking is carelessness in removing a sticking shell. Very often, the closest tool at hand is used by the operator to remove a sticker. This might be a screwdriver, which is a dangerous tool so far as aluminum patterns are concerned. Pointed hardwood sticks will do just as well; dowel stock is excellent.

Another common cause of pattern damage is improper pattern storage. Shell molding patterns should be stored on edge in a storage rack. This rack should include pegs or other suitable supports to hold the plates upright. By allowing a small space between patterns, the ejector pins of one plate will not gouge the adjacent pattern.

Shell molding is similar to other casting techniques from the draft allowance standpoint. This statement is made to counteract a general feeling that shell patterns require very little draft.

Patterns should be made with sufficient draft allow-

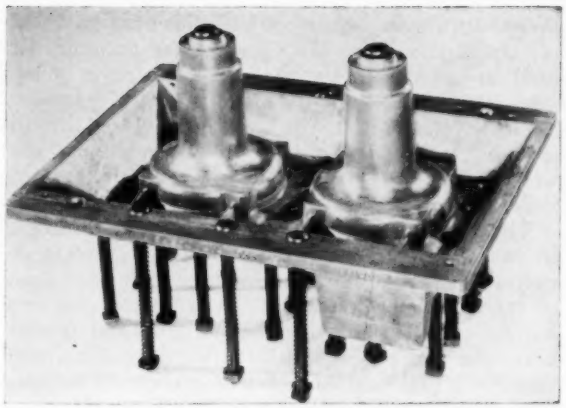
ance to permit uninterrupted shell mold production with a broad range of sand-resin mixes, a thin shell, or a variation in condition of cure.

The tighter the draft allowance, the more closely all of the above variables must be controlled. In some cases it has been impossible to place a sufficient number of ejector pins in a plate to insure obtaining a shell in one piece, because of draft limitations.

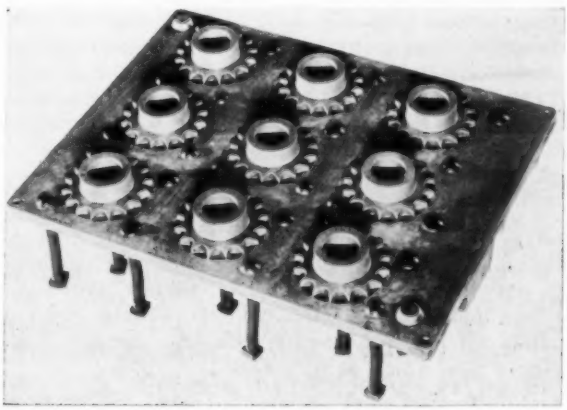
There is no standard angle that can be universally recommended for draft purposes. In contrast to green sand molding, holes or chambers in a pattern sometimes eject more readily than an exterior projection, such as the holes in a wheel, as compared with the hub outside diameter. There seems to be a slight shrinking action of the cured shell against these outside diameters or projections. Shells can be made in steady production, with little or no draft on a few points, if other vertical surfaces have some excess draft allowance. In general, an attempt should be made to obtain the same draft allowance as would be used in green sand molding. Draft angles can be reduced by dressing down the patterns. Adding draft presents more of a problem.

Ejector Pin Design Important Factor

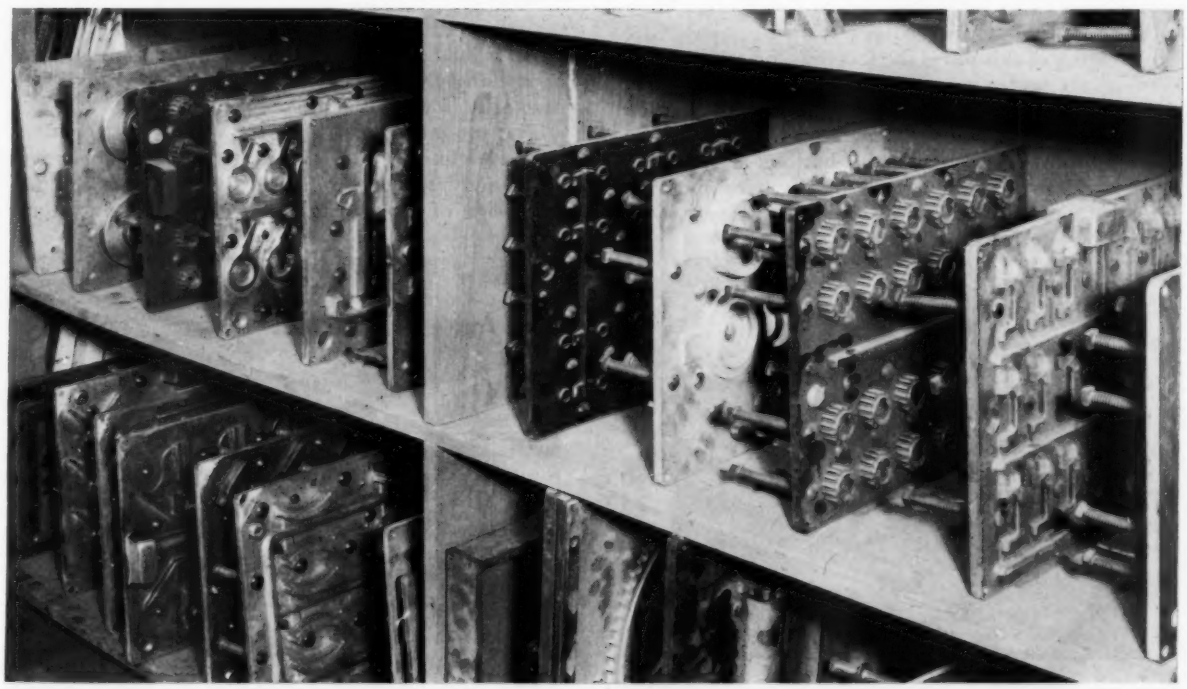
Ejector pin design and placement constitute one of the most important factors in the production of shell molds. Many of us have started in this process on the premise that the ejection assembly required a subplate, or ejector plate, fitted with guide posts lined up with holes through the underside of the pattern plate. The ejection pin holes, in turn, had to be line bored, or otherwise matched in both pattern plate and ejector plate. These holes in our first patterns were all fitted with very few thousandths of an inch clearance. First, this arrangement was very costly. In some instances,



Long vertical draws require careful finishing with strokes perpendicular to parting line.



Shells can be made with less draft in the bores.



Care should be taken to prevent pattern damage by using safe storage racks.

the cost of rigging a pattern with this ejection assembly equalled the cost of producing the patterns. Second, on many jobs it is necessary to relocate or add pins after tryout. This is, obviously, also expensive. Third, we found that the close fits required by this assembly were not suited to steady production; pins were sticking, due to the abrasive (galling) action of the fine grains of sand.

The simplest, cheapest design of ejector pins seems to be working most satisfactorily. This design requires no sub-plate. If trouble should occur, and a pin or spring becomes damaged, it can be replaced in a minute without removing the pattern from production. One type of pin which is coming into increased usage is a slight modification of a common carriage bolt. The section immediately under the head is dressed or turned down to the bolt diameter, to permit the head to set flush on the face of the pattern plate. Best practice is to drill, not ream, a hole through the plate, at least 1/64 in. to 1/32 in. larger than the bolt diameter. Comparatively light stainless steel springs work very well, retained by the carriage bolt nut. The nut does not tend to loosen or turn, because of slight spring pressure.

Carriage Bolts Good Ejectors

Cotter pins and washers are also commonly used to retain the springs. The crowned head of the carriage bolt does not harm the joint face of the shell as long as it is not on a casting surface. In fact, tests have shown that this oval section has definitely less shearing action on hot, just-cured, shells than the flat, flush ejector pins which were first used.

Another type of pin, similar in characteristics, is inexpensively produced of drill rod on a screw machine. The head consists of a separate steel disc, retained by a snap ring. The spring is retained by a split washer, commonly used as a bearing retainer. One good feature of this pin is the fact that the relationship between the underside of the head and the bottom of the pin can be held to a few thousandths of an inch in the screw machine. This causes uniform ejection when the ejector platen contacts the bottom of the pins. With carriage bolts, the ends of the pins must be sized, because of coarse manufacturer's tolerance.

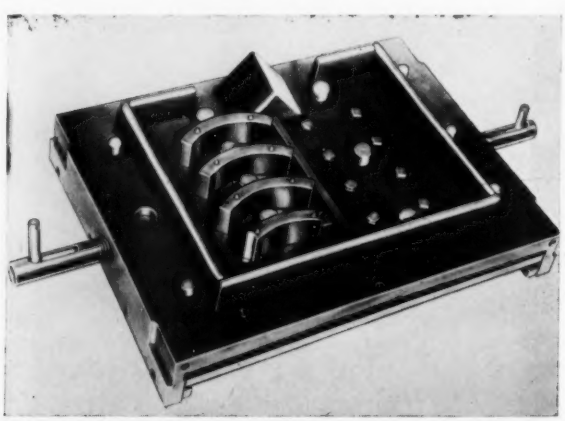
There are a number of techniques used to assemble and hold together the two halves of a shell mold for pouring. Some are poured with backup material, and others without. It is not the purpose of this article to discuss this phase of shell molding; however, regardless of the technique used, it is desirable to start with a reasonably flat joint surface. Some deviation from a flat, true surface is caused by variation of the rate of cooling of different portions of the newly ejected shell. Shell investing and curing conditions and sand-resin mixes greatly affect the degree of plasticity of a shell at the time of ejection. If a very flexible or plastic shell is immediately placed face down on a flat surface for ten to 15 seconds, it will tend to level out.

How to Keep Shell Flat

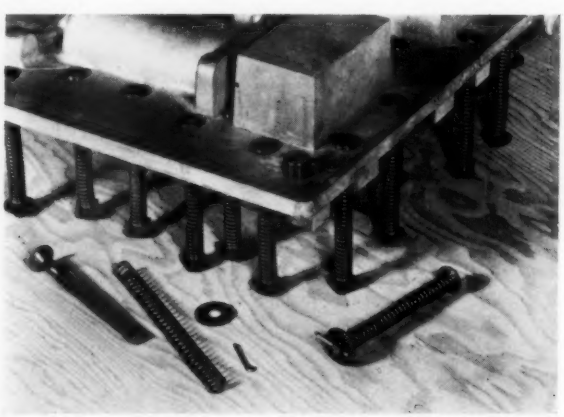
One of the best aids toward keeping a shell flat, particularly around the edges is the following: raise the outer perimeter of the pattern plate, or of the frame around it, above the plate surface. A 1/4 in. high ledge is usually sufficient. The inside edge of the ledge determines the outline of the shell, and on it, generous draft should be allowed, such as five, ten, or 15 degrees. The top of this inside edge should match up with the inside of the dump box, or investment chamber adaptor, if pattern is run on a shell molding machine. This ledge prevents peel-back, which sometimes occurs when the pattern and invested shell are removed from the investment chamber. There is also no tendency to curl in curing with this raised ledge.

Larger patterns which have some depth above the parting line usually create straighter shells. The vertical shell section around the edge of the pattern acts as a rib, holding it flat. Runner and sprues have the same effect to a lesser degree. On extremely flat patterns, it is sometimes advantageous to create a rib by mounting or casting on the plate surface, away from the casting cavity, a rectangular bar. In some cases, the use of these bars around one half of the shell and correspondingly matched grooves in the other half, satisfactorily lines up the mold.

The mounting or casting of gating on a pattern plate is definitely part of a patternmaker's job. However, there are many who feel that the responsibility for design of this gating and the arrangement of the



Cast iron plate with stripper sub-plate. Raised bars around edge of plate help maintain a flat shell surface.



Simple design of ejector pins: they are easily installed and readily replaced.

patterns on a plate should also be assumed by the patternshop. The foundry engineer or someone directly responsible for the production of the castings should assume this responsibility. There is nothing mysterious about shell casting gating. It may seem so on the first few attempts to run a specific casting, but the answer will finally appear. Best previous practice and judgment indicate that bottom gating should be used, and in the end, top gating will prove to be the answer.

Vertical Pouring Brings in New Variables

Vertical pouring brings in a whole new set of variables, which can be overcome with trial and patience. One fallacy seems to have gained a foothold: that "shell castings do not have to be fed." Tipping a casting at 90 degrees may change the whole picture, as far as directional solidification and hot spots are concerned. In most cases, yield is improved with shell casting, due to less gating. However, if there is a heavy section in the casting, which will remain molten after the gates are solid, it must be risered, fed, or chilled. One of the principal causes of shell casting defects is the extremely smooth surface of the shell, as compared with a conventional sand mold. This permits the metal to more or less slide into the casting cavity without the retarding, cleansing, or even combing effect of the sand mold surface. The shell castings may show evidence of dross or slag as a result of this condition.

The one reason for emphasizing this point in a pattern discussion is to call the attention to the following: when gating a shell pattern, it may pay to add insurance in the form of slag traps, basins, dirt catchers, or strainers, to avoid scrap castings.

Shell pattern equipment generally costs more to produce than conventional sand foundry patterns. This increased cost has been aggravated by casting accuracy requirements which in many cases are academical. Some common sense is necessary in this tolerance question. Perhaps on small and medium sizes shell castings, all but critical surfaces could stand tolerances of plus or minus 0.010 in. It is usually possible to produce castings which will be true to pattern, within 0.002 in. of pattern size, with a minimum tolerance of about 0.003 in. This, of course, is true only on those surfaces which are contained in one-half of the shell. Relationship between both halves of the shell are dependent upon the accuracy of fit and position of the locators. Tolerances across the parting line are entirely dependent upon the tightness of the joint around the casting cavity, and the support which may be necessary while the molten metal is solidifying.



Raised ledge around of pattern plate helps to eliminate rejected shells. Raised steel strip between patterns permits breaking shell into two mold halves.

A casting is never more accurate than the pattern from which it is made. Higher productivity and closer tolerances in sand foundry practice have called for more skill and ingenuity on the part of the patternmaker. In shell molding, both productivity requirements and casting accuracies are reaching goals never before thought possible. As ever in the past, patternmakers will meet and are meeting the challenge and are providing tools for the job.

A SIMPLIFIED COST SYSTEM FOR SMALL JOBBING FOUNDRIES

By

Roy W. McDonnell*

ABSTRACT

Keeping in mind that you can't spend more money keeping track of foundry costs than you do producing castings, the author has outlined a simple cost system applicable to small shops. While not opposed to detailed, complicated systems, he feels there is need for an elementary method of determining foundry costs.

■ The overall cost of setting up and operating a detailed cost system is approximately the same for a foundry producing 500 tons a year as it is for a foundry producing 50,000 tons, but the cost per pound of such a system in the smaller plant, would be prohibitive. The smaller jobbing foundries need a simple method of accounting for foundry costs. They need a system that gives reasonably accurate costs without requiring a battery of cost accountants or industrial engineers. A review of the author's cost system may be helpful even to foundries which have systems tailored to their own individual needs.

First, consider the items that make up costs of operating a foundry. They can be classified under five main headings.

DIRECT PURCHASES

Scrap iron
Pig iron
Ferroalloys
Lime rock spalls
Coke
Refractories

CUPOLA MEN'S TIME

DIRECT LABOR

Molding time
Core time

FACTORY EXPENSES

Casting time
Chipping time
Supervision
Helpers
Stock cores
Errors & defects
Holiday pay

EXPENSES—(Cont.)

Machine repairs
Building repairs
Small tools
Supplies—such as
  Molding sand
  Chaplets
  Plumbago
  Parting materials
Power, light, heat, and water
Depreciation
Misc. expenses
Group insurance
Unemployment insurance
Workmen's compensation

ADMINISTRATIVE

Office salaries
Auto expenses
Travelling
Advertising

ADMINISTRATIVE—(Continued)

Legal	Truck expenses
Telephone & telegraph	Exchange & bank charges
Office expenses	Selling expenses
Subscriptions & dues	Interest paid
Donations	General expenses
Bad debts	Insurance
Shipping and receiving	City taxes & license

It can be seen that the total of direct purchases and cupola men's time gives the cost of iron at the spout of the cupola. This figure, divided by the total pounds produced in a given period, gives the cost of melted iron per pound at the spout of the cupola. This can also be calculated from past experience as shown in the following example.

Cost of a 700 pound cupola charge of gray iron

Pig Iron, 140 lb	@	$79.40/ton	$ 5.56
Returns, 100 lb	@	42.00/ton	2.10
Scrap Steel, 70 lb	@	28.00/ton	.98
Scrap Iron, 390 lb	@	42.00/ton	8.19
Manganese Briquettes, 7 lb	@	.149/lb	1.04
Ferrosilicon Briquettes, 5 lb	@	.1055/lb	.53
Silicon at Spout, 2 lb	@	.236/lb	.47
Cost of Metal Charged, 714 lb			$18.87
Lime Rock Spalls, 21 lb	@	$ 8.50/ton	.09
Patching Refractory, 25 lb	@	2.58/100 lb	.65
Coke, 167½ lb	@	34.05/ton	2.85
Cupola Men's Time	@	.0052/lb	3.71
Total Cost of Charge of 714 lb			$26.17
Less returns, 15 per cent 107 lb	@	$42.00/ton	2.25
607 lb			$23.92
Less 5 per cent loss 36 lb			
Salable Castings 571 lb			$.0419/lb

To keep this paper as simple as possible, 4¢/lb will be used for the melted cost of iron. This figure should remain fairly constant except for any major changes in the cost of material or labor.

Direct Labor, is variable. This time must be kept separately and charged to the individual job on a work order or some similar method. Factory Expenses plus Administrative Expenses constitute total overhead. This can be added to costs on a percentage basis.

As an example, take a past year's or a past month's operation from the profit and loss statement or general ledger. As this system is simple and easy to keep up,

* Treasurer, Letson & Burpee, Ltd., Vancouver, Canada.

it is suggested that monthly figures be used and kept up to date. Here is a typical month's operation showing the operating costs and expenses in total only for the sake of simplicity.

Total Sales			$16,000.00
Opening Inventory		$2,000.00	
Direct Purchases		2,300.00	
		$4,300.00	
Less Closing Inventory		1,000.00	
Total Direct Purchases		$3,300.00	
Cupola Men's Time		700.00	
Melted Cost of Iron at Spout of Cupola		$4,000.00	
Direct Labor (molding and coremaking)		2,000.00	
Direct Costs		$6,000.00	
Factory Expenses	$6,500.00		
Admin. Expenses	1,900.00		
Total Overhead		8,400.00	
Total Costs			$14,400.00
Net Profit Before Taxes			$1,600.00

Tonnage Produced: 100,000 pounds; Average Cost per lb: $.144; Melted Cost per lb: $.04.

The problem now is to distribute the total overhead of $8,400.00. There are three ways this can be done but the one used should be the fairest, most accurate, and most equitable. The three methods are:

1. On the direct labor of $2,000.00. Overhead: $8,400.00 or 420 per cent.

2. On the direct costs of $6,000.00. Overhead: $8,400.00 or 140 per cent.

3. On the tonnage or pounds produced—100,000 lb. Overhead: $8,400.00 or $.084 per pound.

The following examples assume a molder is paid $1.85 per hour.

	METHOD 1	METHOD 2	METHOD 3
EXAMPLE A			
A 100-lb casting with 1 hr molding			
Melted Cost—100 lb @ $.04	$4.00	$4.00	$4.00
Direct Labor—1 hr @ $1.85	1.85	1.85	1.85
Total Direct Costs	5.85	5.85	5.85
Overhead	7.77	8.19	8.40
Total Cost	$13.62	$14.04	$14.25
Cost per lb	.1362	.1404	.1425
EXAMPLE B			
A 200-lb casting with ½ hr molding			
Melted Cost—200 lb @ $.04	$8.00	$8.00	$8.00
Direct Labor—½ hr @ $1.85	.93	.93	.93
Total Direct Costs	8.93	8.93	8.93
Overhead	3.91	12.50	16.80
Total Cost	$12.84	$21.43	$25.73
Cost per lb	.0642	.1072	.1287
EXAMPLE C			
A 50-lb casting with 2 hr molding			
Melted Cost—50 lb @ $.04	$2.00	$2.00	$2.00
Direct Labor—2 hr @ $1.85	3.70	3.70	3.70
Total Direct Costs	5.70	5.70	5.70
Overhead	15.54	7.98	4.20
Total Cost	$21.24	$13.68	$9.90
Cost per lb	.4248	.2736	.199

In Example A, possibly an average casting, the method used makes little difference. The cost per pound is about the same. However, Examples B and C show sharp fluctuations in the cost per pound. While the last two examples are possibly extremes they are certainly not ridiculous.

The writer advocates the middle of the road method (Method 2) for recovering the total overhead. Charging out rates, which are derived from a cost system, must do two things. First, recover all costs and a fair margin of profit and second, give a selling price that is in line with competition. In the author's company, Method 1 and Method 3 fail to give a price in line with competition. The cost in Method 1, Example B, is too low, giving only $3.91 towards overhead or $.0195 per lb whereas many items in overhead vary directly with the weight of casting produced. For example, casting time, chipping time, helpers, molding sand, plumbago, truck expenses, shipping and receiving, etc.

In Method 1, Example C, cost is too high. An overhead figure of 420 per cent on molding labor of $1.85 means that molder's time has to be charged at $9.62 per hour. It is quite possible for a molder to have 1 1/4 hours on a casting one day, 1 hour on the same casting the next day, and 3/4 hour on the following day. Time has been reduced 1/2 hour but costs have not been reduced $4.81. If, in using Method 1, molding time can be cut in half by using match boards, calculated costs are cut drastically whereas the only cost actually cut is molding. Casting time, chipping time, helpers, etc., remain the same per pound with no extra overhead to compensate for it.

In Method 3, Examples B and C, costs are too high and too low respectively. Some factory expenses such as chipping time, supervision, rejects, etc., do have a bearing on the molding and core time and Method 3 applies no overhead to the molding and core time.

Method 2, applies some of the overhead on the molding and core time and some on the metal, which seems to be the fairest on this simple cost system basis.

Now it becomes fairly simple to figure the cost and selling price of a casting:

Weight of Casting	500 lb @ $.04	$20.00
Molding and Core Time	5 hr @ 1.85	9.25
Direct Costs		$29.25
Overhead	140 per cent	40.95
Total Cost		$70.20
Profit	say 10 per cent	7.02
Selling Price	$.1544 per lb	$77.22

Or, for simplification, the selling price is 2.64 times the direct costs and this can be added to the cost of the melted metal bringing it up to $.1056 (2.64 x $.04) and added to the molding time bringing it up to $4.884 per hour (2.64 x 1.85). Then selling price becomes

500 lb	@ $ .1056	$52.80
5 hr	@ $4.884	24.42
Selling Price		$77.22

Thus, rate per pound on metal and a rate per hour on molding and coremaking have been established.

When making special irons, the cost of the nickel, chromium, and other additions to the charge of iron should be added to your selling price after overhead and profit at cost plus a percentage for profit. For instance, if the cost of the additions to produce a 2 per cent nickel casting amounted to $0.15 per lb the charge might be $.02 per lb extra.

Many foundries are operated in conjunction with machine shops under one roof and some firms have difficulty in allocating the overhead common to both

departments. For instance, they might have only one shipper and receiver, salesmen selling for both foundry and machine shop, and an office administering both divisions. They also might have trouble with some of the depreciation, building maintenance and other items impossible to break down between foundry and machine shop. This can be overcome by putting these items in a general overhead group and pro rating the total between the foundry and machine shop.

Need Estimator

Regrettably, this system does not do away with an estimator. Whether a shop makes or loses money on a job depends a great deal on his skill. At any rate, it gives him a formula with which to work and many foundries do not know their costs on a job even when they know the time and the weight of the castings.

Then too, it must be remembered, that increasing capacity 50 per cent by simply adding more men to the payroll, does not increase total overhead 50 per cent. Many items do not increase at all and some very little. Other items of course will increase in direct proportion to the increase tonnage. Should the opportunity of a very large order come up which will increase normal production considerably, an operator might be wise to take these factors into consideration.

This article was written with small jobbing foundries in mind. Manufacturing foundries with multiple production runs present different problems and where chipping and helping time, etc., can be charged directly to the job some different method of applying the overhead such as on the direct labor might be more feasible. However, when these charges are put in overhead it means the greatest portion of foundry cost is being applied on the smallest portion on a percentage basis if applied to the direct labor. It is far better to apply this overhead figure on the greater figure of total direct costs which will help reduce sharp fluctuations on individual job costs.

Not Foolproof

This is not suggested as a foolproof system but the writer believes it will give small jobbing foundries a reasonably accurate method of determining their costs on individual jobs with a minimum amount of trouble and expense. Many systems and ideas have been brought forward during the past 15 to 20 years but one must wonder if the old time foundry operators were far wrong when they set price lists where the selling prices per pound were reduced as the weight of the casting increased and placed a premium on the list for castings of a more complex nature.

PRACTICAL HEAT TRANSFER

AN INTERPRETIVE REPORT

By

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Foreword

It is desirable along the line of a prolonged research or development investigation to look back and assess the situation which existed at the start, to review the goal intended, the direction taken, and the accomplishment. Only thus may new sights be taken and proper decisions made as to the future course. After eight years of heat transfer research** under the auspices of the American Foundrymen's Society, it is proper that such a review be presented and that future plans be discussed.

Heat transfer is the very basis of the foundry industry—in great part, the foundryman's every day work relates to transferring heat in and out of the metal in such a way as to end up with a salable casting and a profit. The efficiency with which he can accomplish this is dependent on his knowledge of established practices and on the correctness of his intuitive deductions in cases such that the established know-how falls short of his requirements. The foundryman can never know enough regarding his most important working tool—practical heat transfer. The work on the Heat Transfer Committee should be recognized as having as its principal aim the development of new and more exact knowledge regarding the process of heat transfer as related to the solidification of castings, so that the foundryman will be provided with better working or "thinking" tools. It should be recognized also that the Committee's interests are basic to the entire foundry industry and not specific to any specialized field of the industry. It is evident that with such aims the process is never fully completed . . . new goals will arise with time. However, practical accomplishments should accrue along the road requiring summarization and interpretation as is planned for this report.

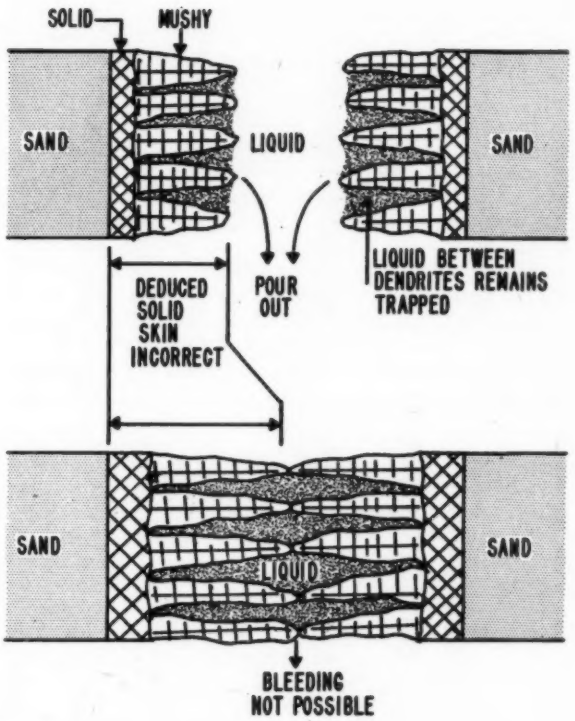


Fig. 1—Why skin thickness observed in "pour out" tests doesn't represent true solidification conditions.

All technologies are basically dependent on a specialized science related to their particular problems. Heat treating, for example, is greatly dependent on the science of transformations which occur in the solid material which is being cooled. Certainly, heat treating was carried out before the day of the development of the now classical S curve information. However, important improvements resulted from the application of this know-how by practical heat treaters. Technologies which operate with deficient scientific tools operate at a disadvantage; in this respect the foundry field has operated at a disadvantage inasmuch as a basic

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** The AFS Heat Transfer Committee research covered by this report was conducted under the chairmanship of H. A. Schwartz, National Malleable & Steel Castings Co., Cleveland. Original members of the Committee (1945) included H. A. Schwartz, E. C. Troy, Palmyra, N. J., J. B. Caine, Wyoming, Ohio, and H. F. Taylor, Massachusetts Institute of Technology. The subject report has been reviewed and concurred with by the present membership of the Committee: H. A. Schwartz, E. C. Troy, J. B. Caine, V. Paschkis, Columbia University, C. E. Sims, Battelle Memorial Institute, and E. T. McGuire, Deere & Co., Moline, Ill.

science related to the solidification of metals was essentially non-existent at the time of the starting of heat transfer work by the A.F.S.

It is no overstatement that during the past eight years a solidification science has been evolved, in part, and that the period and process of its development compares in all respects with the period in the thirties which saw the evolution of a science related to heat treating. The foundrymen should, as the heat treaters did, begin as soon as possible to use the new information available to them.

The approach taken by the Heat Transfer Committee in the initial planning was considered rather radical, and indeed it was. Heat transfer and solidification problems were to be studied, not on the foundry floor but on an electrical panel board having the appearance of a telephone exchange, i.e., the Electrical Analogue developed by Dr. V. Paschkis at Columbia University.

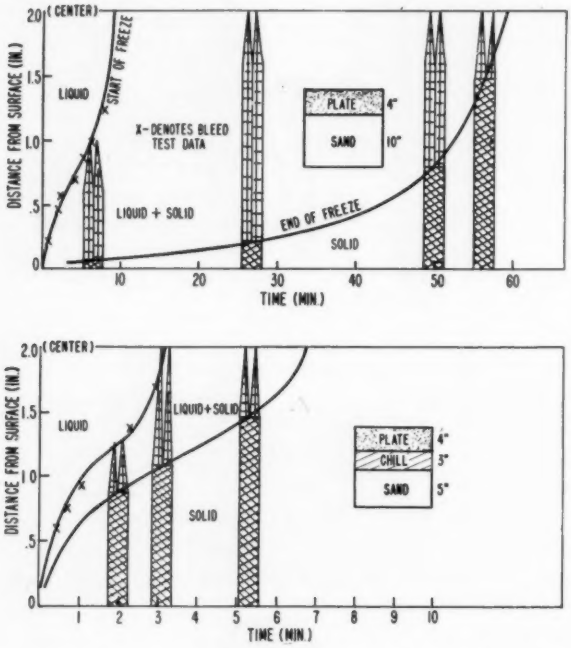


Fig. 2—Relation of Analogue freezing curves and "bleed" test data for sand and chill wall solidification of steel plates. Schematic dendrite structures indicate zone of mixed liquid and solid at various times.

There was a reason for this. Dr. Paschkis had demonstrated repeatedly to industry that heat transfer problems could be resolved by means of the Analogue. On the other hand, the only then available and used foundry technique for solidification studies, the "pour out" or "bleed tests" had repeatedly failed to provide the needed information. Clearly, it was a time to try something new. At that time direct solidification studies by means of thermal analysis methods which have been evolved since were not considered to be feasible.

In great part the work to date has related to establishing the nature of the solidification of metals. Proper risering, gating and mold practices depend on a knowledge of the manner by which metals solidify. In retrospect, "bleeding" or "pour out" tests provided rough approximations of the initial rates of freezing for certain metals such as steels. However, no information was provided as to the nature of freezing or of rates and times of final solidification. For metals such as irons or non-ferrous types it was not even possible to obtain reliable relative freezing rate information at initial stages. Figure 1 illustrates the bleeding test method and its shortcomings. Pour out at early and intermediate stages of solidification is illustrated schematically. The dendrites are drawn as single columnar structures for simplification. It is recognized that mushy zone may, in many cases, consist of layers of dendrite crystals with various orientations, however, the degree of solidification (fraction of liquid and solid) is as represented by the schematic picturization of the skin growth process.

It is illustrated that the remaining skin represents the actual solid skin plus part of the "mushy" material comprising intermixed solid and liquid. Under certain conditions part of the mushy material is dislodged and bled along with the liquid, under other conditions, which will become clear in later portions of this report, the mushy condition attains sufficient coherency to prevent bleeding and pour out is not possible. At this stage of our knowledge two general opinions prevailed regarding results of bleed tests:

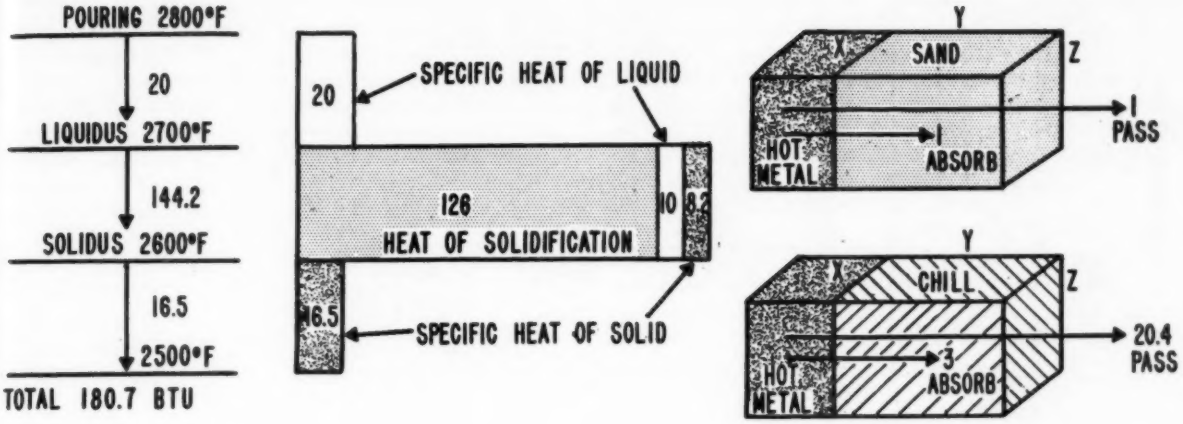
Opinions on Bleed Tests

1. It represented misleading information since the thermal gradients which existed in solidifying castings should result in producing solid, mixed liquid and solid, and liquid zones as demanded by the constitution diagram. (Minority opinion).

2. The data were accurate, i.e., at any time a given position was either completely solid or completely liquid which conclusion necessitated the further conclusion that the phase diagram did not apply to the "non-equilibrium" freezing of castings. (Majority opinion).

The first major application of the Analogue was aimed at resolving this question. The problem specifically concerned the determination of the nature of the solidification of steel plates of various thicknesses in comparison to bleed test data obtained by Clark at the Naval Research Laboratory. Two conditions of molding were used, sand and chill backed by sand. Figure 2 illustrates the general nature of the results obtained by the Analogue and pour out methods. The Analogue answer was characterized by two freezing curves, one denoting the time of *first formation* of solid metal at various distances from the casting wall and the other the time of *complete solidification.*

The two curves indicated that freezing proceeded by the wavelike movement of a band of solidification (mushy zone) from the surface to the center. The pour test data (X points) characterize soldification as proceeding by the growth of a completely solid skin at rates equal to the rate of initial formation of solid as deduced by the Analogue. The pour out test points were not carried beyond 0.6 distance to the center for the sand case and 0.8 distance for the chill case due to inability to bleed the castings beyond these respective times. Thermal analysis methods developed at Naval Research Laboratory at a later date have shown that the Analogue predictions were correct in all respects.



Figs. 3 and 4—Left: Heat liberated during cooling of 1 lb of steel from 100 F above liquidus to 100 F below solidus. Right: Relative ability to pass and absorb heat for sand and chill; cube of equal dimension.

By consideration of Fig. 1 it is evident that the pour out method should agree with the "start of freeze" curves of the Analogue. It is also evident that bleeding should not be possible shortly after the start of freezing reaches the 0.6 and 0.8 distance positions quoted above, inasmuch as the start of freezing progresses to the center very rapidly thus entrapping the remaining liquid and preventing further pour out. The width of the freezing bands and the shapes of the initial curves for the case of sand and chill also explain the reason for the ability to bleed to somewhat deeper positions in the case of the chill.

The two curves derived by the Analogue thus present a complete and accurate description of the course of freezing; the various shapes and rates which may be developed provide of wealth of practical information. In other words, these curves are the equivalent of the S curve information in that the rates and mode of transformation (liquid to solid) are defined graphically. Since the bleeding test data corresponded to points on the "start of freeze" wave of solidification, the time which was predicted as complete solidification of the casting by the bleeding tests actually represented the time at which solid *began* to form at the center. In the

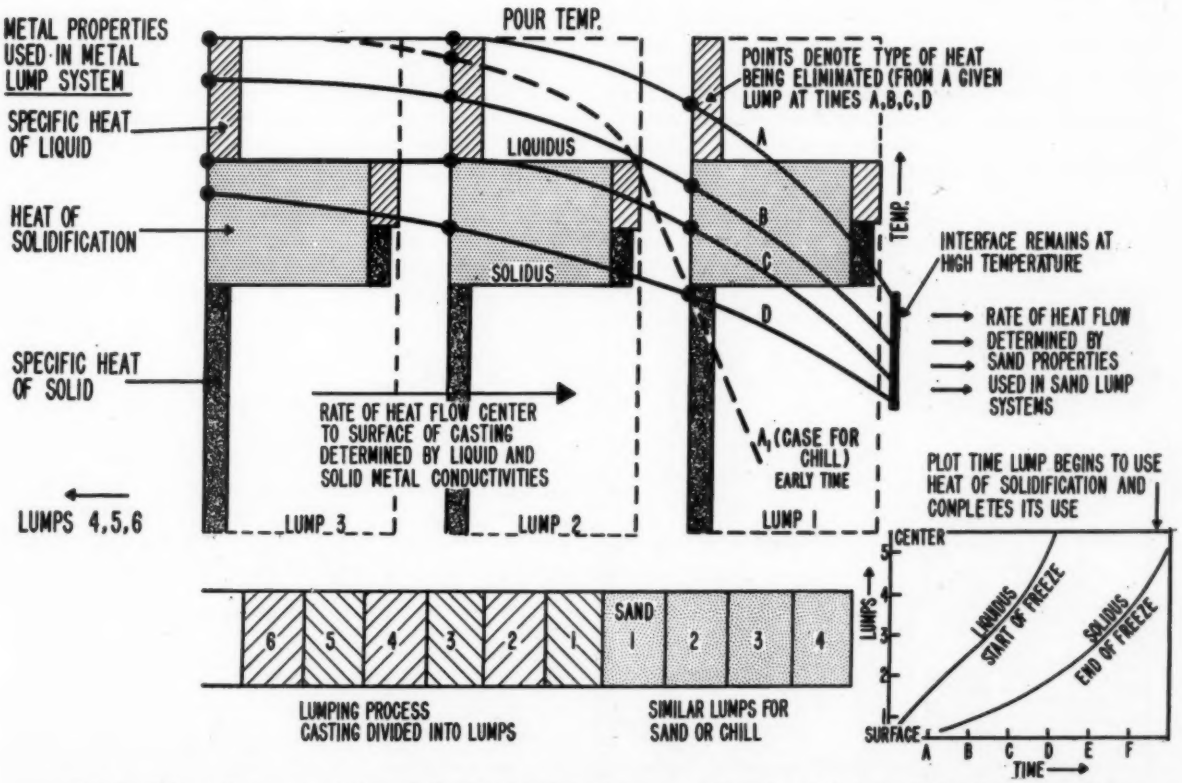


Fig. 5—Schematic representation of the Electrical Analogue procedure for studying solidification of metal.

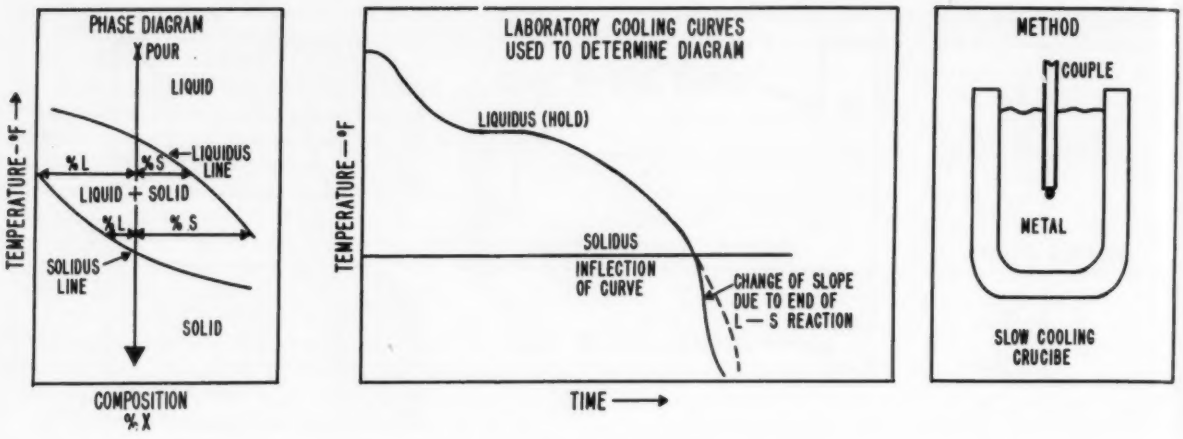


Fig. 6—Cooling curve method for determination of liquidus and solidus points to establish phase diagram.

case of the chill wall the casting completely solidified a short time afterwards, however, in the case of the sand casting solidification did not occur until a much later time. In the case of the 4-in. plate cast in sand (Fig. 2) the bleed tests were in error by a factor of six in the prediction of final solidification times; in the case of the chill castings the error entailed a factor of two.

Unfortunately the understanding of the mechanism of solidification at the time was not sufficiently advanced to permit the above deduction regarding the significance of the Analogue data. It was erroneously concluded that the "start of freeze" wave of the Analogue actually represented the end of feeding (hence, from a practical viewpoint a condition of final freezing) inasmuch as a check was obtained with the bleed experiments. The "end of freeze" curve was therefore ignored as of no significance and accordingly the work performed during the next few years related only to the "start of freeze" curve ("end of freeze" either not determined or not considered).

This, then unavoidable, error was unfortunate for we now know that the metal conditions and characteristics which are of concern to feeding to soundness, relative freezing rates of various sizes and shapes (risering problem), etc., are related to the "end of freeze" curve. The significance of this will be more evident with the discussion of the various sections of this report. It is now recognized that the Analogue correctly presented the course of solidification of the first casting problem which was presented and demonstrated the lack of reliability of bleeding tests. At the same time a completely new picture of the mechanism of solidification was unfolded.

In the discussions which follow, the subsequent Analogue studies will be discussed and compared with direct experiment, i.e., thermal analysis data to indicate the extent to which the Analogue data have been corroborated by research on the foundry floor. For this purpose the general scheme of approach of Analogue and thermal analysis methods will be discussed as introductory material.

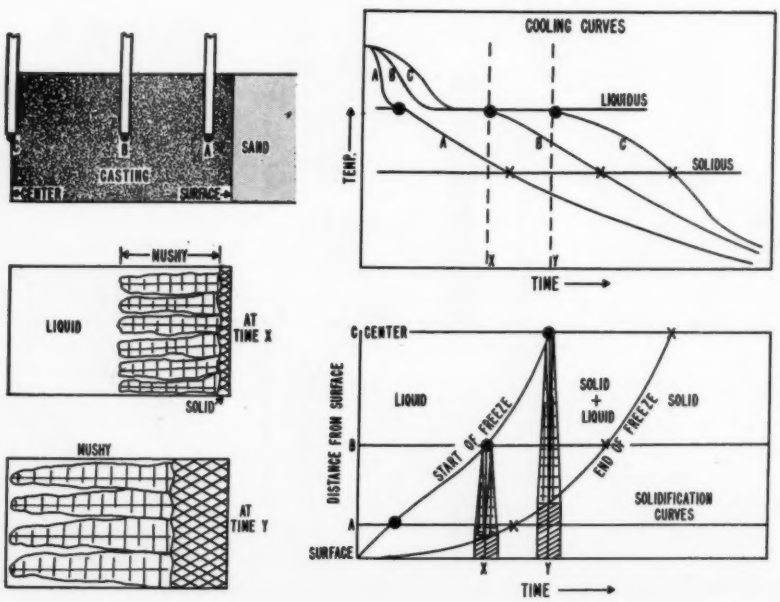


Fig. 7—Thermal analysis of solidification, illustrating case for sand cast steel. Thermocouples are placed at various positions in the casting and the individual cooling curves are analyzed to determine the times of "start" and "end" of solidification. A plot of these times (circles and X's) traces the course of solidification.

It is not within the purpose of this report to provide a detailed description of the operation of the Electrical Analogue from a viewpoint of the actual make-up of the equipment; this has been presented in detail by Dr. Paschkis in the various reports which have been issued. In keeping with the interpretive plan of the present report, it is of interest to discuss the general approach to a casting problem by means of the Analogue. Briefly, the elements which determine the transfer of heat may be represented in terms of electrical elements as follows:

Thermal System (Casting)	Electrical System (Analogue)
Thermal Conductivity	Electrical Conductivity
Thermal Capacity	Electrical Capacity
Temperature	Voltage
Heat Flow	Current Flow

Before setting up an electrical system which is analogous to a casting thermal system it is necessary to know the material properties of the metal and of the mold material. This basic requirement provides certain practical difficulties for the thermal properties of the various metals are not always known exactly. One of the primary reasons for the emphasis on steel solidification during much of the initial work was the availability of reliable data for the thermal properties of this metal.

It should be noted in this respect that the Analogue may be used to "work backwards", i.e., to determine its own constants by adjustments to match the solidification conditions determined by a direct method such as thermal analysis. This means that the unknown constant is varied until the final answer matches the solidification data by direct experiment; the constant thus determined may then be used for future work. Table 1 presents a listing of the thermal constants adopted for steel and sand.

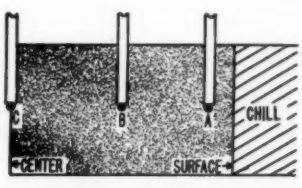
TABLE 1—THERMAL CONSTANTS FOR STEEL AND SAND

	Property	Solid		Liquid	Units
Steel	Specific Heat	.165		.200	Btu/lb, °F
	Density	468		450	lb/cu ft
	Heat of Fusion		126		Btu/lb
	Conductivity	18.4		9.2	Btu/sq ft, hr, °F
Sand	Specific Heat	.28			Btu/lb, °F
	Density	93.6			lb/cu ft
	Conductivity	.9			Btu/sq ft, hr, °F

The freezing range for most of the experiments was taken as 2700-2600 F, this corresponds roughly to a 0.60% C steel (2720-2575 F). This choice was made originally in Analogue solidification work related to steel ingots and was "borrowed" when the A.F.S. work was started at the default of a better suggestion at the time. Actually, the 100 F freezing range is a good average for the freezing range of steels within the practical limits of 0.20 to 0.60% C and the difference in liquidus temperatures between these steels is not sufficient to introduce major errors in the results of the heat transfer problem.

The basic elements of a typical Analogue approach are illustrated schematically in Figs. 3, 4 and 5. Given the constants in Table 1 it is recognized that the solidification of a unit mass must be accompanied by the removal of the various heats shown in Fig. 3; it is evident that the heat of solidification is by far the largest block of heat which must be removed. Accordingly, it follows that for any given rate of heat removal by the mold the time of cooling through the solidification range should be longest, since approximately 7-8 times more heat has to be removed to cause a given temperature drop than at temperatures above or below the solidification range.

The rates at which the heat is removed from the casting depend on the thermal characteristics of the mold wall. The thermal constants of the two materials indicate, Fig. 4, that (assuming equal temperature gradients) in a given time period a unit volume of chill passes 20.4 times as much heat and absorbs 3 times as much heat as a similar volume of sand. Obviously, the sand should be expected to behave as a mildly insu-



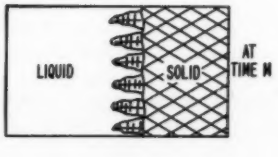
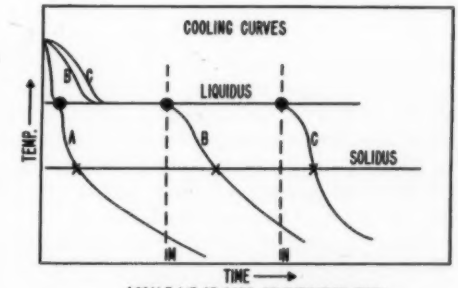
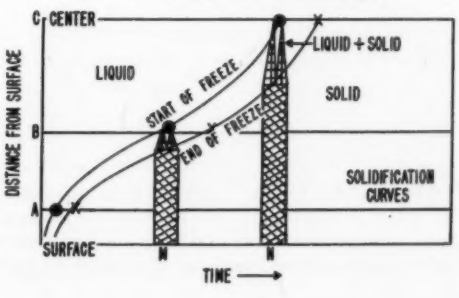




Fig. 8—Steel cast against a chill. Note the "narrow band" freezing in comparison to the "broad band" freezing observed for sand (Fig. 7). This is due to steeper thermal gradients developed in the casting as the result of the faster heat extraction of the chill.

Fig. 8





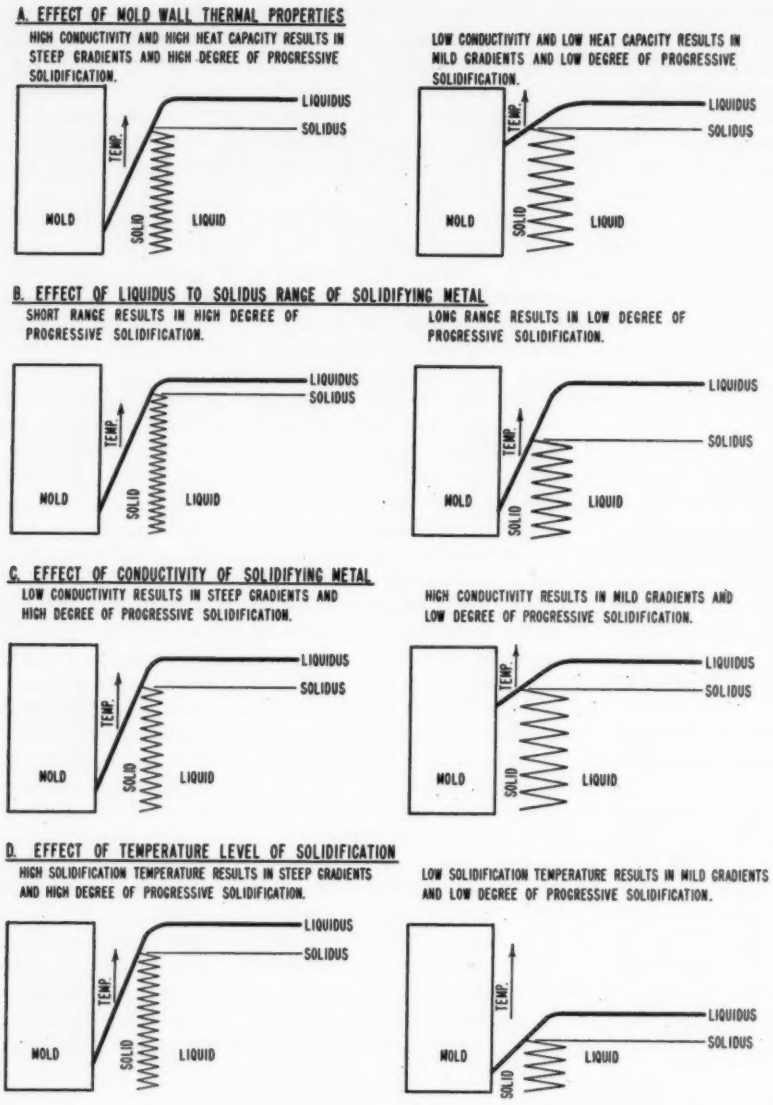


Fig. 9—Mold wall thermal properties, as illustrated in Fig. 7 and 8, are not the only factors in determining the broadness of freezing bands. This illustration shows how the mold wall and three additional factors influence the progressive nature of solidification.

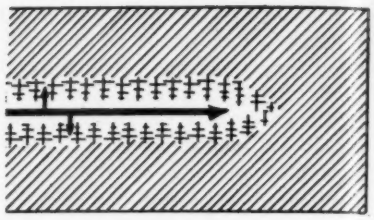


Fig. 10a—Effects of freezing band conditions on ease of feeding to soundness are shown in Fig. 10a and 10b. This shows a free channel condition. Solidification is strongly progressive, mushy zone is narrow, feeding is easy.

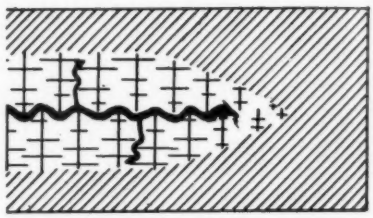


Fig. 10b—Behavior of metal in a hindered channel condition. Solidification is only slightly progressive while the mushy zone is wide and feeding is difficult.

lating material compared to a chill. It will be shown in the discussions to follow that the relative differences in the heat removal capacities of sand and chill are somewhat less than indicated by a simple consideration of thermal constants since the thermal gradient conditions which exist in sand molds are considerably steeper than in the case of chills.

Figure 5 presents an idealized picture of the solidification problem as "set up" on the Analogue electrical system. The casting and mold are broken down into "lumps" or discrete sections. Each section is characterized by having electrical properties equivalent to the various "blocks" of heat. Thus, cooling in the range above the liquidus necessitates the removal of the heat (stored current) denoted by the specific heat block, and so forth. The properties of the lumps in the mold side determine the rate at which heat can move out of the casting. In the sand case (shown) the thermal resistance (electrical resistance) in the sand side is high and heat passage (current flow) is slow. This permits a gradual and progressive removal of the "highest temperature" form of heat from each of the lumps in turn. Thus, the block of heat which represents superheat of the first lump leaks out to the mold system, then the heat of solidification begins to move out.

During this time the superheat present in lump 2 will tend to flow into lump 1—its rate of flow is determined by the thermal conductivity of the castings (electrical conductivity between lumps). In the case of the sand mold which withdraws heat slowly from lump 1 there is considerable time for lump 1 to lump 2 heat flow to occur, then lump 3 to 2, etc. This means that superheat will be eliminated in all lumps before very much heat of fusion (indicating solidification) flows from lump 1. With superheat entirely eliminated the temperature gradient conditions (voltage conditions in Analogue) are as shown in curve C. The process is repeated in the heat of solidification range, each inner lump transfers heat to the next outer lump which is at a lower temperature (voltage) condition. The higher the conductivity of the metal and the slower the passage of heat to the mold, the more equal will be the tem-

perature (voltage) gradient curves which traverse the lump system.

Curve D represents a typical case for casting sand system; it is indicated that at the time lump 1 is completely solid (all heat of solidification removed) extensive solidification has occurred in the inner lumps. If the sand mold is replaced by a chill, very fast heat transfer from the casting results. This means that the heat of lump 1 is literally "ripped" out of it; since very little time is allowed for heat flow from lump 2 to lump 1, etc., a curve such as A1 is developed at very early times. This signifies that in extreme cases lump 1 may be completely solidified before the superheat of lump 2 is removed. The process of heat removal from the inner lumps slows down with time, however, heat passage from casting to chill remains sufficiently fast to result in essentially a block by block freezing rather than several blocks freezing in unison as in the case of sand; compare chill and sand cases of Fig. 2.

This mechanism of solidification from sand and chill walls has been shown to be correct by thermal analysis, gradient curve studies, as will be described. The Analogue data of the starting and ending of solidification of the various positions in the casting are plotted as "start" and "end of freeze curves", shown on the lower right hand corner of Fig. 5. The plot represents the time at which heat of solidification *first begins to pass* from a given lump and the time the *process is completed* and the lump has completely lost all of this heat.

The process of thermal analysis requires discussion, inasmuch as it has been shown to independently recognize the same features of solidification as the Analogue. Basically, thermal analysis is a lumping process also, in that thermocouples are used to determine cooling curves of small lumps of metal at various positions in the casting and the results are analyzed in integrated fashion. This method is basic, inasmuch cooling curves are used to determine phase diagrams (Fig. 6).

It should be observed that such cooling curves do not represent truly equilibrium freezing but freezing under conditions of slow cooling; unless thermal gradients were established in the crucible solidification would never occur. These conditions are not unlike the cooling conditions of heavy sections in sand molds. In fact, if allowance is made for the effect of alloy elements, the actual undercooling of liquidus and solidus points observed in practical castings, even against chills is not greater than 10 F. This should not be surprising for it is well known that metals are highly difficult to undercool unless of extremely high purity (devoid of all small foreign particles. gases, etc.) and the container is of glass-like smoothness.

Figure 7 illustrates a typical thermal analysis approach to the problem of determining the mechanism and rate of solidification of a sand casting. Cooling curves are obtained at various surface-to-center positions across the casting (a number of positions such as shown in Fig. 7) and the time the first drop below the liquidus and solidus temperatures (corresponding to starting and ending of the flow of heat of fusion) are

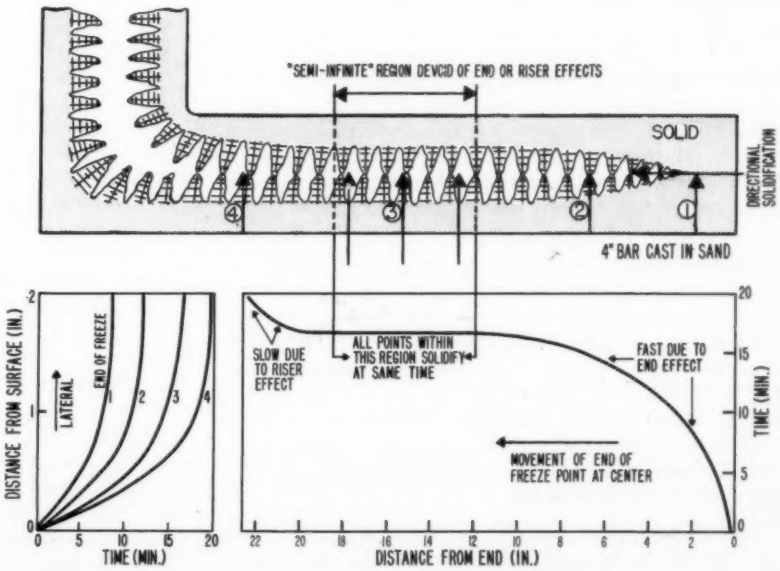


Fig. 11—Mechanism of directional solidification represented by varying rates of solidification at various positions in the casting. For simplification, all solidification curves presented in this report are specific to semi-infinite regions. At other positions there are differences in rate but not in the nature of solidification.



Fig. 12—Radiograph showing center-line shrinkage in a cast steel bar. The shrinkage is developed as the result of lateral solidification proceeding at the same rate over this region. Riser was at right end.

taken as indicating the "start of freeze" and "end of freeze" at the respective locations. The cooling curve for the center couple establishes distinct liquidus and solidus temperatures so that it is not necessary to know the solidification range of the alloy in advance to deduce its nature of solidification. Fig. 8 presents the case for a chill casting.

Comparison of the cooling curves of Fig. 7 and 8 shows that solidification occurs in a narrow band in the case of the chill wall which removes heat at a fast rate and in a very broad band in the case of the sand mold which removes heat slowly. The progress of solidification and the relative portions which are liquid, semi-solid or solid are denoted in the schematic casting diagrams and in the solidification curve diagrams. It is obvious that the solidification curves present a clear picture of the process and rate of solidification from the casting wall to the center of the section. The exact shapes of the solidification curves both as to rates of progression of the "start" and "end of freeze" curves and the width of the solidification band (distance between the two curves at any given time) are determined by the thermal properties of the casting and of the mold materials.

Various Factors Influence Freezing Mechanism

Figure 9 presents a summary of the factors which have been deduced to affect the width of the solidification band (width of mushy zone) developed during solidification. The data which support this generalized introductory picture of mold and metal effects will be discussed in detail in the following sections. The point to be made at this stage of the discussion is that the mechanism of solidification is determined *both by the metal and the mold* and that a great variety of conditions result from the great variety of combinations to be met in practice. However, the specific nature of freezing for a given metal in a given mold system is accurately defined by the solidification curves.

A number of practical deductions may be made from such information. For example, it may be deduced that the feeding of metal which solidifies in such a manner as to develop broad "mushy" regions should be more difficult than for metals which develop narrow "mushy" regions (Fig. 10). Solidification curves for the various commercial metals and mold materials could be assembled in the form such as the "S Curve Atlas for Transformations of Steels" and used as reference by foundrymen in the same manner that S curves are used by heat treaters.

In this respect, it should be recognized that S curves are specific to isothermal transformation and certain interpretations are required for practical use in the case of continuous cooling transformations typical of heat treatment cycles. In the case of solidification curves it is necessary to specify a fixed condition for which the *time scale* is strictly applicable; this condition is that heat flow is occurring only in the direction perpendicular to the mold wall, i.e., there is no heat flow along the axis of the casting (to casting ends) or into the casting (from the riser).

The significance of this stipulation is illustrated in Fig. 11 for the case of a long bar casting. Such a casting may be divided into three zones: (1) a zone which loses heat both in the direction of the side surfaces and in the direction of the casting end; (2) a zone termed "semi-infinite" which loses heat only in the direction of the side surfaces; and (3) a zone next to the riser which is supplied with additional heat flowing from the riser. In simple terms it may be said that the center zone does not know that a riser or a casting end exists. As the bar is made longer the lengths of zones 1 and 3 remain fixed, however, zone 2 becomes longer by the exact amount that the casting is made longer. Obviously, if the casting is made shorter zone 2 disappears.

It should be noted that the nature of solidification is the same in all three zones (and also the riser) the differences in the three zones are only in the rates of solidification. This actually describes the process of "directional solidification", i.e., the directional freezing from the casting end to the riser due to the varying rates of *lateral* solidification which exist along the length of the bar. "End of freeze" solidification curves 1, 2, 3, 4 illustrate the relative rates of solidification (lateral) at various positions along the bar; inasmuch as all positions in the semi-infinite zone freeze at the rate of curve 3, solidification is not directional here.

The movement of the "end of freeze" point along the centerline of the bar denotes the features of directional solidification. It should be noted that since freezing occurs at the same rate in the semi-infinite zone, feeding is not possible at the last stages of solidification. This results in centerline shrinkage at this area; zones 1 and 3 will remain sound (Fig. 12). Note also, that the rate of final freezing in a lateral direction is very fast (several inches per minute) in the last ½ in. at the center. This means that feed metal has to move in along choked-up, tortuous interdendritic paths at very high rates to compensate for the last amounts of liquid-solid shrinkage. The extent of the mushy zone (solidification band width) which exists during the last stages of freezing in great part determines the difficulty of movement of the liquid metal. Because of this difficulty a portion of zones 1 and 3 may develop shrinkage for metals which develop extensive mushy freezing despite the fact that solidification was directional.

Solidification Curves Explain Feeding

The point to be made is that the solidification curves of the various metals provide the necessary information to deduce the relative feeding difficulties. It is also to be noted that solidification curves which relate to semi-infinite casting sections (long bars, plates, cylinders, etc.) may be interpreted to faster or slower solidification rates depending on the position in the casting which is of interest, i.e., depending on the nearness to risers or casting edges. This is the practical interpretation of the solidification curves which the foundryman must make in considering practical problems.

To date all of the published work performed with Analogue and to be discussed in this paper relates to semi-infinite positions. A paper presented at the 1953 A.F.S. convention covers the first case of end effects and demonstrates the relative increase in solidification rates in a short cylinder as compared to a very long one. See "Solidification of Cylinders," pages 142-149 in this volume.

Relative Solidification Times of Castings

Information as to practical methods of calculating the solidification times of castings has been long desired by foundrymen. There are many practical applications which could be made, such as calculating riser sizes, establishing shake-out times, etc. It is important, therefore, to review the progress which has been made in this respect, and particularly the contributions of the Analogue approach to the problem.

At the start of the A.F.S. heat transfer work two entirely different procedures for the calculation of relative solidification times were available. The two methods provided completely different answers and it was not known which of the two methods, or if either, was correct. Of the two, the use of solidification formulae of the type $D=K\sqrt{t}-C$ (skin thickness proportional to square root of elapsed time minus a delay constant) was more generally known. These formulae merely represented the rate of growth of the skin developed in bleeding test studies. It was known that the formulae predicted skin growth only to approximately half thickness and that more complex and difficult formulae would be required to predict the last half of the skin growth cycle observed in the bleeding tests. In view of the demonstrated inadequacies of the bleeding test data it may now be concluded that such data of this type as exist in the old literature are of no significance to the calculation of relative solidification times.

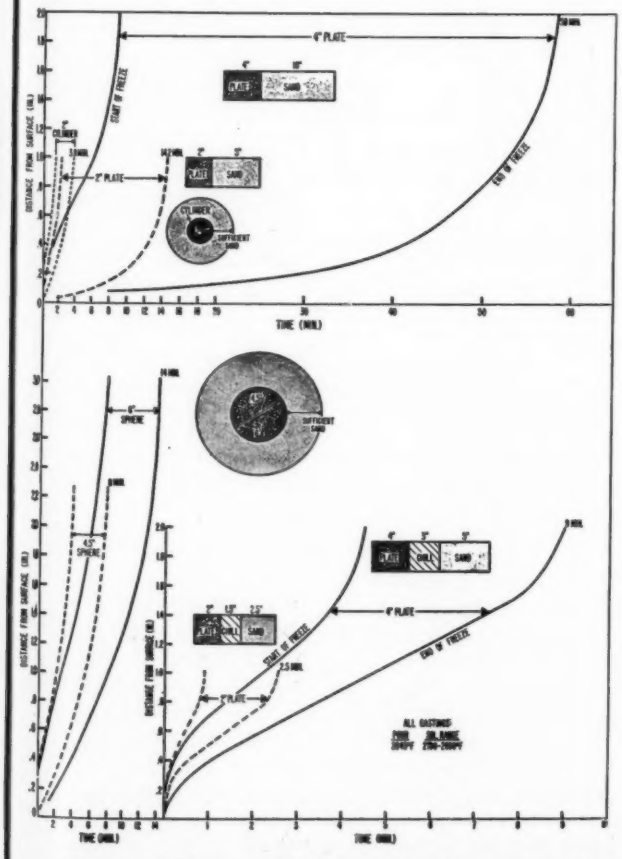


Fig. 13—Analogue solidification curves for various steel castings illustrate shape, size, and mold effects.

The second method which was available is credited to Chvorinov; the method was evolved from theoretical considerations and justified on a basis of thermal analysis check data. Chvorinov's solution was first evolved for semi-infinite plates and expressed as the formula:

$$T=(V/SA)^2/M^2$$

T = freezing time, hours; V = volume; SA = surface area; and M = constant (2.09).

While Chvorinov derived the formula only for the case of semi-infinite, i.e., large plates, it was claimed that the equation is valid for castings of finite size with only small error. It was also claimed by Chvorinov that the relative freezing times of two shapes is predicted by the ratio of the relative $(V/SA)^2$, that is:

$$\frac{\text{Solidification time of Casting A}}{\text{Solidification time of Casting B}} = \frac{(V/SA)^2 \text{ of A}}{(V/SA)^2 \text{ of B}}$$

Clearly, the information provided is of great value if it could be shown to be valid. The Analogue accordingly was directed to resolving this problem. Figure 13 presents Analogue solidification curves for various simple shapes, illustrating the case for sand and chill wall solidification. The same time-distance scale is used for the sand casting while an expanded scale of time is used for the case of the faster solidifying chill casting. These various curves were assembled into one figure to emphasize the effects of shape, thickness of casting and mold material on solidification times.

The effect of shape (surface area to volume ratio) is very great as may be deduced from observation that a 4-in. plate requires 58 minutes to solidify, while a 4.5-in. sphere requires only 8 minutes, i.e., approximately the same time as required for a chilled 4-in. plate. It is also noted that the effects of thickness variations for the same shape are not in direct relation to the thickness but to the square of the thickness dimension as predicted by Chvorinov.

Various calculations are presented in Appendix A to illustrate the reliability of the $(V/SA)^2$ relation, at least for shapes of simple geometry. It may be noted from the variety of calculations which are presented that there is considerable promise to the method; in many cases the calculations are very nearly exact. At our present state of knowledge it is concluded that the freezing times of simple shapes such as plates, cylinders, spheres, etc., may be calculated with a high degree of precision using Chvorinov's formulae.

Get Rough Approximation of Freezing Time

Bars and probably rectangular and cube shapes freeze approximately 10-15 per cent slower than indicated by Chvorinov's formulae. The use of a modified constant (1.92) evolved by the Naval Research Laboratory permits accurate determination of freezing times for these shapes. It should be observed that these relationships hold for normal superheats of 150-200 F. Significant variations outside of these limits result in changes of freezing times in the order of 5-10 per cent (to be described in the sections which follow). Thus, as a generalization it may be stated that the calculations will generally provide answers in the

order of ± 15 per cent of the actual values. For the case of short bars, cylinders or plates (roughly of a length *less than* 4 times the thickness) the calculations are only rough approximations of the actual freezing times.

It should be recognized that for purposes of calculating freezing times for the determination of riser sizes, complex castings require breaking down into the various sections which are fed by specific risers. This process, which has been explained in detail by Caine will be developed to a high degree of exactness once the feeding range and the effect of one section on the solidification of other sections which are joined to it are known accurately. Lately, feeding range data have become available, however, data of inter-effects of sections are still required. The obtaining of such data represents the next logical approach to the problem of complex castings.

In review of the published Analogue research reports concerned with relative freezing times it may be noted that the conclusions of these reports are in direct contradiction to the above interpretive comments regarding the Chvorinov approach. This is due to the incorrect assumptions, at the time, that "start of freeze" rather than the "end of freeze" curves indicated solidification times. Since the $(V/SA)^2$ relationships do not hold for the "start of freeze" curves it was concluded that the Chvorinov relationships were not valid even for simple shapes. With the realization that the "end of freeze" indicated the correct solidification end times the error was rectified and a review of the earlier data indicated the need for the revised conclusions which have been presented in the foregoing sections of this report.

Effect of Metal Variables on Solidification

The effects of superheat level and solidification range have been investigated in detail by the Analogue and checked by thermal analysis experiments. The data which are available present a clear picture of the effects of these variables. Figure 14 presents Analogue solidification curves for 6-in. spheres and 4-in. plates cast with various levels of superheat, and similar Naval Research Laboratory data obtained by thermal analysis of 7 x 7-in. cast steel bars. It is readily apparent from these curves that high superheat causes an initial delay in the progression of the "start of freeze" curves. This represents a delay during the time superheat is being eliminated. Such an occurrence should be expected inasmuch as the mold accepts heat at a given rate and the delivery of superheat should occur at the expense of heat of fusion, thus resulting in a slower rate of formation of solid.

It is indicated also that after superheat is eliminated the "start of freeze" curves become parallel, i.e., solidification proceeds at the same rate regardless of the initial superheat, however, the curves are displaced on the time axis by an amount equal to the total delay occasioned during the loss of superheat. The relative displacement of the "end of freeze" curves is somewhat greater than that of the "start of freeze curves" resulting in broadening out of the solidification band, which signifies that freezing occurs with the formation of a somewhat wider mushy zone. The effect is not pronounced, however. The greater

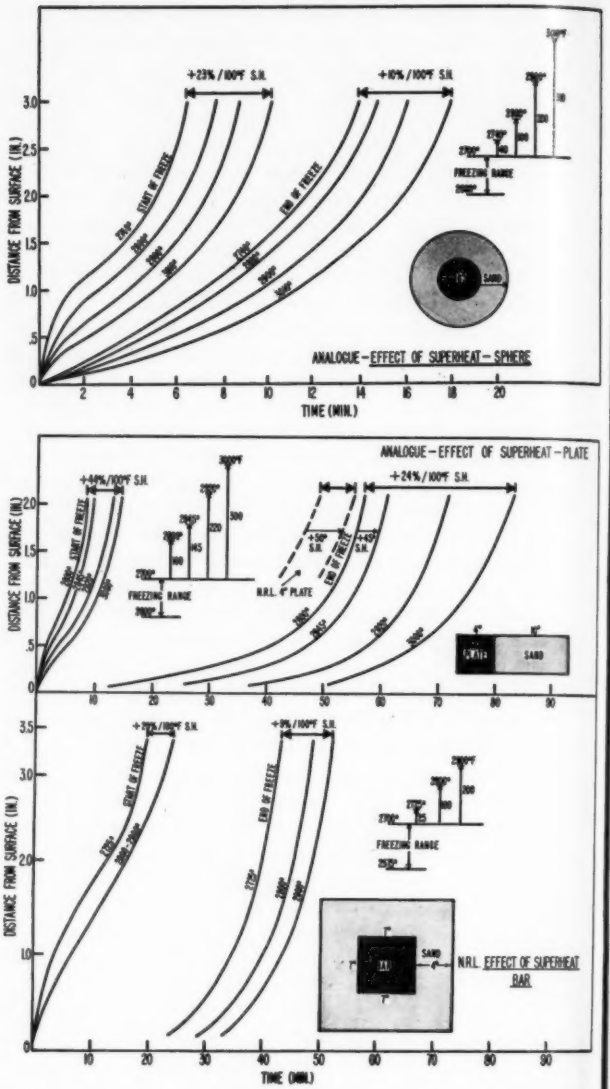


Fig. 14—Analogue and thermal analysis curves show superheat effects for various forms of steel castings.

effect on the "end of freeze" curves may be ascribed to the heating of the mold wall which effectively has the same result as a slightly warm mold.

Good Reason to Pour Cold?

There also appears to be a greater effect in displacing the solidification curves in the case of plates as compared to spheres and bars, which indicates that the effect of superheat is somewhat dependent on shape factors. The evidence in this respect is not conclusive, however, it is observed that a "24 per cent delay per 100 F superheat" is developed in the case of the plate while only "10 per cent delay per 100 F superheat" is developed in the case of the sphere and bar castings. If further research should show this to be true there appears to be good reason to pour cold, because shapes of high V/SA ratios (slow freezing shapes) such as the cylindrical shapes used for risers develop less delay

than rangy shapes such as plates. Obviously, a riser system which is barely adequate for a rangy casting at low pouring temperatures would revert to being inadequate at higher pouring temperatures. More research on this particular question appears to be highly desirable in view of its practical significance.

The effects of solidification range are illustrated in Fig. 15. It is apparent that the Analogue and thermal analysis methods are in agreement that narrower freezing ranges result in more progressive freezing. This signifies that the mushy zone developed during freezing is narrower, hence conducive to easier feeding. This fact has been checked experimentally and it was shown that 0.10% C steel which has a very narrow freezing band will feed to soundness for much longer distances in flat plates than 0.30% and 0.50% C steels which have broad freezing band characteristics. It may be observed also that the Analogue and thermal analysis predictions, while in the same direction, apparently disagree as to the degree of the effect. The disagreement may not be real, however, since the V/SA ratios of the two castings used are quite different (plate vs. bar). This represents another case of information which is required regarding shape factors.

The effect of the conductivity of the solidifying metal on the end of freeze curves was also investigated by the Analogue. It was found that changing the conductivity by as much as ± 20 per cent of the values determined for steel had no significant effect on the final solidification time. This is not surprising inasmuch as the conductivity of the steel is very much greater than the conductivity of the sand; accordingly the factor which limits heat flow is the sand itself.

Conductivity of Metal Not Big Factor

A water analogy may be made to this problem. If the rate of heat flow from the casting into the sand is represented by the rate of water flow through a small diameter pipe, it will be apparent that changing the size of the already larger pipe which exists in the casting (higher conductivity through the casting) will have no effect on rate of heat passage (water flow) into the sand. Accordingly, solidification times should remain unchanged.

This statement holds only for the case of sand molds. In the case of chills the conductivity is essentially similar to that of the solidifying metal, hence it should be expected to have a great effect on solidification rates. Analogue studies related to the effect of conductivity for chill walls have not been carried out, however, thermal analysis studies have indicated that such is the case.

The effects of specific heat and heat of fusion were also investigated. As should be expected, increasing the total amount of heat which must be removed during solidification has a pronounced effect on final solidification time. Increasing heat of fusion by 10 per cent increased solidification time by approximately the same percentage. Increasing specific heat by 10 per cent had a considerably smaller effect. A consideration of the relative fraction of the total heat which must be removed during solidification, indicates that 10 per cent increase in specific heat means the addition of considerably less extra heat than a 10 per cent increase in heat of fusion. Obviously, the effect on solidification time should be in near proportion to the extra heat added or subtracted.

Studies of metals other than steels by the Analogue have been of relatively limited nature to date. Figure 16 presents Analogue data and comparative thermal analysis data for aluminum and copper alloys. The solidification curves of these alloys are compared also with curves determined for steel. It is apparent the two

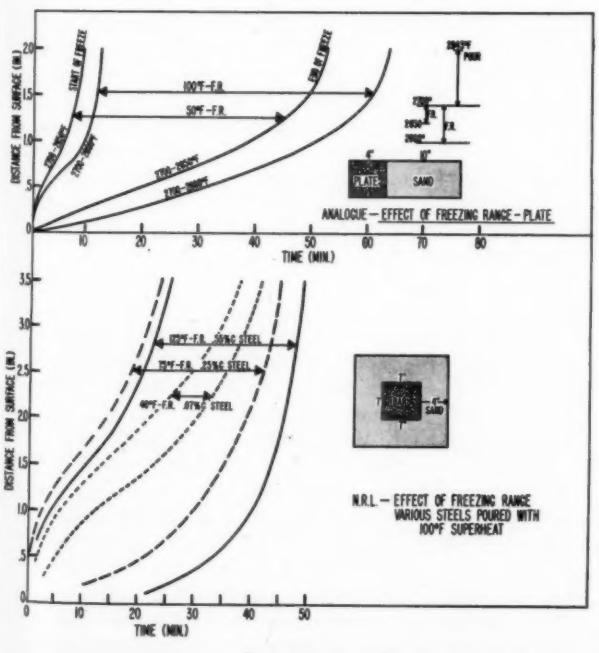


Fig. 15—Analogue and thermal analysis curves illustrate freezing range effects for steel bars and plates.

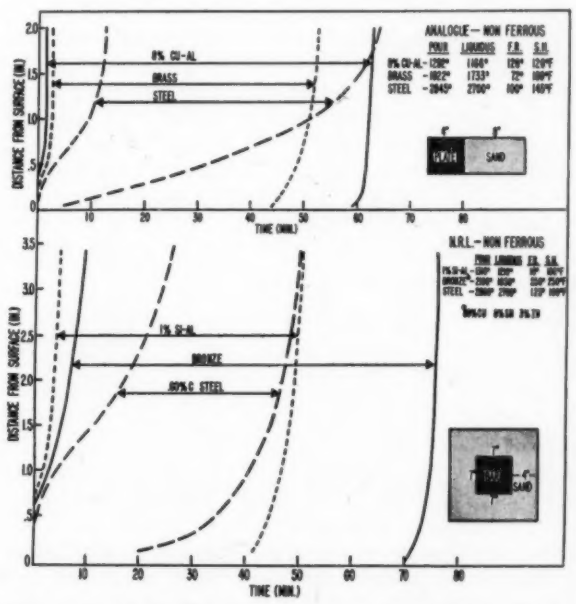


Fig. 16—Solidification curves determined by Analogue and thermal analysis for non-ferrous metals compared with steel. High conductivity non-ferrous alloys show "broad band" freezing of entire casting section.

methods are in agreement in denoting that the solidification of these very high conductivity alloys is characteristically different from the solidification of steel which has lower conductivity.

While the steel develops a completely solid skin at intermediate solidification times, the non-ferrous alloys do not do so until very late stages of solidification. As the result, the subject non-ferrous castings becomes completely mushy from surface to center during a period which may be as long as 90 per cent of the total solidification time. Solidification during this time proceeds by gradual thickening of the solid dendrites throughout the casting until very little liquid remains. When the final freezing wave moves through the casting there is very little liquid left to solidify and as the result the "end of freeze" wave proceeds at very high rates as denoted by the steepness of the final solidification line.

The difficulty of feeding the tortuous, interdendritic channels existing throughout the casting from surface to center under conditions of very rapid final freezing accounts in part for the interdentric shrinkage and leakage channels which are typical of such alloys. The tendency of such alloys to "puncture through" from the skin when a gross shrink area is formed is also related to the lack of a completely solid skin at intermediate stages of solidification.

The development of general freezing throughout the casting despite narrow solidification ranges (10 F freezing range for 1 per cent Si-Al alloy for example) may be ascribed to the extremely high conductivity of these alloys. Effectively, only the very slightest thermal gradients can be developed across a solidifying casting, since heat flow can occur from the center outward with great rapidity (Fig. 9). Thus, the temperature near the surface can never be much lower than the center temperature, with the result that the solidification conditions which exist near the surface must necessarily be only slightly in advance of the solidification conditions which exist at the center.

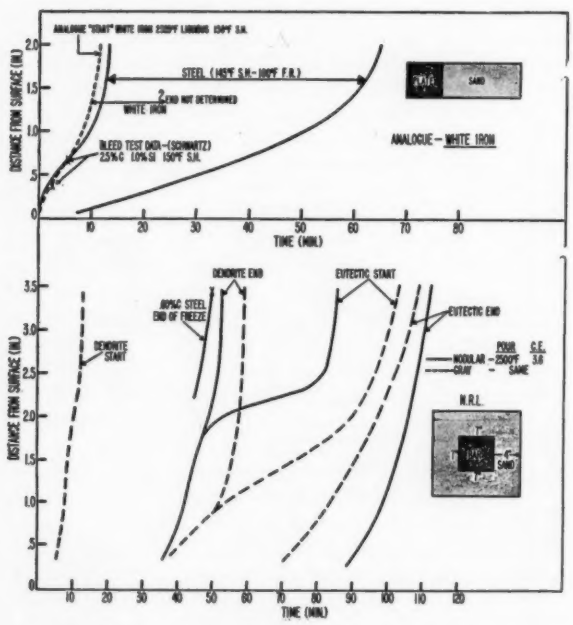


Fig. 17—Separate dendrite and eutectic freezing waves move across castings. Analogue "start of freeze" and bleed test data for white iron are compared with steel "start of freeze." Thermal analysis solidification curves for gray and nodular irons compared with "end of freeze" (eutectic end) for 0.60%C steel show that solidification of irons is much slower.

It is only with freezing ranges of essentially nil values (very high purity aluminum or copper) that such metals may be made to freeze progressively in sand molds. The only alternative for high conductivity alloys having an appreciable freezing range is to use mold walls of very high conductivities, i.e., to remove heat faster than can be moved across the solidifying casting. For aluminum and copper alloys this obviously is not practical, although thin, water-cooled mold walls may satisfy the requirements.

It is observed from Fig. 16 that the solidification times of the non-ferrous alloys are in the order of the solidification of steel and longer. Two factors related to the rate of heat removal by the sand act to prolong the solidification time: (1) the lower interface temperature and (2) the lower conductivity of the sand at the lower temperature. The latter effect was clearly shown by the Analogue in an attempt to check Hunsicker's bleeding tests of aluminum. The first Analogue tests were made using the same sand conductivity which was used for steel, from which it was determined that the "start of freeze" curve traced an earlier path than the skin thickness curve developed by the bleeding tests. On reconsideration it was decided that an error had been made in that the lower conductivity of sand at aluminum solidification temperature should have been used. When this was done an excellent check was obtained.

Investigation of the solidification characteristics of irons by the Analogue were performed only for the case of white iron of malleable grade (Fig. 17). In this case only the "start of freeze" curve was obtained and found to provide a good check at early stages with the skin growth observed for bleeding tests by Schwartz. Figure 17 also presents comparative solidification curves for steel to illustrate that the rates of initial freezing are quite similar. Inasmuch as the initial mode of freezing represents austenite dendrite formation in both cases this is not unexpected.

Thermal analysis studies have indicated that the mechanism of solidification following the initial start of dendrite formation is considerably more complex than for steel (Fig. 17). It is observed that the solidification curves indicate the movement of several freezing waves across the casting section as demanded by the phase diagram of the complex iron alloys. It is also observed that the final solidification times of gray and nodular iron occur at considerably later times than the solidification of steels—at least twice as long is required for complete solidification than for a 0.60% C steel. Several factors probably account for this behaviour: (1) lower solidification temperature; (2) much wider solidification ranges necessitating the removal of extra amounts of specific heat of solid during solidification; (3) lower conductivity of the metal as the result of graphite formation.

It is apparent from the questions which have arisen concerning plain carbon steels, and the relatively sketchy information which exists relative to other metals, that a considerable amount of research remains to be performed. The methods to be used are now well defined and the interpretations which may be made are fairly well established. Ultimately a collection of solidification curves will become available to match the valuable S curves, which also have required years to gather and for which the process continues.

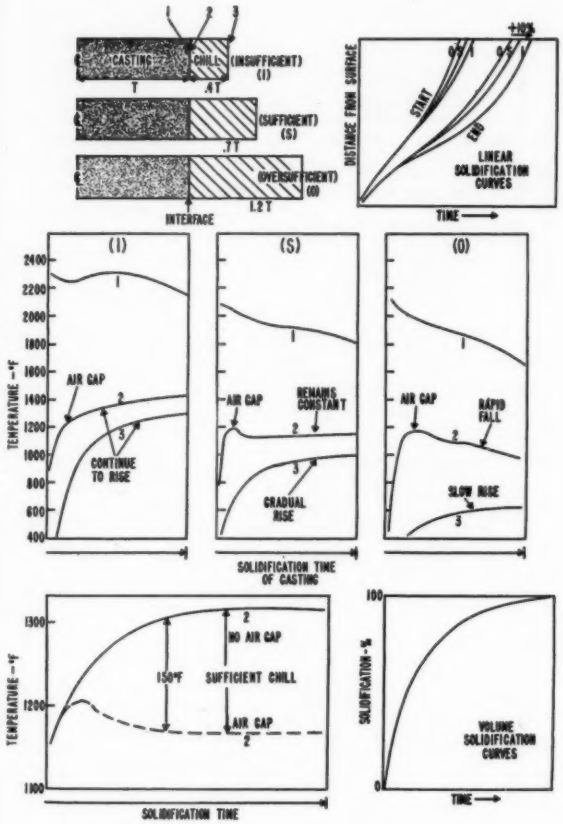


Fig. 18—How chill thickness affects solidification rates of steel. Effects are related to heat capacities of chills; air gap decreases rate of heat transfer.

Mold Variables

The temperature conditions in the mold, insofar as the solidification of the casting is affected thereby, are of considerable interest to foundrymen. It was shown in the previous sections that the rate of heat extraction by the mold when varied over wide limits, such as sand to chill, not only affects the rate of solidification but also the nature of solidification itself. It is the purpose of the discussions which follow to consider the question of variations in mold conditions in greater detail. The effects on the solidification of the metal, as well as the reasons for such, will be considered inasmuch as an understanding of the heat transfer conditions in the mold provides the information required to effect practical control over the solidification of castings.

When Mold Is Saturated With Heat

At the start of the A.F.S. heat transfer studies it was known that the temperature gradients in the sand adjacent to the casting were relatively steep in comparison to chill gradients; also, that the interface temperature of the sand approached the temperature of the solidifying metal. Quantitative data as to the effect of variations in the properties of the sand, thickness of mold, dry vs. green sand, chill thickness and other such practical variables were not available. Following the exploratory work which provided quantitative data as to the solidification of metals under one specific condition of sand properties (dry sand of fixed conductivity), the Analogue investigations were directed to similar exploratory work related to mold variables. At the same time thermal analysis data became available which served as a check on the Analogue data as well as providing related information.

It should be recalled that the mold thickness conditions assumed for the previously discussed Analogue and thermal analysis studies related to "sufficient mold walls." The significance of this concept requires discussion. In simple terms this means that the mold walls were assumed to be of sufficient thickness to prevent a significant rise in temperature at the flask during the course of the solidification of the casting. In extreme cases the rise in temperature may be sufficient to approach heat saturation. With the approach of saturation the mold is no longer able to absorb heat (specific heat capacity is used up) and while it can continue to pass heat to the exterior, the rate of heat transfer may

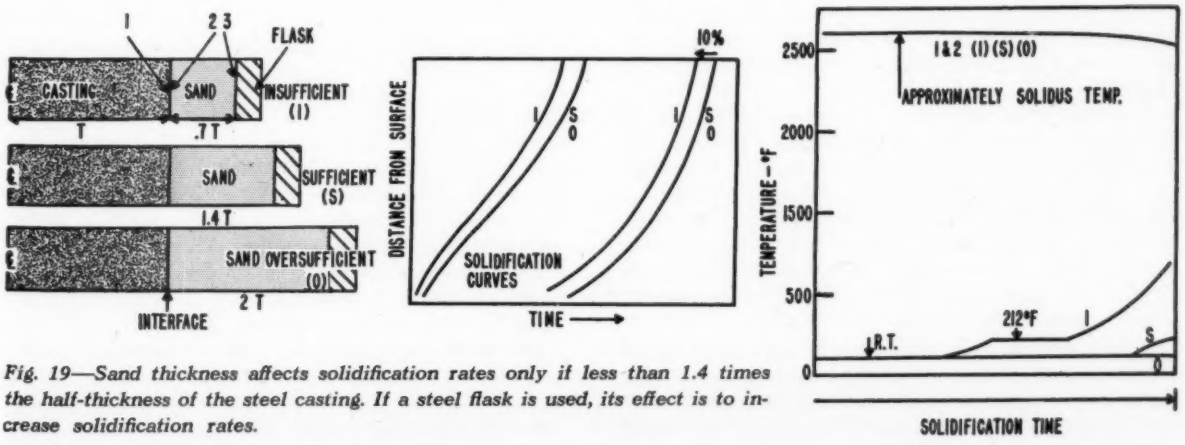


Fig. 19—Sand thickness affects solidification rates only if less than 1.4 times the half-thickness of the steel casting. If a steel flask is used, its effect is to increase solidification rates.

be decreased (chill) or increased (sand) as will be explained in the following sections.

Effect of Chill Size

Figure 18 illustrates the case of chill sufficiency for a bar casting (steel) completely surrounded by chills of thickness *equal to, less than,* and *greater than* sufficiency. It is observed from the temperature gradients at the chill interface and chill outer surface that the insufficient chill (0.4 of the casting thickness —0.4T) becomes saturated with heat early during the solidification period and that its temperature continues to rise during solidification. Also, that the temperature difference between interface and surface is small (approx. 150 F) indicating relatively slow passage of heat. If the chill were made much smaller, say 0.1T, the chill temperature would approach the casting temperature at early times and such a chill would act as if it were an extension of the casting—i.e., it would pass but not absorb heat. The effect of such a thin chill would be felt only during the very first period of solidification. For purposes of countering a hot spot without "freezing off" the section it is necessary to use thin chills which have such a short time action.

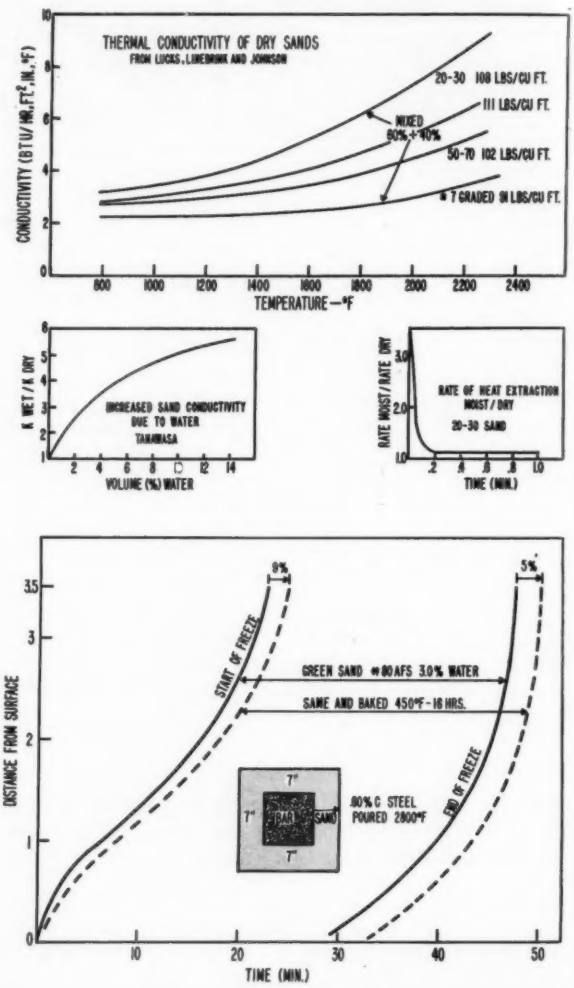
It is observed that the sufficient 0.7T chill develops a nearly constant temperature at its interface during solidification and that the surface temperature continues to rise. Such a chill strikes a heat transfer balance with the casting, passing heat at about the same rate it is received from the casting. The oversufficient 1.2T chill develops a falling temperature after the initial rapid heat transfer is decreased. Heavier chills behave in a similar but more drastic fashion.

In order to understand the described behaviour it is necessary to consider the fact that volume rates of solidification (lower left, Fig. 18) constantly decrease with time, in contradistinction to the increasing rate shown by the previously discussed linear solidification curves. Since a greater total amount of metal is solidified during early times there is a greater heat load to be transferred at early times, hence the natural tendency for the heavy chill to cool-off during later stages of solidification.

There is moreover another effect, i.e., the formation of an air gap at the casting-chill interface due to shrinkage of the solid portions of the solidifying casting. From Analogue and related studies by Paschkis it is concluded that the exact width of the air gap is not important, at least for the high melting point metals, since heat is *radiated* across the chill-casting gap. The formation of an air gap is demonstrated by the Analogue and thermal analysis to decrease the rate of heat flow which prevails during the "contact" period, i.e., during the time that the intimate contact of casting and chill permits direct heat transfer by conduction. The rate of solidification is decreased as the result. There are conditions, however, such that an air gap cannot form (for example casting located over the chill) under such conditions the chill interface reaches a higher temperature (lower left, Fig. 18).

The effect of chill sufficiency is indicated by the solidification curves. Within the limits of chill thickness represented (1.2T to 0.4T), an increase of 10 per cent in solidification time is noted for the insufficient chill. It is also observed that at early times of solidification the metal does not know whether it is against a 0.4T or 1.2T chill. This should be expected, inasmuch as the chills did not have time to heat through, hence all act as very thick chills at early times.

The effects of sand thickness, if a steel flask is used, are the reverse of that observed for the chill, as shown by Fig. 19. It is observed that the casting-sand interface essentially holds a fixed temperature (very close to solidus temperature) during solidification, irrespective of the sand thickness. The temperature of the sand at the flask interface is raised considerably if the mold



wall is less than approximately 1.4T, resulting in a more rapid transfer of heat than in the case of sufficient or over sufficient sand walls. The reason for this may be found in the fact that the sand is heated through and therefore becomes a better heat conductor. It has been shown that the conductivity of sand increases with temperature since most of the heat transfer is by radiation from grain to grain—the higher the temperature the greater the transfer of radiant heat.

This fact also explains why the formation of an air gap (if such occurs has not been established) at the sand interface should not be expected to result in a

significant difference in the rate of heat passage from casting to sand. At least,the temperature records at the interface show no sign of an air gap "hump" observed in the case of chills. The action of the flask in entering into heat extraction from the casting results in faster solidification. With the conditions shown, a 10 per cent decrease in solidification time was observed. It should be noted that unlike the case for the chill there is no effect in increasing the sand thickness above the point of sufficiency.

The action of the flask indicates that metal mold inserts may act as indirect chills even when located considerable distances from the mold cavity. Care should therefore be exercised in placing rods, etc., particularly near risers. The practice of placing risers near flask corners with a small thickness of sand separating the riser from the flask obviously will result in defeating the riser by virtue of a decreasing its solidification time.

The problem of thermal property variations of common foundry sands is difficult to analyze inasmuch as the properties are highly temperature sensitive. Despite the complexity of the problem the Analogue has provided answers which are of considerable practical interest. Before the problem could be studied it was necessary to obtain data regarding the specific conductivities of various sands over the entire temperature range of 75 F to metal solidus temperatures. Conductivity data for dry sands were obtained by Lucks; the effect of moisture on conductivity was determined from data reported by Tanawasa. Figure 20 summarizes the various data; it may be observed that the conductivity of sands increases markedly with temperature in the range of 1000-2300 F and that increasing grain size results in a more rapid increase of conductivity with temperature. This effect is due to the previously described condition of heat transfer by radiation from grain to grain. Filling of the intergrain spaces with fines results in decreased conductivity since the effective path for radiation is diminished.

The effect of moisture content on the "apparent" conductivity is very great as may be deduced from Tanawasa's data. Common moisture contents of 4 per cent increase the effective conductivity approximately 3½ times. These last data are somewhat misleading, however, for as shown by the Analogue the effect of high conductivity due to the presence of water is momentary and moreover represents an "effective" or apparent increase rather than true change in thermal conductivity properties. Figure 20 illustrates Analogue data which indicates that the several-fold rate of heat extraction is developed only during the period (approximately 1 second) that the moist sand at the interface is dried. During this period the water is changed to steam and the extra heat is removed by convection of steam through the air spaces of the mold. As soon as this process is completed the interface is occupied by dry sand and thereafter the controlling effect is primarily that of the dry sand conductivity of this initially thin layer. On this basis the Analogue predicts that the solidification times of large castings should be relatively unaffected by water content of the sand. Thin castings, however, should be greatly influenced by the presence of water.

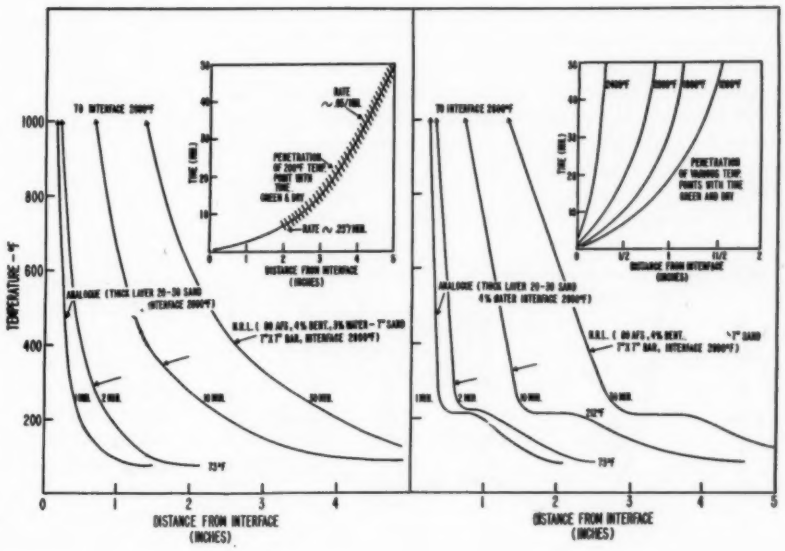


Fig. 20—Graphs (upper left) show how thermal conductivity of sands increases with temperature. Momentary high conductivity when metal first contacts green sand is lost as sand dries, assumes normal conductivity.

Fig. 21—Thermal analysis curves (lower left) indicate small increase in solidification rate of thick castings due to moist sand. For thin-walled castings (probably less than ½ in.) effect should be greater.

Fig. 22—Heat penetration (right) into green and dry sands are relatively minor as shown by scatter band region. Rise of temperature was measured at various positions in mold.

Figure 21 presents thermal analysis data for a heavy section which indicate approximately a 5 per cent decrease in solidification time due to the presence of water. This is in agreement with the Analogue predictions. It is believed that the continuing slight effect of water following the formation of the initial dry layer, is due to the continuing extraction of added heat in the sand layer which is at steam temperatures. The effect may be visualized as being somewhat similar to the presence of a mild chill which moves outward in the mold with the movement of the 212 F zone.

Dry and Green Sand Effects

Figure 22 illustrates the temperature conditions which exist in dry and green sand molds at various times during solidification as indicated by the Analogue and by thermal analysis. The Analogue studies

were limited to very short times since the primary interest was the effect of initial drying of the sand at the interface. It is noted that the Analogue correctly indicates the gradient conditions observed by thermal analysis at very early times. The thermal analysis curves for later times indicate clearly the "steam hold" or isothermal zone which represents the region of steam formation in the case of green sand. The main difference in the thermal gradient curves for green and dry sand concerns the nature of the gradients at temperatures near 212 F. Since the 212 F steam hold is not developed in the case of the dry sand the thermal gradient conditions extend a somewhat greater distance into the mold. This indicates that the depths of heating of a dry sand mold are always somewhat greater than for a green sand mold, the differences are quite small, however.

Figure 22 presents data indicating the rates of penetration of various temperature points into the sand mold wall. The rate of penetration of the 200 F temperature point is relatively rapid compared to that of the higher temperatures. At the end of 50 minutes (approximate solidification time for a 7 x 7-in. bar of steel) the 200 F point has penetrated to approximately 5 inches while the 2000 F point has penetrated only to approximately ¾-in. These data are of interest in indicating the complexity of the problem concerning sand properties (hot strength, plasticity, etc.).

Obviously a steep gradient of sand properties must exist to match the steep thermal gradients observed.

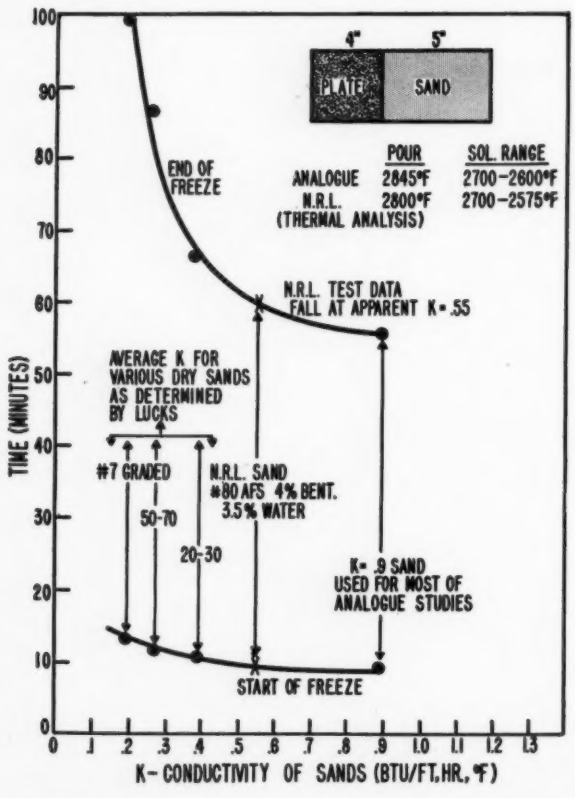


Fig. 23—Analogue "start" and "end of freeze" times for sands for various thermal conductivities.

The steepness of the thermal gradients and the slow nature of heat penetration depicted by these curves illustrate the poor thermal conductivity of sand. It is significant that the effects of sand (variations in thickness of mold wall for example) are best visualized by considered the sand as having the characteristics of an insulating material rather than a conducting material.

Actual differences in thermal conductivity of the sand, as contrasted to the apparent differences due to the short lived steam formation at the interface layer, have a considerable effect on solidification rates. Analogue data for the solidification of a 4-in. steel slab with the sand properties varied to conform to the relative conductivities of the sands used by Lucks are presented in Fig. 23. The conductivity values were taken to represent an average conductivity of the overall gradient zone for ease of study. The effect of conductivity on "start of freeze" time is observed to be rather small, however, for the "end of freeze" time the effect is quite pronounced. It is indicated that decreasing the conductivity by one half (0.4 to 0.2) results in an increase in solidification time of approximately 50 per cent. This surprisingly large effect due to variations of practical sands requires confirmation before acceptance.

Thermal analysis data for the solidification of a 4-in. steel slab in green sand indicate an effective K value of 0.55 when plotted on the curve of Analogue relationships for solidification time and conductivity. It is indicated that the 0.9 K value used for all of the previously discussed Analogue investigations represent a good approximation of the effective conductivity of the gradient zone.

The Analogue investigations for the 1952-1953 period entail an evaluation of the effects of casting edges or "ends" on the solidification of a simple cylindrical shape, varying in length from 1T (length equal to diameter) to 7T. This investigation may be recognized as a first approach to the problem of the solidification of complex shapes, i.e., castings having longitudinal as well as lateral heat flow patterns. Such a methodical step by step approach is required to obtain fundamental answers to the most important of the remaining problems concerning the solidification of castings.

While there is no question as to the importance of this phase of investigation, certain practical aspects related to the complexities of the Analogue problem indicate that this approach should be reconsidered for future planning purposes. The difficulties mentioned relate to the fact that two-directional heat flow requires the use of complicated Electrical Analogue wiring systems, inasmuch as a double system of casting "lumps" must be used. Therefore, it is indicated that such studies may perhaps be conducted to best advantage by using thermal analysis methods, particularly if several shapes are to be considered. This follows from the fact that a new wiring "set up" must be made for each new shape while studies related to metal or mold variables of a single shape may be made simply by varying the capacitance and resistivity of the various electrical components—an easier and considerably less expensive change.

In essence this means that property changes and simple shape changes may be made and studies con-

ducted at a lesser cost than for thermal analysis methods. The cost of a single thermal analysis study is fixed and does not decrease with number of studies, this is not true for Analogue studies for the initial cost of the wiring "set up" is a major item, particularly if the "set up" is complex. This discussion serves to indicate the apparent fields of casting solidification and related studies which are best handled by thermal analysis and by Electrical Analogue methods. Such practical considerations must necessarily serve as guides for the planning of future investigations.

Plan Future Work

A considerable number of important investigations concerned with single direction heat flow (semi-infinite shapes), such as described in the body of this report, remain for future work; several of these have been indicated previously. For purposes of summarization these may be grouped as follows:

1. Metal Variables: Studies concerned with the solidification features of various practical metals. The limitations of this approach involve the availability of proper thermal constants for the various metals.

2. Shape Factor Variables: Studies concerned with the effects of shape on solidification time, i.e., such as the effect of superheat, etc., on the solidification time of "rangy" and "chunky" shapes.

3. Mold Factor Variables: Studies concerned with heat flow rates in sands. Such studies could relate not only to the effects on the solidification of the metal but also on the sand itself. For example: (a) Thermal gradients developed in cope surfaces due to radiant heating which could result in cope spalling, also in drag surfaces from the initial spreading out of the liquid metal which could result in buckling; (b) Temperature losses in flowing through gating systems, and the possibility of solid skin envelope formation in sprues and runners; (c) Rates of heating of cores completely surrounded by metal; (d) Effects of combustibles (oil, sea coal, etc.) on sand thermal gradients and heat extraction rates, etc.

It is obvious that the problem of future planning is concerned not so much with the limitations of the Electrical Analogue as with the choice of the most important of the many investigations which could be made. It is requested therefore that A.F.S. members express their interest in the various possible types of investigations (not necessarily included in the above) by informing the Heat Transfer Committee of their considered opinion as to the information which is most urgently required. The consensus should serve as a guide in planning future programs.

Appendix

Method of calculating freezing times of simple shapes based on Volume to Surface Area relationships.

(A) V/SA relationships for various shapes (D = diameter or thickness).

Shape	V	S	V/SA
Sphere	$\pi D^3/6$	πD^2	D/6
Long Cylinder	$\pi D^2 L/4$	πDL	D/4
Large Plate	DLW	2LW	D/2
Cube	D^3	$6D^2$	D/6
Long Bar	D^3	$4D^2$	D/4

(B) Direct Calculation method (Chvorinov); constant applies only to steel castings.

$$\text{Freezing time (min)} = \frac{(V/SA)^2}{(2.09)^2} \times 60$$

Example: F.T. of 4-in. Plate

$$\text{F.T.} = \frac{(V/SA)^2}{(2.09)^2} = \frac{(D/2)^2}{(2.09)^2} = \frac{(4/2)^2}{(2.09)^2} = \frac{4}{4.38} \times 60 = 55 \text{ min.}$$

Example: F.T. of 6-in. Sphere

$$\text{F.T.} = \frac{(D/6)^2}{(2.09)^2} = \frac{1}{4.38} \times 60 = 14 \text{ min.}$$

Example: F.T. of 6 x 6-in. Bar

$$\text{F.T.} = \frac{(D/4)^2}{(2.09)^2} = \frac{(6/4)^2}{4.38} \times 60 = 30.8 \text{ min.}$$

It was determined as the result of NRL thermal analysis investigations that Chvorinov's constant (2.09) of formula *B* while apparently correct for plates, spheres and cylinders should be modified to (1.92) for bars. Recalculation of the above example using the 1.92 constant yields exact check with the observed solidification times of bars.

$$\text{F.T.} = \frac{(D/4)^2}{(1.92)^2} = \frac{(6/4)^2}{3.7} \times 60 = 36.5 \text{ min.}$$

For purposes of this appendix the modified Chvorinov formula will be termed formula *D*.

(C) Relative calculation method (Chvorinov). If the F.T. of one shape is known, the F.T. of other shapes may be determined as follows:

$$\frac{\text{F.T. of Shape A}}{\text{F.T. of Shape B}} = \frac{(V/SA)^2 \text{ of A}}{(V/SA)^2 \text{ of B}}$$

Example: F.T. of 4-in. steel Plate is known to be 55; use to calculate F.T. of 6-in. steel Sphere.

$$\frac{\text{F.T. Sphere}}{55} = \frac{(D/6)^2}{(D/2)^2} = \frac{(6/6)^2}{(4/2)^2} = \frac{1}{4}$$

$$\text{F.T. Sphere} = \frac{(1)\ (55)}{4} = 14 \text{ min.}$$

Example: Calculate F.T. of 7 x 7-in. steel Bar from F.T. of 4-in. Plate.

$$\frac{\text{F.T. Bar}}{55} = \frac{(D/4)^2}{(D/2)^2} = \frac{(7/4)^2}{(4/2)^2} = \frac{3.05}{4}$$

$$\text{F.T. Bar} = \frac{(3.05)\ (55)}{4} = 42 \text{ min.}$$

This formula may be used to calculate F.T. of any metal if the F.T. of one simple shape is known. For example the F.T. of 7 x 7-in. bronze Bar cast in sand is 75 min.; calculate F.T. of 2-in. bronze Plate.

$$\frac{\text{F.T. Plate}}{75} = \frac{(D/2)^2}{(D/4)^2} = \frac{(2/2)^2}{(7/4)^2} = \frac{1}{3.05}$$

$$\text{F.T. Plate} = \frac{(1)\ (75)}{3.05} = 25 \text{ min.}$$

Example for case of chill. Figure 13 shows F.T. of 4-in. Plate cast with chill walls is 9 min.; calculate F.T. of 2-in. Plate cast with chill walls.

$$\frac{\text{F.T. Plate}}{9} = \frac{(D/2)^2}{(D/2)^2} = \frac{(2/2)^2}{(4/2)^2} = \frac{1}{4}$$

$$\text{F.T. Plate} = \frac{(1)\ (9)}{4} = 2.3 \text{ min.}$$

The tables in this Appendix present Analogue and thermal analysis data for the solidification times of

various shapes (all steel) and the results of calculations. The accuracy of the calculation methods may be evaluated from these comparison data.

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FREEZING TIME CALCULATIONS COMPARED TO ANALOGUE FREEZING TIMES

Analogue	Freezing Range	Super-heat	Casting	Analogue Freezing Time	Calc. R.F.T. Formula C
Steel, .60%C Dry Sand K=0.9	2700-2600F	145F	2-in. Plate	14.2 min.	14.2 Basis*
K=0.9	2700-2600F	145F	4-in. Plate	58	58
K=0.9	2700-2600F	145F	2-in. Cyl.	3.8	3.6
K=0.9	2700-2600F	145F	6-in. Sphere	14	14.2
K=0.9	2700-2600F	145F	4½-in. Sphere	8	8
Chill	2700-2600F	145F	2-in. Plate	2.5	2.5 Basis for R.F.T. calc. below
Chill	2700-2600F	145F	4-in. Plate	9	9

*That is, F.T. of 2-in. Plate used as basis for calculation of the R.F.T. of the remaining castings.

EVALUATION OF FREEZING TIME CALCULATION IN COMPARISON TO NRL THERMAL ANALYSIS FREEZING TIME

Data	Steel	Pouring Temp.	Super-heat	Casting	Actual Freezing Time, Min.	Calculated F.T. Formula B		Calculated R.F.T. Formula C	
NRL	.20%C	2950F	170F	2-in. Plate	13.7	13.7		13.5 basis for R.F.T. calc. below	
NRL	.20%C	2950F	170F	2-in. Plate	13.5				
NRL	.20%C	2950F	170F	2-in. Plate	13.5				
NRL	.35%C	2950F	190F	2-in. Plate	14.0				
NRL	.25%C	2900F	130F	4-in. Plate	50	55		54 (min)	
NRL	.25%C	2900F	130F	4-in. Plate	50				
NRL	.25%C	2950F	180F	4-in. Plate	55				
NRL	.60%C	2800F	100F	4-in. Plate	61				
Analogue	2700-2600F Freezing Range (.60%C)	2845F	145F	4-in. Plate	58				
						B	D		
NRL	.30%C	2950F	180F	4x4-in. Bar	16	13.7	16.2	13.5	16.0 basis for R.F.T. calc. below
	.30%C	2950F	180F	4x4-in. Bar	17				
	.30%C	2950F	180F	4x4-in. Bar	17				
NRL	.30%C	2950F	180F	6x6-in. Bar	38	30.8	36.5	30.4	36.0
	.30%C	2950F	180F	6x6-in. Bar	37				
NRL	.30%C	2950F	180F	8x8-in. Bar	66	54.6	65	54	64.0
	.30%C	2950F	180F	8x8-in. Bar	68				

Actual Freezing Time—as determined by thermal analysis of green sand castings (No. 80 AFS Sand).
Formulae B and C as described in text (Chvorinov's).
Formula D as described in text (Pellini-Bishop).

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DISCUSSION

Chairman: C. E. SIMS, Battelle Memorial Institute, Columbus, Ohio.

Co-Chairman: J. B. CAINE, Foundry Consultant, Cincinnati.

Recorder: J. B. CAINE.

CO-CHAIRMAN CAINE: There seems to be one point that needs clarification for the man in the shop. He is confused by the values given in the paper showing a plate freezing slower than a sphere of equal thickness.

MR. PELLINI: Foundrymen think of plates as rangy castings of high ratio of surface area to volume, certainly much higher than that of a sphere. This is true for a plate of limited dimensions (width and length less than approximately 6 times the thickness), however, this is not true for a plate of greater dimensions for now the "ends" are eliminated from thermal consideration and the central portion of the plate freezes by heat loss through only *two* surfaces. Thus a given volume of metal at a central position in such a long plate is effectively of lesser surface area than an equivalent sphere of diameter equal to the thickness of the plate. This of course results in slower solidification rates for a wide or as is termed in Heat Transfer "semi-infinite" plate. This fact may be visualized in a somewhat different fashion, as follows:

(1) Consider a wide plate and at the center visualize a cube-like volume of dimensions equal to the plate thickness. This cube has only two faces of heat extraction, i.e., the drag and cope surfaces of the plate.

(2) Consider a long bar of a thickness equal to the above plate and visualize again the same cube shape at a central region of the bar. This cube is of the same dimensions as the previous one (same volume) but now we have four faces of heat extraction.

(3) Consider a cube of the same dimensions as above but cast as such. This cube has six faces of heat extraction.

It is obvious that the exact same cube volume in each of these three cases (located in a plate, bar and cast "as such") solidifies by heat transfer to sand of 2A, 4A and 6A areas respectively. Hence, the solidification time of the cube of (3) should be the most rapid, that of (2) intermediate and that of (1) the slowest. In simple terms, long plates solidify more slowly than long bars and these in turn more slowly than cubes (or spheres) of equivalent thickness. This is not a question of different volumes but actually of different surface areas for the same volume.

M. H. HORTON:[1] We have found that the freezing time of iron castings varies greatly when poured in green, or dry sand. For example, cylindrical castings with 1½-in. sections can be shaken out ½ hr sooner without bleeding when poured in undried green sand than when poured in dry sand.

MR. PELLINI: All direct experimental evidence for steel checked by the electrical analogue method indicates that water in the sand has only a momentary effect on the freezing rate. Water may affect the rate of initial skin formation in irons and prevent bleeding, but the effect on actual transfer of heat ½ hr after pouring should be negligible.

MEMBER: I would like to have a discussion on zircon sand.

MR. PELLINI: This is a complex problem of comparison. Coarse-grained silica sands have a much greater conductivity than fine-grained sands, however, the specific heat capacity varies directly with density. This means that during the initial period of heating up of the sand (particularly at internal corners, etc.) the specific heat difference is the important factor which results in faster heat removal by a fine-grained, dense sand. After the sand has saturated with heat, heat transfer is entirely a question of conductivity and now the lower density, coarse sand will be more efficient. This is a round about way of answering the question but it leads to the conclusion that fine-grained zircon sands are more effective than coarse-grained silica sands due to the higher specific heat of the zircon sand. Hence, one should expect zircon sands to be very effective heat removers at corner positions or other isolated regions where the effectiveness depends on ability to "soak up" heat rather than to pass it on.

E. C. TROY:[2] Data from Battelle, published by the Committee showed that conductivities increased with density up to the point where increased number of grains interfaces started a reversal of the trend to decreasing conductivity, even though density was still increasing.

MR. PELLINI: Using a sand of a broad grain size distribution increases density and decreases conductivity. At the mold interface temperatures encountered with steel almost all heat is transferred by radiation across the void spaces. Therefore the coarser and more uniform the particles, the more opportunity for radiation and the higher the rate of heat transfer. Again this applies only to the case of sand in a state of heat saturation.

MR. HORTON: In the malleable foundry we find the finer, more dense sands to be the best in that they develop more rapid skin formation.

MR. PELLINI: That is true. Increased density, due to either greater compaction, or weight of a material like zircon sand, increases rate of heat extraction, but this increase is soon lost as the interface sand saturates with heat and reverts to being merely a conductor (no longer able to absorb heat).

V. PASCHKIS:[3] The effect of conduction and specific heat must be separated. Conductivity is dependent on shape and size of the pore spaces in the sand and there is very little difference between silica and zirconium silicate in this respect. Specific heat is dependent mainly on density and weight. Zirconium silicate is superior to sand because it is about twice as heavy. Specific heat is important early in the time cycle of casting, conductivity is important late in the time cycle.

B. C. YEARLEY:[4] Although he should not be, the foundryman is still confused about riser shape. The riser should remain liquid as long as possible and therefore should have the lowest surface area-volume ratio. This is a sphere.

MR. PELLINI: A short cylinder, top insulated, also satisfies this requirement and is more practical from a molding standpoint.

CHAIRMAN SIMS: The confusion here has to do with volumes and dimensions.

MR. TROY: There are a few exceptions to the use of spheres, or short cylinders as risers. There are some casting shapes that are best fed by rectangular risers.

CHAIRMAN SIMS: Sometimes the foundry uses a layer of sand between the chill and casting. Have we any information regarding heat transfer under these conditions?

MR. PELLINI: A little, but not enough. Heat flow is increased with insufficient sand between the casting and flask, so that the flask itself is heated. This increase in heat flow is sufficient to increase solidification rates. The rate of heat transfer through hot sand is 2 to 3 times that through sand at room temperature and buried chills should be effecive making use of this added conductivity of the sand. This advances the possibility of controlled chill action by means of buried chills.

MR. YEARLEY: We use chills in the malleable foundry. Many times a chill in direct contact with the casting causes hot tears and cracks. If separated from the casting by a thin layer of sand, it still controls solidification without causing ruptures.

R. A. CECH:[5] What data have we regarding the size of chills to be used?

MR. PELLINI: Not enough to go on record. If the chill size is increased slightly from "insufficient," that is to when the chill is just saturated with heat when the casting is solid, there is an increase in solidification rate of about 10 per cent. This increase tapers off to only a further 2 or 3 per cent increase in solidification rate as the chill size is further increased to the extent of doubling or tripling the chill thickness.

MR. CECH: It would be of interest to mention some of the immediate benefits we have accrued as a result of past investigations by the Heat Transfer Committee. We at the National Malleable and Steel Castings Co. have arrived at a simplified method for feeding steel castings by applying the data and

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results obtained from previous experiments by the Naval Research Laboratory. By this method we have successfully produced castings heretofore fed by trial and error methods. Turbine castings weighing up to 7,500 lb finished weight have thus been cast. Granted that most of the investigations are part of long-range programs, there are nevertheless many practical benefits that may be obtained along the way from careful scrutiny of current results. We congratulate those responsible and look forward to any future investigations.

D. L. WATSON: [6] Can the electrical analogue method be used to study graphite formation in gray iron?

MR. PASCHKIS: This problem would be too much for the analogue. There are too many variables that are not dependent on heat transfer.

L. F. PORTER: [7] Need any special factors be considered in the study of heat transfer of thin castings and in the first fractions of an inch of the sand?

MR. PELLINI: The electrical analogue method is especially valuable in the study of heat flow under these conditions, for thermocouple data are questionable close to the sand-metal interface.

MR. PASCHKIS: The changes are gradual in the analogue method down to dimensions of ½ to ¼ in. Some changes and allowances must be made if dimensions in the order of 1/10 in. become significant.

J. T. MACKENZIE: [8] There is some English work in the record showing that thin castings cooled slower in air when shaken out immediately after pouring than when cooled in the mold.

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PRACTICAL FOUNDRY APPLICATIONS OF PRINCIPLES OF RATIO GATING

By

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ABSTRACT

This article describes experiments and actual applications of ratio gating used on several hundred thousand castings of aluminum-base alloy cooking utensils produced by Wagner Manufacturing Co., Sidney, Ohio. This gating procedure was reported by the Battelle Memorial Institute, Columbus, Ohio, in research for the Light Metals Division of A.F.S.

All of the castings produced in our plant are cast in sand, a blend of two naturally bonded types. The composition is controlled to insure proper strength and permeability, with a moisture content of approximately 4.5 per cent. All molding is done in tight, steel flasks with an average size of approximately 13 x 19 in., using a 5-in. cope and a 2½-in. drag.

The alloy produced, an aluminum-base type, contains approximately 4.5 per cent magnesium and 2 per cent silicon, with enough titanium added to insure a very fine grain structure. It is melted in 600-lb, gas-fired furnaces, with rigid control of such items as cleanliness of melting pots, temperature of metal, preheating of chill bars, loading of furnaces in such a manner that all ingots and returns are preheated before reaching the molten bath, cleaning of metal after becoming molten, and avoiding turbulence of metal as much as possible in transfer from the furnace to the mold.

All castings are finished with a clear, high-luster polish. Cross-section thickness varies from ⅛ to ¼ inch. To achieve this excellent finish, it is imperative that castings be relatively free of dross inclusions, large gashole porosity, hydrogen or pinhole porosity, and shrink porosity. If castings are not of this optimum quality, rejects and rework will result.

By following carefully the foundry practices described, it is believed that the metal produced is as free of dross and gas as possible. In our experience, these practices are as essential as any gating or molding technique.

At present, 27 different patterns on which ratio gating has been used are in production. Castings from these patterns vary in weight from 1 to 8 lb, and vary in surface area from 87 to 438 sq in. Through experiments on various patterns, it was found advantageous not only to vary the ratio between the cross-sectional area of the runner and total area of the gates, but also to employ more than one sprue, depending upon the size of the casting. Furthermore, it was beneficial to employ a sprue with a very small area at the base. In our practice, the sprue diameter is ½ in. at the bottom and tapers to 1 in. at the top. On all castings, the gate is very thin—⅛ in.

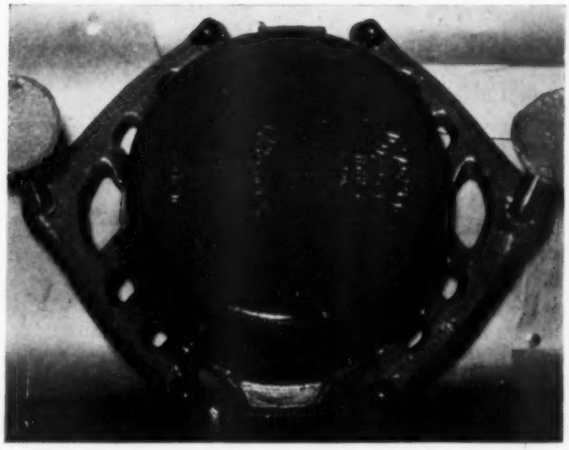


Fig. 1—Cope view of early gating system for 10-in. diameter kettle. Runner is in cope, sprue tapered.

Two particular castings have been selected for detailed discussion in this paper. Photographs have been prepared to record the history of gating used.

Development of Gating Practice

The dimensions of the first kettle casting studied were: diameter, 10 in.; depth, 5 in.; side walls, approximately ⅛ in. thick; bottom section, approximately 3/16 in. thick. All castings are designed with a back draft extending to within 1 inch of the bottom of the casting. The bottom inch of the casting, which is the open end of the kettle, is in the drag, and the remainder of the casting in the cope. This arrangement causes the metal to fill the drag section first, then rise 4 in.

* Wagner Mfg. Co., Sidney, Ohio.

to fill the side walls, and finally flow across the top of the casting to form the bottom of the kettle.

The gating system is illustrated in Fig. 1; runner and gates are in the cope. The gating system consists of two round tapered sprues with double-runner systems from each sprue and a total of 12 finger gates. The dimensions of this particular gating system are: area at the base of the sprue, 7/16 sq in. tapering to an area of 1¼ sq in. at the top; cross-sectional area of each runner, 9/16 sq in. (¾ x ¾ in.). This produced a gating ratio of 1:2.6:1.3; cross-sectional area of each gate, 3/16 sq in. (3/16 x 1 in.).

This practice resulted in many rejects and rework because of large gasholes and surface inclusions. Although the practice had been used for several years, it is now apparent[1,2,3] that imperfect castings were a result of the manner in which the metal entered the mold.

The first beneficial change from this practice was effected when the shape of the sprue was changed from a round to a rectangle measuring ½ x 1¼ in. at the bottom and tapered to ¾ x 1½ in. at the top. A 1-in. deep sump was incorporated directly under the sprue. The depth of the runner was increased from ¾ in. to 1 in. and it was placed in the drag. The runner completely surrounded the casting and the finger gates were connected to form a 3/16-in. thick slit gate, as illustrated in Fig. 2. This produced a gating ratio of 1:1.3:6.2. This method of gating eliminated much of the trouble from misruns, and, at the same time permitted the pouring temperature to be lowered considerably.

Turbulence Trouble

With these changes improvements were noted, but the human element in the actual pouring of the metal into the molds was still present. Carelessness at any time on the part of the pour-off team would result in losses, which were thought to be caused by the large size of the sprue and the large area of the gate. The increased size of the gating system made it possible, through mishandling of the metal, to create turbulence at the base of the sprue and in the metal as it entered the mold cavity.

At about this time, the results of the A.F.S.-sponsored investigation at Battelle Memorial Institute on gating techniques were published.[1,3] After viewing the motion-picture film, on 1:4:4 ratio gating,[2,4] this type of gating practice was applied to a pattern as shown in Fig. 3a and 3b. The use of a rectangular sprue was continued. The runner was reduced in cross-sectional area as each gate was passed. The gate was also restricted to a large degree at the base of the sprue to minimize turbulence in the runner. Finger gates were used with all approaches well rounded to avoid any radical change in flow pattern. A round sump, about 1½ in. in diameter and deeper than the runner itself, was incorporated directly below the base of the sprue as is evident in Fig. 3b. A loose roll or ball of steel wool, which acted as a partial strainer, was employed in the sump.

This particular gating technique proved to be a great improvement over any practice adopted thus far. However, difficulty at two locations in the casting caused the majority of reworks. A fairly large percentage of castings contained dross inclusions at the first finger gate from the sprue. Also large gas porosity had been forced to the top of the casting directly opposite the sprue. A small percentage of misruns still occurred.



Fig. 2—Drag view of kettle shows first improvement in gating. Runner is in drag, slit gate in cope, large, rectangular, tapered sprue is employed.



Fig. 3a and 3b—Cope view (3a) of second improvement, shows finger gates in cope, runner in drag. Runner was decreased at sprue base and as each gate was passed.

A study of scrap and of the design of the gating system led to the conclusion that additional changes must



Fig. 4a and 4b—Present standard gating for 10-in. kettle. Two ½-in. diameter, tapered sprues are used, runners are in drag, ⅛-in. slit gates in cope.

be made to guard against the mishandling of metal on the part of some pour-off teams. The solution to this, it was believed, was to attempt to slow down and quiet the flow of metal before it entered the runner, but still maintain enough pressure to run the casting.

Experiments were started on using as small a sprue as possible with correctly proportioned runner and gates which would not result in an excessive number of misruns. As a result of these experiments, the diameter at the base of the sprue was made ½ in. and the sprue tapered to a diameter of 1 in. at the top, as illustrated in Fig. 4a and 4b. It was interesting to note that the older, experienced foundrymen were difficult to sell on the idea that the percentage of misruns would not increase as a result of running the casting through so small an opening. Actually, it was possible to decrease the pouring temperature by 100 F and at the same time reduce the number of misruns to a negligible value. Experience proved that it was practically impossible to pour metal into this sprue without choking it. This choking of the sprue eliminated some turbulence and exerted automatic control over the pouring rate.

With the sprue design shown in Fig. 4a and 4b, it was believed that turbulence of metal entering the runner would be at a minimum at all times. It was realized, however, that the pouring of metal directly into the sprue is considered bad practice, and will create some oxides and gas which may pass into the runner system and into the casting. With this in mind, the



Fig. 5—Long kettle (12¾ in.) formerly had this gating system with runner in drag along with ¼-in. slit gate. Two large, rectangular, tapered sprues were employed.

runner was extended about 2 in. beyond the far edge of the slit gate. Because this runner is in the drag, it must be filled before any metal can reach the casting. It was assumed that the first metal poured into the sprue would be the more seriously contaminated, and this contaminated metal would remain in the extension of the runner. Again, experience indicated that this runner extension served its purpose, even though some oxide inclusions apparently still entered the casting. These inclusions did not constitute a serious problem because they were small and not numerous.

To obtain the 1:4:4 gating ratio on this particular casting, it was apparent that it was impractical to use finger gates. Because of the limited space, the finger gates would, of necessity, have been $\frac{3}{16}$ in. thick or thicker, thus obviating any skimming action. Since we believed there should be a skimming action of the metal while passing through the gate, the finger gates were eliminated and the desired cross-sectional area was obtained by using a slit gate ⅛ in. thick. This thin gate, it was believed, not only caught more oxide particles, but also had a tendency to minimize further any turbulence that may still be in the runner at the time the metal starts to rise in the casting.

Use Ratio Gating Throughout Shop

This type of ratio gating has been applied to all of our patterns, in all instances employing the ½-in. diameter tapered sprue and the ⅛-in. thick slit gate. The gating ratio has been increased or decreased according to the surface area of the casting, using ratios as low as 1:3:3 for the small castings and as high as 1:7:7 for the larger castings. The number of sprues was increased from 2 to 4 on two of the larger patterns, but, in these instances, the ratio was reduced to not less than 1:4:4.

A second type of casting will be described briefly because the principles illustrated in Fig. 4a and 4b apply. In the past, the second type casting, shown in Fig. 5, had a greater scrap loss record than the round kettle described earlier. The larger kettle measured 9¾ in. in width, 12¾ in. in length, and 4⅝ in. in height.

Note in Fig. 5 that the casting was completely surrounded by a heavy runner which was in the drag.

This fed a slit gate ¼ in. thick, located in the cope. The runner was fed by two large, rectangular, tapered sprues with a round sump directly below each sprue. The dimensions at the bottom of the sprues were ½ in. x 1¼ in. A small roll of steel wool was used as a strainer in the sumps. In this instance, a ratio of about 1:1.8:3.9 was obtained. This method of gating proved to be far superior to our previous methods of gating, which had incorporated the runner and finger gates in the cope and was fed by round, tapered sprues.

The present gating ratio of 1:7:7 is illustrated in Fig. 6. The sprue was tapered and had a diameter of ½ in. at the bottom, and the slit gate was ⅛ in. thick. The sump directly below the sprue was eliminated. In this case, with a shallow drag section, the mold hardness was too high at the location of the sump. When permissible, incorporation of a sump as illustrated in Fig. 5, is believed to be beneficial.

Because the sump was eliminated, the steel wool screen was also eliminated. Steel wool screens were, no doubt, helpful, but if they can be avoided, costs can be reduced and iron pickup can be obviated.

Fig. 7 shows a cross section of the old type gating system employing the rectangular sprue. Fig. 8 shows a cross section of the present gating system and a round, tapered sprue. Note the difference between cross-sectional areas of these two sprues.

Improvement in Yield

Rejects and rework on castings, after finishing from the pattern for the round kettle with the gating system shown in Fig. 1, were approximately 50 per cent. After finishing from the same pattern gated as in Fig. 2, rejects and rework on castings were reduced to approximately 25 per cent.

Rejects and rework on castings, after finishing from the same pattern with the present standard gating practice as illustrated in Fig. 4a and 4b, are approximately 8 per cent. An increase in yield of 10 per cent was achieved over that obtained when using the gating system shown in Fig. 2.

Rejects and rework on castings, after finishing from the pattern for the elongated kettle with the gating system shown in Fig. 5, ranged from 35 to 40 per cent. Rejects and rework on castings, after finishing from the same pattern with the present standard gating system illustrated in Fig. 6, are approximately 9 per cent. This represented an increase in yield of 12 per cent.

It was not possible to maintain this high increase in yield on all patterns. It was possible to achieve an over-all average increase in yield of 5 per cent on all castings by employing these improved gating techniques.

These figures on decreased percentage of losses do not paint a complete picture of the improvements made possible by these gating techniques. The number of castings that are returned for salvage work because of gas holes is now about 75 per cent less than with the earlier gating methods. Seldom more than one gas hole is found in any casting; even then, the gas hole is usually very small and readily repaired. The appearance of the castings has improved greatly as a direct result of the absence of scattered dross on the finished surfaces.

Summary of Present Practice

In reducing the area at the bottom of the sprue, which was proved to be necessary in our process, the possibility of misruns was increased. To minimize misruns, all castings are poured with not less than two sprues which have separate runner systems. This practice eliminates possible trouble from misruns and pro-



Fig. 6—New gating for long kettle. Dimensions and locations of sprues, runners, and gates are as in Fig. 4.

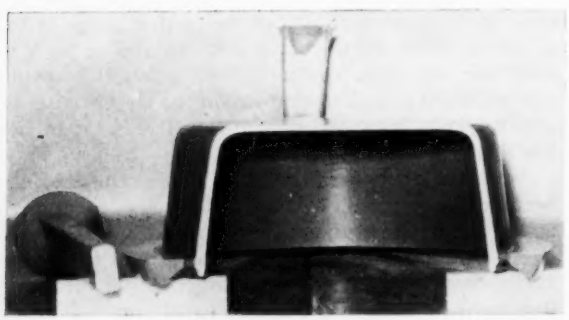


Fig. 7—Vertical section shows early gating system for 10-in kettle. Compare sprue size and casting.

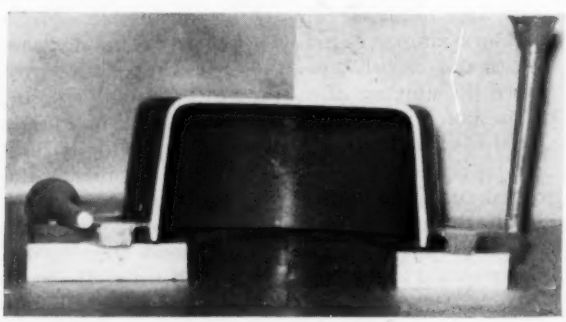


Fig. 8—Improved gating system for 10-in. kettle is shown in section. Note difference (from Fig. 7) in sprue.

vides a more uniform flow of metal up to and over the flat top of the casting.

These experiments and their results apply exclusively to the casting of kettles. No claims are made that the practices or gating techniques described are perfect. They were developed on a trial-and-error basis, and undoubtedly, would have to be changed somewhat when adapted to other types of castings. It is the opinion of the author that ratio gating is a sound method to start re-gating of a casting. Once the correct proportions are reached, there should be a noticeable decrease in rejects.

Acknowledgments

The gratifying results reported in this paper were obtained as a result of the help of such able men as T. D. Stay, T. D. Kramer, R. B. Elliott, and others. Thanks are given to the Wagner Manufacturing Co. for permission to publish this paper.

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1. R. E. Swift, J. H. Jackson, and L. W. Eastwood: "A Study of the Principles of Gating," *Transactions, A.F.S.*, vol. 57, pp. 76-88 (1949).

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DISCUSSION

Chairman: T. D. STAY, Reynolds Metals Co., Cleveland.
Co-Chairman: P. J. PETTO, JR., Arrow Aluminum Castings Co., Cleveland.
Recorder: P. J. PETTO, JR.

MEMBER: If the runner were extended ⅛ in. into the cope, would the oxide collect in the runner in the cope?

MR. LUCAS: Yes, it definitely would be beneficial to have the runner extend into the cope, if you were not concerned about casting yield.

MEMBER: Did you pour any of your test castings two up?

MR. LUCAS: Yes, we did and it proved advantageous. The mold could be filled more quickly thereby making it possible to lower the pouring temperature. This is beneficial in reducing gas absorption and oxide formation during melting and pouring.

D. L. LAVELLE:[1] Was the test sprue ½ in. in diam at the base or was it ½ in. in cross-sectional area? Would a rectangular sprue have been better than a round sprue?

MR. LUCAS: The sprue used in our tests was ½ in. in diameter at the base. We found that there was no advantage in a rectangular sprue over a round sprue. In fact, we found that with a round sprue a much cleaner casting could be obtained.

CO-CHAIRMAN PETTO: What type of melting procedure did you follow for the metal used in these tests?

MR. LUCAS: The metal used was melted in a 600-lb gas-fired melting furnace and fluxed with a commercial dry flux composed of zinc fluoride and zinc chloride.

CHAIRMAN STAY: The practical application of research work sponsored by the American Foundrymen's Society and carried out at Battelle Memorial Institute has been ably presented by Mr. Lucas. As he pointed out it is difficult to "sell" old and experienced foundrymen on the small down sprue, but seeing is believing. His foundry saw the A.F.S. motion picture films which resulted from this research project and they were "sold". Mr. Lucas and his company are to be congratulated for the fine job they have done.

[1] Research Metallurgist, American Smelting & Refining Co., Barber, N.J.

MULTIPLE MOLDING IN A MALLEABLE SHOP

By

Joseph G. Kropka*



Molder setting the cores in a section of a stack mold.

ABSTRACT

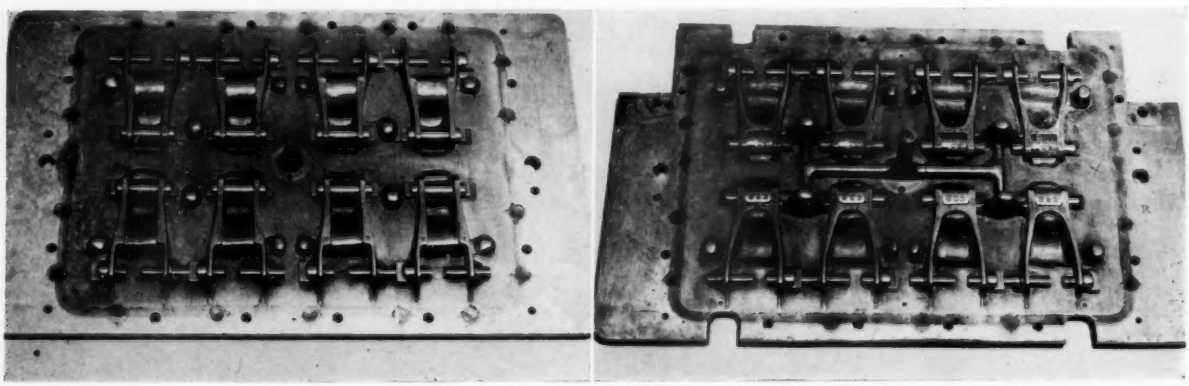
Due to demands for increased production and shortage of skilled foundry labor, multiple stack molding was adopted in the author's plant because of its basic economic factors. The need of continuity of operations for full realization of all possible economies is stressed in this paper. Methods of sand handling, molding, pouring, and shakeout operations are presented. Tests and equipment used are described in detail. Need for standardizing flasks, machines, and molding procedure to meet specifications and production requirements and recommend constant checking to insure quality of products are stressed.

The principle of stack molding is not new. The piston ring industry has used this method of molding rings for years to maintain production at a high level and at a very low cost per casting.

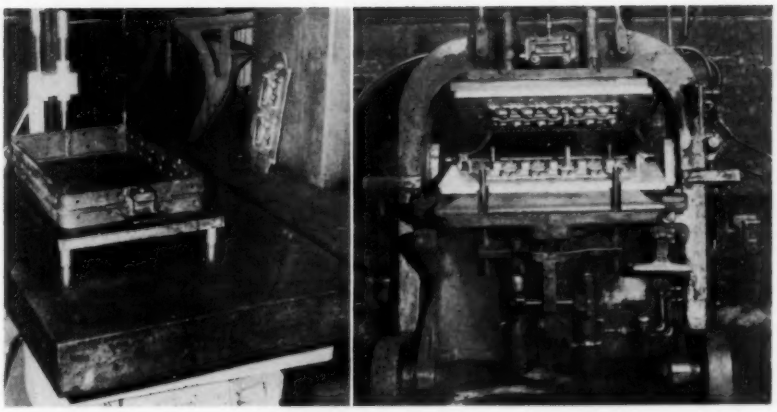
The author confines the description to "multiple" molding methods. This type of stack molding was adopted in our foundry after a series of experiments and studies proved, conclusively, that certain economies could be realized that were impossible to obtain under other methods.

In multiple molding pattern impressions are made in both sides of the mold, which results in one side

* Superintendent, Malleable Div., Chain Belt Co., Milwaukee.



Pressure plates used in stack molding. Drag impressions are made by plate on left, cope plate is at right.



Near right is flask in jig for checking alignment of bushings. Case-hardened bushings minimize wear and insure precision. Straightening equipment is available for putting flasks back into shape if necessary.

Molding machine set up for production of stack mold sections. Patterns on squeeze head will make cavity for drag. Cope patterns are on lower plate.

of the flask being used as the drag and the other as the cope when placed in a stack. The successful use of this method of molding is dependent on a number of factors such as accuracy of pattern alignment, rigidity and alignment of molding machines, proper gating and sprueing, mold hardness, pouring methods, and the proper permeability and temper of the sand used.

While it is difficult to present in this paper all the detailed problems and precautions necessary to have a successful production operation, we will try to cover all the basic equipment and methods that we are employing.

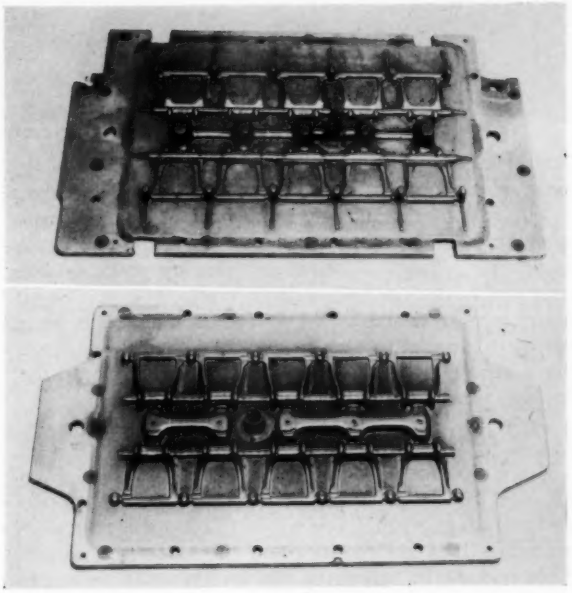
Selecting Molding Machines

The selection of the right type of molding machine to be used in multiple molding is of great importance in the making of good castings. The diameter of the squeeze cylinder must be large enough to give good mold hardness and the jolt solid enough to keep the number of jolts at a minimum. Proper squeezing and packing is necessary for an accurately formed pattern impression.

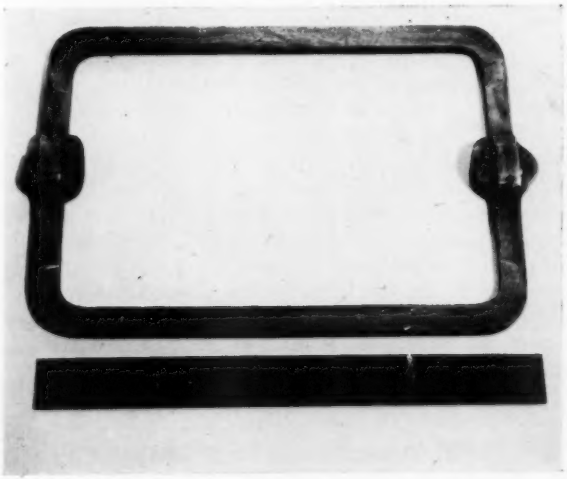
To maintain proper pattern alignment the table of the machine has to be equipped with hardened steel plate guides. The drag pattern is mounted on the platen or top plate of the machine. Upon squeezing, hardened ground guides assure the proper entrance of the drag pattern mounted upon the platen of the machine into the sand and assures a very close tolerance so proper alignment during the squeeze and draw is maintained. The stripping of the flask and sand from the pattern is done by means of four stripping pins contacting the flask flange. Adjustable pins accommodate the change in flask sizes.

Basically, any machine that is manufactured to proper tolerances and provides the necessary hardened wearing parts to prevent "wobble" can be used but care must be used in the selection of a machine so that the type requiring the least number of manual motions is chosen.

The problem of making pattern equipment for multiple molding is no different than making cope and drag plates. Aluminum pressure plates are used in our multiple molding. In planning of multiple molding equipment, the pattern shop's principal concern is to see that the pattern is dimensionally accurate and sound. Close attention has to be given to gating,



Cope (upper) and drag (lower) plates showing backfeeders and bleeders. Notches (upper) are for lift pins.



Strike-off plate and bar. Thickness and shape of plate, determined by experiment, provide extra sand for drag.



Dry-sand pouring cup keeps metal clean and controls metal flow to help give constant ferrostatic head.



Stop-off core, shown being placed in bottom mold section, plugs sprue hole which is automatically produced in each section by mating sprue cutters mounted on cope and drag plates.

parting, and draft to insure that a full supply of molten metal will properly reach all parts of the mold cavity.

The first step in making pattern equipment consists in making master plates for the table and platen of the molding machines. The table plates are made of cast iron and have dimensions of 28 x 18 x 2½ in. These plates have to be machined so that the surfaces of each plate are parallel to each other. The platen plates are made of steel and are 27 x 17 x 1¼ in. Here again the surfaces must be parallel to each other.

Pattern Equipment Must be Accurate

After the cope and drag patterns have been checked for dimensional tolerances the patterns are machined and rechecked so that when they're placed on the master plates they are parallel and in perfect alignment. This is done by means of jigs and fixtures to insure absolute accuracy. By means of socket head machine screws the plates are now mounted to the table and platen of the molding machine. The mounting is done so that the pattern plates are interchangeable.

The changing of pattern plates from one machine to another is only a matter of minutes, providing the proper care has been exercised in designing a master plate that will accommodate all pattern plates. To make this successful, it is necessary to adopt a universal size that will accommodate all pattern plates, and to do a certain flask size must be selected as standard.

A two-piece sprue cutter is fixed permanently on the pattern. The male part is mounted on the cope pattern. The female part is mounted on the drag. The operation is similar to that of a piston and cylinder action. Accuracy in placing the sprue cutter is required as the common sprue for a stack must be of such nature as to prevent washing or splashing.

In multiple molding selecting the proper type of flask and size is very important. We use two sizes of flasks on our system—13 x 19 x 3 in., and 14 x 20 x 4 in.—and design all pattern equipment to suit so we do not have to change flasks every time we change patterns. These flasks are constructed of special rolled steel channel having a solid center reinforcing rib and full width bearing sand retaining flanges. The copper content is sufficient to make the channel rust resistant.

Bushings are made of steel and cyanide hardened against wear. A round bushing is installed in the lower pin lug at one end of the flask section while an elongated bushing is installed in the lower pin lug at other end of each section. The elongated bushing is lined up exactly parallel with the pin center line due to the use of a flat pin on the lower machine platen.

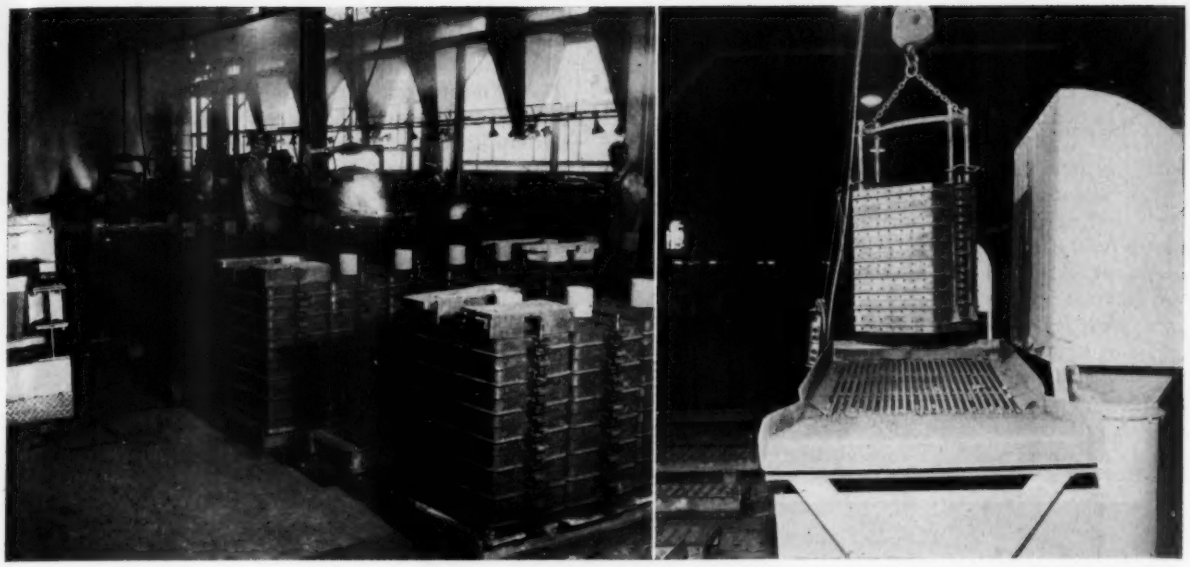
Pins extending 1⅞ in. above parting are used on the 3-in. deep sections, while pins on the 4-in. sections extend 2⅜ in. above parting. Pins are made from standard cold rolled stock steel and are not hardened. Round pins are installed in both of the upper pin lugs of each section and are fastened into the pin lugs by a drive fit. To prevent shifts and yet to give the molder enough play to allow closing the sections without binding a clearance of 0.0065 to 0.007 in. is maintained.

Since metal generates steam and gas when brought into contact with mold and core walls, we vent our flasks with 44 holes, 7/16-in. in diameter in each section.

To keep the quality of castings uniform and the foundry loss at a minimum the accuracy of the flasks has to be checked constantly. Jigs and flask straightening equipment insure good flask equipment. Constant vigilance must be maintained so that this part of the operation is not neglected with the resultant loss in production and increased scrap ratio.

Making the Mold

In making a mold, the flask is placed on the pattern mounted on the table of the machine. The strike-off plate is placed over the flask. The strike-off has holes drilled to fit the pins of the flask to hold it in place. The size and contour of the strike-off and its thickness are determined by experiment and will vary from job to job. Due to variation in parting lines and high and low spots created by various casting designs the amount of sand has to vary to give a good pattern impression and proper mold hardness and density in



Stacks are poured from 300-lb covered ladles suspended from tramrails. Same tramrail is used for shakeout.

the drag or top part of the flask. This is accomplished by means of strike-offs.

The sand is placed in the flask and strike-off frame from an overhead hopper. One or two jolts are used to ram the cope. The excess sand is struck off and the strike-off frame is removed from the flask. The thickness of the strike-off is very important as it determines the amount of sand above the flask line. The head of the machine is now brought forward. Both hands of the molder are now occupied, one holding the squeeze valve, the other the vibrator valve. The table now moves upward to squeeze the drag. If the right amount of sand has been left by the strike-off frame the pattern impression of the drag will be true to pattern dimensions and will provide a sand to sand contact when stacking, preventing fins and runouts. We now have the cope and drag impression made in one flask with the cope cavity on one side and the drag on the other.

Complete Mold Section

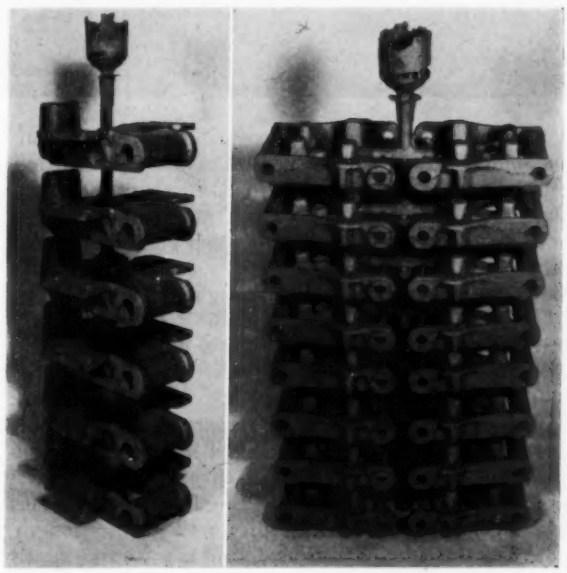
As the table of the machine is lowered after the squeeze, the flask mold rests on the flask strip pins. As the machine head goes back, the mold operation is complete with the exception of the core setting. The complete mold is now placed on an asbestos bottom board. A stop-off core is placed in the sprue hole of the bottom mold to prevent the iron going through the bottom board.

When the flasks are stacked the drag side of each combines with the cope side of the adjoining flask to form a complete mold. This continues until the stack is complete. We stack 7 and 9 high. This gives 6 and 8 good molds since the impression in the upper face of the top mold and the impression in the lower face of the bottom mold are not mated with another cope and drag and therefore are not usable.

A common sprue through the stack supplies iron to each mold runners and casting cavities. To insure uniform flow of hot metal, a dry sand pouring cup is employed. The cup is designed so that a constant head is maintained during pouring and a uniform flow of metal into each mold is insured, with slag or dirt collecting in the pouring cup.

Pouring is done by means of overhead cranes with 300-lb ladles. Iron is brought into the pouring area by means of a tramrail system and transferred into the pouring area. The amount of iron poured into a stack varies from 90 to 175 lb. The time required to pour a stack varies from 20 to 30 seconds. In our type of work the cooling time varies anywhere from 5 minutes to 25 minutes, and this fact must be considered in coordinating the different operations.

After pouring and cooling the molds are ready for shakeout. The same cranes used for pouring are now utilized in shakeout. A special frame is used for holding the flasks while transferring the complete stack of



Castings at left weigh 49 lb, sprue and gates 45 lb. Casting weight at right is 59 lb, sprue weight 46 lb.

molds to the shakeout. One shakeout for every three molders was found to be necessary. After the castings are removed from the flasks, the crane hoists the frame and the flasks and places them on the return rolls for return to the molder. The castings fall into a tote box and are hauled away by an electric truck to an area for sprueing at any convenient time.

The use of tote boxes rather than a casting conveyor for handling the castings was dictated by the need of grouping the different types of castings that are almost identical. Also, the use of alloy irons makes sorting too difficult and expensive if they are allowed to become mixed in handling to the cleaning room.

To make molding, pouring, shakeout, and flask return as efficient as possible many methods of mechanization were explored and studied. For molding and flask handling, we found roller conveyors to be most practical, since we have intermittent pouring, and require storage for stacks between heats. Gravity rollers offered the necessary flexibility and in addition allowed enough storage time to take care of the variations in cooling time.

Each molder is supplied with two gravity lines for molds, each 42 feet long, and one return line for empties. The two lines provide holding space for 50 stacks. The average production per man is from 40 to 70 stacks per day, depending on the number of cores in a mold.

To provide the necessary sand to insure a constant rate of production and to take advantage of all possible cost reduction, we installed a sand handling and conditioning system. This insures that uniform sand is delivered when and where desired with a constant check on moisture, permeability, and green strength.

While properly conditioned sand is a "must" in any molding operation, it is doubly important in stack molding. This type of molding requires a sand having good flowability for molding while retaining the proper green strength to prevent washing in the sprue due to the long primary drop of the hot metal when filling the lower molds in the stack. Proper cooling is also a "must" as the ratio of sand to castings is very small with the resultant excessive heating of sand. To accomplish the above, we selected the muller method as best suited to meet our needs.

It is also very important that a sufficient volume of sand is provided to take care of the fluctuations in the amount of sand in use during the "setting out" period between heats. The volume of sand should be large enough so that the "use cycle" does not exceed two times per day to prevent rapid deterioration of the sand and also to allow sufficient time in storage for the sand to equalize in temperature to insure closer control during conditioning.

It then became necessary to design a sand handling system to meet all these basic conditions. The sand falling through the shakeout is carried by a steel pan conveyor to a magnetic belt conveyor where all the tramp iron is removed. The sand is then elevated by means of a bucket conveyor to a hexagonal revolving screen which breaks up sand lumps and removes foreign non-ferrous material. We reclaim about 95 per cent of our core sand and in this way keep the volume of sand constant, the core sand additions making up for the gangway losses. To maintain the grain size in the backing sand, we use the same basic grain size in our cores as we do in our molding sand.

The screened sand falls into a 100-ton bin where it is held about 3 hours giving it a good chance to equalize in temperature. This bin is equipped with thermometers at the discharge openings so that the muller operator knows at all times what temperature he has to cope with. The sand is fed to the muller in regulated batches where it is cooled and the necessary binders, water, and sea coal are added. Amounts are determined by the laboratory sand technician.



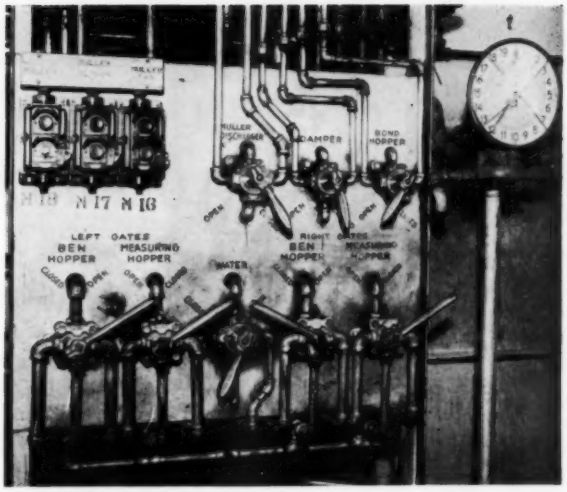
Nine-man stack molding unit in operation. Sand conditioning unit and operator are at far left. Each molder is served by three roll conveyors, two for stacks, one for flask return. Sand system is "closed" with unused sand coming to end of belt at right being returned for redistribution. Grates in floor under each molding machine eliminate shovelling from floor, permit spilled sand to return to system for subsequent use.

The sand is discharged from the muller onto a belt feeder and then elevated by bucket elevator to an aerator set above the distributing belt where it is given the necessary fluffing and aerating. This breaks up any packing or caking so that a light flowable sand is distributed to the molders. The sand is distributed by means of a belt conveyor, with counterweighted V-type plows at each molder's station plowing the sand into the hoppers. The system is a closed circuit type so that any surplus not utilized by the molders is returned by means of an overflow belt to the shakeout conveyor.

Make a Better Place to Work

The hoppers have a capacity of 3½ tons each so that the molding operation will not be interrupted should there be any delay in the conditioning operation. The sand is fed into the flasks by means of manually operated gates rather than power operated as we feit that the amount of sand could be regulated to a closer degree. We have also installed a spill sand belt below the molding machines to remove all strike-off and spill sand so the molders do not have to shovel this sand. Eliminating this operation increases molding production approximately 10 per cent.

Concentrating the pouring operations in a relatively small area creates a smoke and gas condition which must be remedied. Centralized shakeout also creates concentrated gas and dust areas. Betterment of working conditions require the elimination of this smoke and gas. In addition we have a dust problem, fume



Control board for sand conditioning system insures efficient, close control of molding sand properties.

removal, and cooling of sand, all of which require the use of air.

It is possible to collect the air from these different operations and vent to the outside except that in our case we are located in a densely populated area where this is impossible. So we installed a dust collector unit. Either a wet or dry collector can be used but ample capacity must be provided.

CONSTRUCTION AND OPERATION OF COAL FIRED AIR FURNACES

By

Cecil F. Semrau*

■ The author's plant operates three periodic, pulverized coal-fired air furnaces. Two are used for jobbing production and one for fitting production. All furnaces were originally of solid bottom construction. The fitting furnace is still so constructed although we have added improvements like water-cooled plates around the skim door opening and have replaced solid type tie rods with water-cooled tie rods. This latter change has definitely overcome the problem of spreading and distortion of furnace structure. The construction and arrangement of the fitting furnace is shown in Fig. 1, which also illustrates the water-cooled tie rod feature in detail.

* Formerly metallurgist, Illinois Malleable Iron Co., Chicago, now, Hill & Griffith Co., Chicago.

Several years ago we adopted the practice of using brick bottoms in all furnaces. This proved advantageous in many ways. To improve bottom practice we rebuilt the two furnaces used for jobbing production to incorporate air cooling of bottoms. The construction of these furnaces is illustrated in Fig. 2. Notice that this construction provided for the use of brick sub-bottoms.

To further enhance the air-cooled feature, the space below the bottom plate of these furnaces was enclosed on each side to permit the drawing of cold air from front to back to a vent pipe at the stack end of the furnace. The extent of this induced air cooling has not been determined quantitatively since we had not measured the volume of air that flows under the bottom. The exhaust air temperature averages 120 F. We plan to pursue this matter of air cooling further.



"A 6-in flame out of pourback . . . is about right."

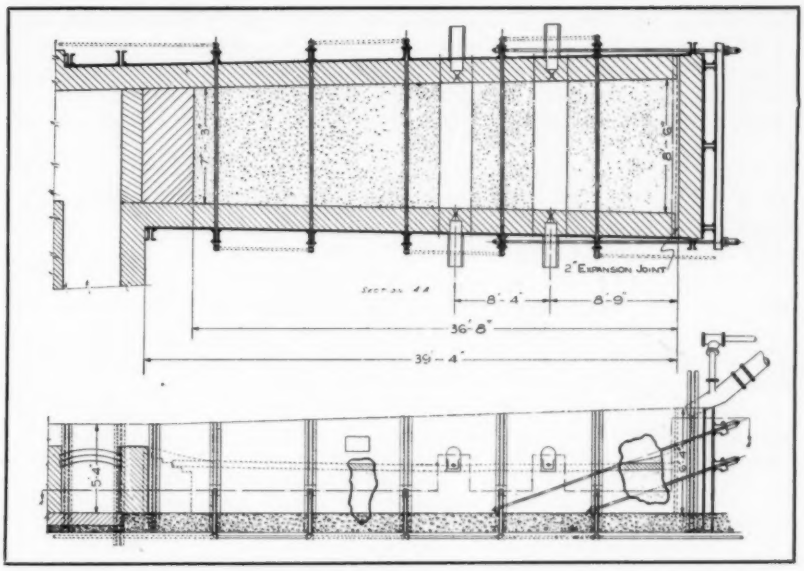


Figure 1

Each furnace is equipped to be operated in conjunction with a Wickes waste heat 400 hp boiler with 3625 sq ft of heating area. Boiler, boiler entry arrangement, and stack by-pass enclosure are illustrated in Fig. 2. Thus we have furnaces on two distinct types of operation, but as every operator well knows even the two jobbing furnaces do not operate alike and present their own idiosyncrasies.

Good Brick Required

The author found that good brick masonry is an essential prerequisite to satisfactory refractory performance and refractory life. Certain practices and procedures have been adopted and we adhere to these rigorously. In describing furnaces and refractories in greater detail we will discuss each enclosure element individually.

We use Missouri 105-13½-in. bungs layed in double row frames as indicated in Fig. 3. The frames are of our own design and cast from gray iron in our own shops. The clamps are similarly cast in our shops.

Joints are broken every third course with a full bung brick and half bungs. We tried using 6¾-in. or half-size bung brick, but found it more economical to obtain half brick by breaking the regular bung brick in two. This may make for a rough break but we can avoid thickness variations that it is impossible to avoid with two different brick. All bungs are layed with fire clay slurry to cushion the brick and eliminate any dimensional non-uniformity.

Frames, clamps and bolts must be kept in good repair. Burned or distorted clamps must be replaced promptly. Frames must be tightened uniformly to hold brick securely. At the end of each frame we use stiff

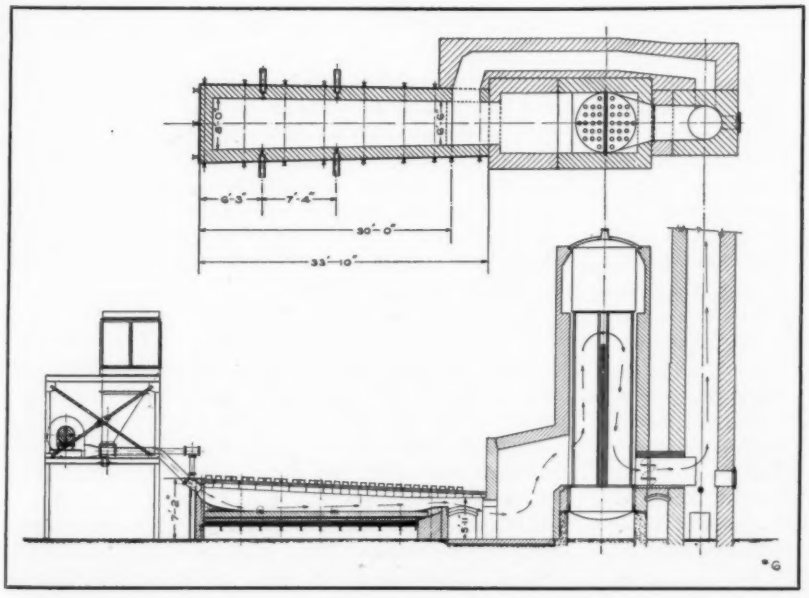


Figure 2

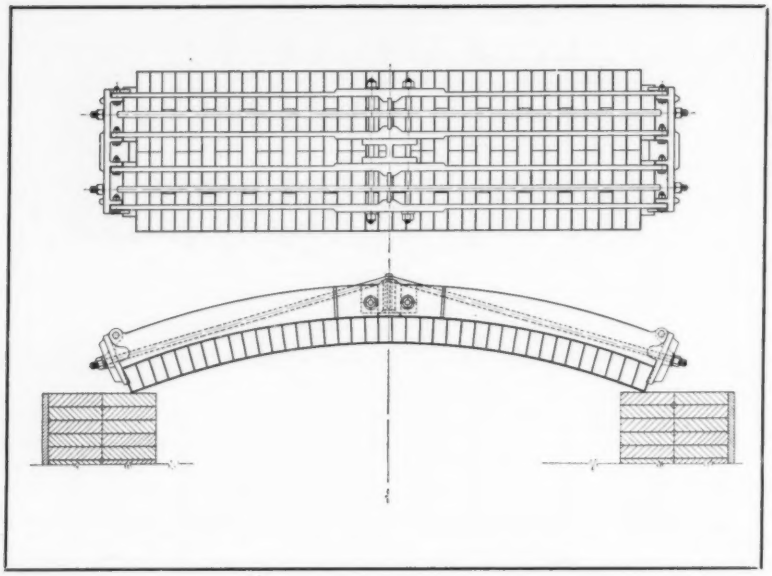


Figure 3

mud No. 2 arches as a bearing surface for the clamps.

Sidewall brick must be layed up with care, particularly below the metal line and around tapouts. Careless masonry is the cause of most run outs. Original installations are always "layed to line" to assure straight walls and level support. Savings in labor, furnace repairs and maintenance, amply justify this added care.

Careful and tight masonry, however, make even more essential the provision for adequate expansion joints to compensate for the reversible thermal expansion of the refractories. Failure to provide for such stresses results in furnace distortion, burner misalignment, and extra maintenance. Suitable expansion joints are provided at sidewall juncture with both burner wall and bridge wall.

Proper Openings Reduce Repairs

Furnace openings frequently are the focus of excessive erosion. Openings designed to proper size, carefully constructed, and protected during melt down can materially reduce sidewall repairs. All jambs should be carefully layed using large 9-in. series for breaking joints. Soaps and series smaller than a regular 9-in. brick should be avoided. Arches over openings are subjected to much abuse. We have found sprung arches better suited than square edge lintle tile. Installation of arches must be done with the proper arch shapes to give correctly aligned radial joints. Skewbacks must be layed with standard shapes to provide a skew face that gives the arch rigid support.

We reduce flame erosion at furnace openings by sealing all openings flush with the inside surface of the sidewalls during melt down. This protects jambs and arches not only against erosion but the excessive thermal differentials that induce spalling.

The burner wall burner port openings must be layed up to dimension not only to provide for structural stability but to provide for proper functioning of burners and directional flow of the flame.

The importance of brick masonry as it affects operation and refractory life was never so forcibly demonstrated as when we started using brick bottoms. In most instances reduced bottom life can be attributed directly to faulty or careless masonry.

Many methods of installation are used. Since our furnaces are tapped from two tap holes on each side we found that convex bottoms were best suited. The details of construction are illustrated in Fig. 4. In the fitting furnace, the brick of the service bottom are layed on sand. In the jobbing furnaces, bottoms consist of a shallow sand fill to support a sub-bottom of two rowlock courses of fire brick. This is covered with a 1½-in. cushion of sand on which the 9-in. service bottom is installed.

These two constructions definitely indicate the desirability of the brick sub-bottom. Where a sub-bottom is not used, constant care must be exercised to assure support. Crude silica sand will pack better and tends to flow away less than washed sand. Each installation must be layed to line and carefully supervised to assure that the bottom has proper contour. The brick sub-bottom, however, is layed to the contour required. The proper contour thus established makes it much easier to replace the 9-in. service bottom with reproducible contour and even less supervision. The 1½-in. of cushion sand makes it easy to remove and replace the service bottom as well as affording additional protection for the sub-bottom.

Rise Is 1½-in.

The spring or rise at the groin section between tapouts is 1½-in. The remainder of the bottom was formerly layed to a similar contour but we have found that it is even simpler to lay bottoms that are flat between sidewalls. The slope in each direction from tapouts is approximately 3 in.

Originally we layed bottoms so that the bottom equidistant between front and back tap blocks was 1½ in. higher than the drain courses at the tapouts.

This assured positive drainage. It was observed, however, that this high spot was such that some metal remained in the back tapout area after the front taps had drained. This resulted in greater slag attack on the front end of the bottom. To minimize this, we now construct the high section closer to the back taps. The furnace drains more uniformly and slag is lessened. Our furnace construction is further illustrated by Fig. 5 and 6.

Special Design Tap-out Used

We use a tap-out of special design (Fig. 4) which is suited to our operations. We feel that the cylindrical section or hole being longer than that of the standard tap-out will withstand more abuse. The bell at each end makes for greater accessibility. We use holes of different size depending upon the size heat, and use both steel bars and cores for plugging. The steel bar is used more often, particularly when the hole erodes.

So far, stress has been on refractory constructions and masonry as a means of keeping refractory consumption in line. Of equal or greater importance is the operation of a furnace. The best refractories installed with the greatest care can be destroyed by careless operational practices. Careful operation pays many rewards. Carefully worked out and controlled operation make it easier on the men, assures better metallurgical control and reduces labor and material costs.

Carefully and orderly charging pay dividends. It assures easy melt down without crowding the burners. It prevents excessive oxidation and minimizes attack and slag erosion of refractories—both sidewalls and bungs.

At Illinois Malleable the average charge runs: 40 per cent pig iron, 50 per cent sprue and hard iron scrap, 2 per cent steel scrap, and 8 per cent malleable scrap. One of the most important controls we have in bringing about an easy and uniform melt down is to control the method and order of charging.

The order of charging employed is indicated in Fig. 7. We first spread the uncleaned hard iron sprue uniformly from front to back starting some 5 ft from the burner wall and extending back toward the bridge wall carrying no nearer the bridge than the size of the heat requires. We attempt to crown this portion of the charge to avoid contact with the side wall.

Then come the hard iron chunks or over iron and the malleable scrap placed as indicated at the front end and back end, respectively, still crowning to keep these portions from piling against the side walls. This placement appears logical in view of the relative resistance to melting of these two materials. The charging procedure up to this point leaves a sizeable space between charge and side walls which we now fill with the clean hard iron scrap. This is followed by the steel charged near the front of the furnace where it is subjected to the direct action of the flame. Malleable scrap is charged toward rear of furnace and the entire charge covered with pig iron.

Why Charge That Way?

Following is the reasoning that led us to adopt this charging procedure. The pig iron, hard iron and malleable scrap melt faster than the sand covered sprue and the flow of molten metal is naturally directed to the back of the furnace to position for easy skimming. The steel and hard iron chunks being in position to be subjected to direct action of the flame are in the most favorable position for rapid melting. During the week when the furnace is hot the hard iron chunks can be charged before the sprue, and melted with no more difficulty.

The loose clean hard iron scrap filled against the side wall protects the side walls from excessive erosion yet provides the desired openness to the charge that prevents complete short circuiting between the top of charge and bungs before melt down. Likewise, the hard iron fillet melts in sequence with the steel and hard iron and pig iron to permit metal and slag flow along the side wall back to skimming position.

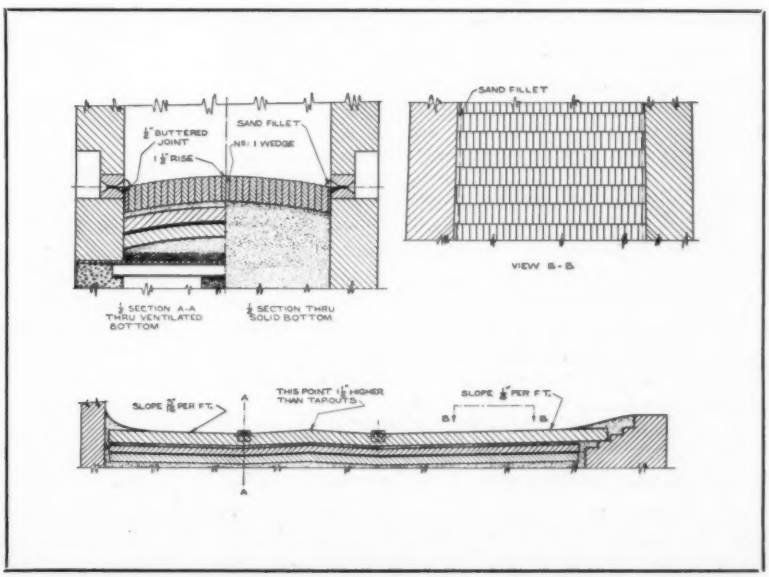


Figure 4

Excessive crowning of a charge should always be avoided to protect side walls, yet the charge against side wall should be such that the melting mass will not be dammed up and confined to the front end of the furnace. Neglect of this important consideration will invariably result in excessive side wall erosion.

Detrimental Method of Melting

We have found that the most detrimental method of melting is to permit faster melting materials (pig iron, malleable scrap, and clean returns) to be charged near the burner end of the furnace. This results in these materials being dammed by semi-molten materials resulting in super-heating, excessive oxidation, and severe erosion of side walls before adequate skimming can be effected. Correct charging and melting

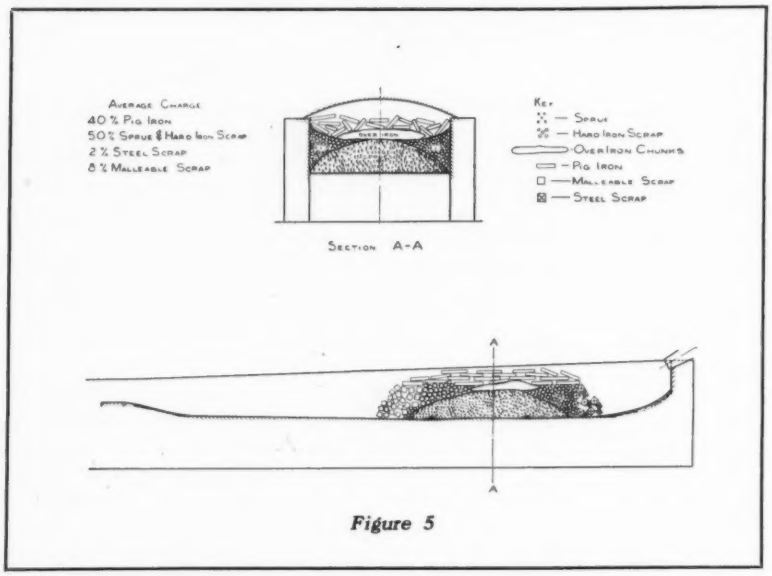


Figure 5

avoids this but any tendency toward this condition should be corrected early in the heat by breaking up the semi-molten restraining portion with a steel bar.

A charge should never be so crowned as to unduly restrict flame travel. It is much more sensible to raise the side walls when necessary to avoid such restriction.

Proper Agitation an Important Factor

One of the most important factors in rapid and efficient melt down is the provision for proper agitation. We induce agitation by the use of green tamarack boughs. On a new brick bottom, the metal circulates readily. Circulation is induced by the flame travel and the pushing which we do with bars. As the bottom becomes eroded the circulation is somewhat retarded and the extra agitation required is easily accomplished with the green boughs.

As the charge melts down slag must be removed as quickly and as frequently as possible. This permits more rapid heat transfer from flame and radiation to metal. Attempting to melt or superheat slag-covered metal excessively erodes side walls. Operators should be instructed to remove slag as formed and not be guided inflexibly by any time schedule of operation.

Coal prepared in a central pulverized system to furnish fuel for power house, annealing and melting requirements creates problems. The fact that higher carbon iron is melted in one of the three furnaces further complicates fuel preparation. These varying requirements create problems not experienced by the unit pulverizer operator. We must, therefore, adopt a compromise of control in our fuel preparation.

Combustion Control

We control fineness to give the proper combustion rate in our air furnaces. With the coal being used, approximately 94 per cent through 100 mesh and 84 per cent through 200 mesh is the best compromise.

Coarse coal allows the carbon to go up. This we try to control by steel additions to the initial charge or added to the heat during superheating. This is difficult for it is hard to dilute the carbon as fast as it is deposited and absorbed.

Fine coal on the other hand is equally troublesome since it not only oxidizes the metal, but the oxide slag resulting excessively erodes side walls. Operators are also aware that highly oxidized heats, regardless of final analysis by furnace additions, lose their life. Such heats are difficult to pour satisfactorily and often cause a higher percentage of misruns.

Control of preparation depends upon the type and source of coal used. We have standardized on a West Virginia coal of the following average analysis (dry basis):

Ash	3.75	per cent
Volatile	36.50	" "
Fixed Carbon	59	" "
Sulphur	0.85	" "
BTU		14,750

This coal is somewhat denser and does not pulverize as freely as the lower volatile coals generally used.

One essential in successfully using this type of fuel is to carefully control the moisture. We have found that moisture should be under 0.5 per cent to assure most satisfactory handling through the screws and uniform flow through burners. Damp coal retards combustion rate and slows up melting of the charge directly in front of the burners. This often results in irregular burn out patterns—more severe erosion in the rear end of furnace walls.

The most successful flame in our practice is one which gives adequate turbulence to effect complete mixing of fuel and air. This requires careful control of flame velocity (as well as proper charging) to produce the desired turbulence and mixing. Excessive flame velocities cause more rapid erosion of side walls and bungs. Flame velocity also affects the rate of heat

transfer and radiation to metal during a heat run.

The twin burners in each furnace are identical. Details of each burner are illustrated in Fig. 8. It is most essential that burners be properly aligned to assure smooth melt down and minimize refractory erosion. We have adopted a very simple practice for determining flame travel. We attach two 10-ft streamers of 1-in. gauze bandage centered inside the burner castings in such a way that the loose ends will be caught in the blast and blown forward in the furnace. This indicates the path of air fuel travel. We adjust our dual burners so that the two ends of the gauze meet about 8 feet ahead of the burners and adjust so that they meet at the exact center of the furnace. Air flow can be adjusted by the two dampers in each burner.

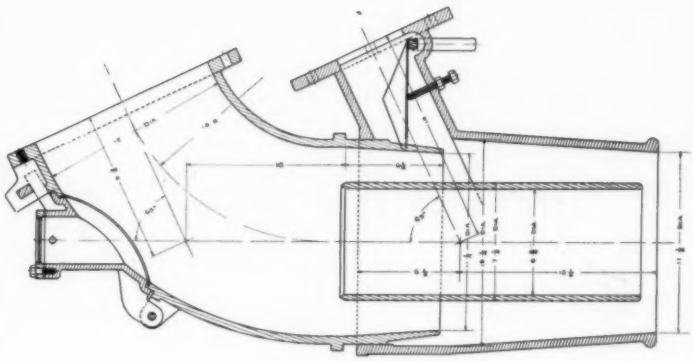


Figure 6

Sufficient checking indicates that this practice on an empty furnace is indicative of proper flame alignment when charging is carried out according to the specifications already described.

No discussion of charging and firing is complete without emphasis on draft control. Rigid control of back pressure is, in our opinion, the most useful factor in melting. Perhaps the most important factor in controlling draft is the provision for proper opening over the rear bridge wall. Adjustment of stack draft is necessary but in itself is inadequate without the proper bridge wall opening.

Our practice is based on observation of the nature of the flame over the bridge wall and the back pressure as indicated by the flame out of furnace openings. We find that a 6-in. flame out of the pour back opening is about right in our practice.

Metallurgical Control

With the careful construction and operational practices described, we find little trouble in melting to satisfactory analysis, and obtaining iron of good flowability that will yield good sound castings that are easily annealed.

Our operational practice makes melt down easy and well scheduled. Our control is accomplished by test ladle observations, close observation of test sprues, and preliminary analysis. Test ladle observations are made at frequent intervals from melt down to tapping time. Three sprues are taken; one at melt down, one when preliminary sample is poured (one hour before tap time), and the third about 15 to 20 minutes before tap time.

Correlating these three methods of metallurgical control gives a good idea of the nature of the heat we are pouring. We feel that careful control of operation facilities assures close metallurgical control without the use of excessive furnace additions and adjustments as well as assuring against undue refractory failures. Failure to follow carefully planned operational control results in variable heats, excessive adjustment and excessive pouring temperatures, to say nothing of the increased labor and refractory costs.

The author wishes to express his thanks to G. B. Stantial, works manager, as well as the Illinois Malleable Iron Co. for permission to prepare this paper.

DISCUSSION

Chairman: W. R. JAESCHKE, The Whiting Corp., Harvey, Ill.
Co-Chairman: R. P. SCHAUSS, Werner G. Smith, Inc., Chicago.
Recorder: R. W. WITSCHEY, A. P. Green Fire Brick Co., Chicago.

J. W. WYRICK:[1] How often do you replace or repair subbottoms?

MR. SEMRAU: We have not replaced one in ten years.

MR. WYRICK: Have you tried a concave bottom?

MR. SEMRAU: No, we have not.

MR. WYRICK: Do you use a sand fillet at the burner wall and bridge wall?

MR. SEMRAU: Yes, we did.

MR. WYRICK: Why is the broken construction used in laying up your bungs rather than using three rows of 9-in. bungs?

MR. SEMRAU: We have never used the construction you mention because our frames tend to crack down the center, so we cannot give a comparison.

GEORGE IDLER:[2] How do you construct the bottoms at the tap-out area?

MR. SEMRAU: We lay a convex contour which drains toward the tap-outs on each side. The convex bottom is easier to install when tapping from each side of the furnace.

MR. IDLER: Does the flame hit the crown of the bottom?

MR. SEMRAU: Yes, it does.

MR. IDLER: Is that not a disadvantage?

MR. SEMRAU: Yes, but our method of moving the high spot back as we have described, corrects it.

[1] Terre Haute Malleable & Mfg. Co., Terre Haute, Ind.
[2] International Harvester Co., Chicago.

CARBON AS A REFRACTORY

By

T. J. Wilde, M. R. Hatfield, and V. J. Nolan*

In some cases carbon and graphite are the only materials which have been successful in meeting the severe and exacting requirements involved in handling hot metals and corrosive slags. This is due in large part to the facts that:

1) They are not wet by most molten metals; thus metals do not stick to carbon and graphite.

2) They maintain their mechanical strength well past the temperatures attained in handling molten metals.

3) They do not suffer deformation at these temperatures.

4) Their resistance to thermal shock obviates spalling and the consequent inclusions.

5) Their negligible thermal expansion assures uniformity of casting dimensions.

6) Their low coefficient of friction reduces erosive wear.

7) Their weight facilitates handling.

The use made of these materials in the metallurgical field is exemplified by the following partial list of typical applications.

Linings for blast furnaces and cupolas
Iron and cinder notches for blast furnaces
Runout trough linings
Ladle linings
Ingot mold plugs, stool inserts and stools
Ingot and over-metal molds
Molds for static and centrifugal castings
Parts for powdered metallurgy pressure molds
Sintering boats
Gassifying tubes
Dies
Runout boards and tables
Core rods
Heat treating boxes
Brazing trays

Blast Furnace Linings

Based upon the background of European practice and American alloy furnace experience, carbon linings for blast furnaces have followed a rapid evolutionary pattern from tamped carbon paste crucibles to standard brick sizes of shaped carbon, and finally to block forms as large as 24 by 30 by 180 in. long and weighing up to 7,000 lb apiece.

Many other applications of carbon forms to blast furnace practice have been made in the past 3 years. These include skimmer plates, cinder notch linings, iron notches, runout trough linings and linings for tuyere cooler openings.

Molds, Plugs and Stool Inserts

A number of applications have been developed for molds, mold plugs and stool inserts of carbon and graphite. The molds are mainly of graphite and are used principally for the hot press formation of cemented carbides and for the sintering of diamond chips and powdered metal into drill bits and wheels. Molds for cemented carbides are made in sizes up to 16 in. O.D.; molds for diamond tools up to 12 in. O.D.

Stool inserts and other mold shapes have been developed for aluminum, magnesium, copper, brass and high-to-low carbon steels.

Carbon mold plugs offer advantages because they do not stick or spall and therefore eliminate ceramic inclusions. Extensive tests have demonstrated no proven evidence of carbon pick-up from the plugs. Their useful life varies from one to 50 pours depending upon plant practice and the size of the ingot.

Cupola Applications

Some uses of carbon as a refractory have been mentioned in order to give some idea of the broad application of this material.

Carbon, in several forms, has been applied successfully in the cupola well, and in the runout trough or skimmer.

Carbon ramming mix, which is an all-carbon but in a semi-plastic form after heating, was the first carbon material used as a lining material in the well of the cupola. A 6 in. thick lining of carbon ramming mix was formed inside of the ceramic lining and extended from the pan to the tuyeres. The carbon ramming mix was then applied to the trough outside the tap hole by building up a 3-in. thickness inside the refractory lining. The absence of slag attack on the carbon has made it possible for carbon to remain

*National Carbon Co., Div. of Union Carbide & Carbon Corp., New York.

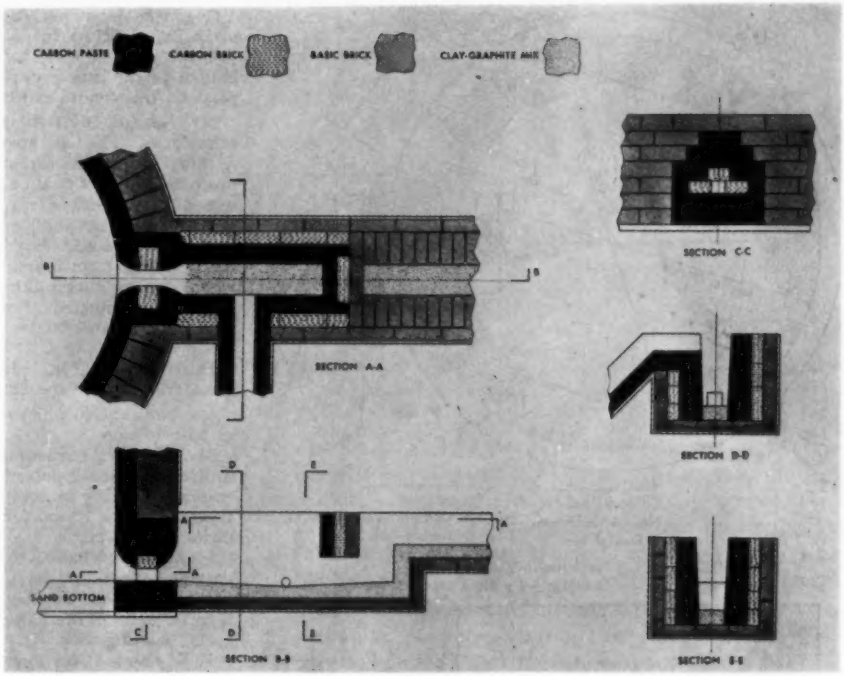


Fig. 1—Carbon ramming mix and carbon brick applications to cupola front-slagging spout.

in the cupola for long periods of time, with small repairs during shut-down periods.

Figure 1 shows one of the early applications of carbon ramming mix and carbon bricks.

One carbon lined trough was lined in July with 2½ in. of basic brick against each well of the trough shell, then 2½ in. of carbon brick, then about 4½ to 5 in. of carbon ramming paste.

A large carbon block was installed for the dam. The carbon block was covered with about 1 to 1½ in. of carbon paste. This remained until October, during which period the paste was allowed to wear away, and the carbon block was exposed to the metal and the slag up to the time it was rebuilt in December. The trough was relined using magnesite backing, carbon brick and 4½ in. of carbon ramming paste, but the original carbon dam block was used with the new lining. This was in operation for 4 months, and except for a shutdown the trough was used on alternate days and on a 5-day week continuously.

The carbon block was still intact and to the proper dimensions on the under surface which covers the iron passage. The face exposed to the molten metal and slag has worn back about ¾-in. The composite lining of the trough side walls will probably last 6 months, as reported previously with the former lining, and perhaps longer.

The working face of the carbon paste side walls will oxidize or wear away in spots and must be touched up every two or three heats. This touching up usually requires about a quart of patching materials. Before patching the surface is brushed off and painted with a light tar, then a patching mix is made of about half carbonaceous and half ramming mix. These are heated and mixed together and applied to the surface, which is already primed with tar. No other precautions are required except to paint the inner surface of the trough with a periclase slurry to protect the lining from the oxidizing effect of the heating torch.

In order to use carbon ramming mix properly arrangements should be made for a small box or tray sufficiently large to hold several hundred pounds of carbon ramming mix. The box or tray should be heated by steam coil or other forms of heating elements in order to prepare mix for use. The temperature required to heat the mix should not exceed 170 F. After heating the mix can be handled in small pails or other containers and applied with a ramming gun or tamper.

The variety of standard carbon brick shapes makes it possible to fabricate carbon linings from large carbon blocks. The large carbon blocks referred to are, roughly, 24 x 30 x 180 in. long; 15 x 30 x 110 in. long, and many other sizes.

Fabricated carbon linings are being used similar to those shown in Fig. 2. Carbon brick may be used against the steel shell to represent approximately one-third of the well lining thickness and then the large fabricated block lining can be made for the remaining two-thirds of the lining thickness. The large block inner lining is made to extend from the pan up under the tuyeres, with the minimum number of vertical joints in completing the circle.

Bricks or blocks of pre-baked carbon constitute a fairly easy lining to design and install. It has the additional advantage of being easy to repair with carbon ramming mix when repairs are necessary. Carbon ramming mix will adhere to the fabricated carbon lining as a patching material. When large volumes of carbon ramming mix are necessary as patches, the inside block or brick can easily be replaced.

These carbon materials have had wide use in metallurgical applications for many years and, properly

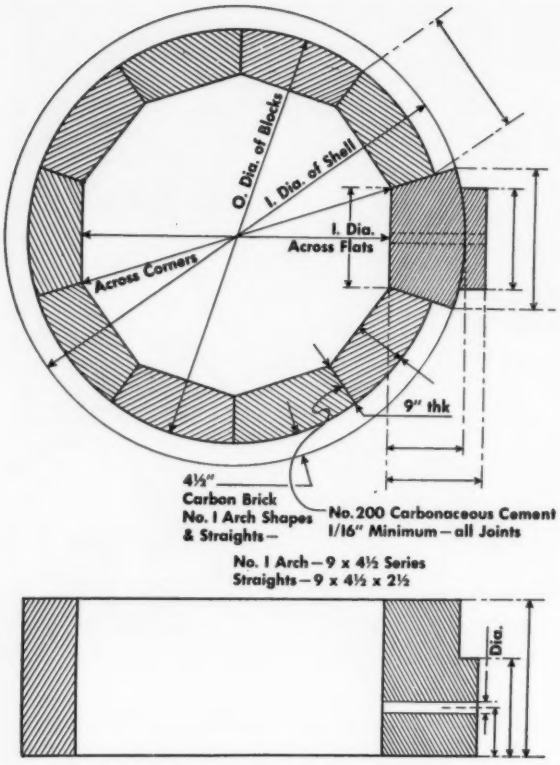


Fig. 2—Diagrammatic sketch of carbon brick cupola lining.

handled and applied in the cupola, are proving to be a superior refractory.

DISCUSSION

Chairman: W. R. JAESCHKE, The Whiting Corp., Harvey, Ill.
Co-Chairman: R. P. SCHAUSS, Werner G. Smith, Inc., Chicago.
Recorder: R. A. WITSCHEY, A. P. Green Fire Brick Co., Chicago.

W. R. KRAFT:[1] You have mentioned a lining that lasted 13 days when banked. Is banking necessary to prevent oxidation?

MR. WILDE: Banking reduces oxidation and reduces labor costs. Carbon apparently is not attacked during operation since it is neutral and resists acid and basic slags equally. We refer to a very complete article by Provias (R. Provias, "Carbon: A New Cupola Refractory", *Canadian Metals*, Nov. 1952). Where a cupola lined with carbon is not banked, adding a small charge of coke before the bottom is dropped and spraying with water to bring below a red heat definitely assists in protecting the carbon.

CHAIRMAN JAESCHKE: You spoke of cooling and quenching. Can you tell us about the possibilities of using water-cooled jackets in the melting zone to protect the carbon against oxidation.

MR. WILDE: Yes it does help. We can refer to our blast furnace experience to bear this out. In actual practice, we find that there is a small build up of a protective coating on the inside or service face of the carbon refractory.

CHAIRMAN JAESCHKE: Banking an acid-lined cupola over night makes for difficulty in starting the next day and tapping the first metal because of bridged material getting down to the bottom of the cupola during the banked period. Will you expand on this condition when using carbon linings?

MR. NOLAN: Metal or slag will not stick to or slag carbon refractories. The old answer was to protect acid refractories. A thin film protects carbon. It is also covered with charge and protected with a CO atmosphere. We hope to try a carbon bottom a little later.

We should also point out that water cooling is expensive. It costs $40,000 to $50,000 per year for water pumping on a blast furnace. We have found that water cooling is more effective where carbon is used although we did think that water cooling might be eliminated. Even though it was not, water cooling was found more effective with carbon than with fire clay refractories.

H. W. LOWNIE, JR.:[2] Is carbon used above tuyeres in those installations where the bottom is not dropped?

MR. NOLAN: No complete data are available yet but we hope to have it soon.

MR. LOWNIE: Carbon linings on cupolas have a wonderful future. We should point out that there is confusion due to the reasons for using in conjunction with water cooling above the tuyeres and the reason for using carbon instead of other refractories in the well.

MR. NOLAN: Basic refractories in the well lasted only two days and the reason for using carbon was to give better overall service. We were asked to do this work in the well section to increase service life, for longer runs obtainable with water-cooled melting zone.

F. E. KASCH:[3] We should point out that failure in the melting zone is the determining factor in making it necessary to drop bottom.

MR. NOLAN: But we were called in when the well was the trouble spot.

E. C. MATHIS:[4] Can you tell us something about the solubility of carbon in hypo-eutectic irons?

MR. NOLAN: Not definitely. We do not know at just what point carbon pickup occurs. We think at present, that 2.50 to 2.60 may be the low level. We were faced with carbon pickup in graphite molds for stainless steel or other low carbon metals. We found that solubility did not raise carbon much and that there was very little pickup. Rapid chill also prevents carbon pickup.

R. H. STONE:[5] Is there any means of protecting carbon from oxidation? I am thinking of a coating for a crucible in an induction furnace. Is there any way to do this?

MR. NOLAN: It is hard to find a material to permanently protect carbon against oxidation. In some experiments we found that periclase coating did not last long. We also had a carbon-lined trough at this furnace and tried water cooling with a hose to get additional life. We also tried a fire clay slurry to protect the lining.

Graphite crucibles used in induction melting are made with a replaceable 3-in. top ring.

One of our associate companies has developed a coating that seems to be retained even with constant reheating.

MR. STONE: Does the coating used on ingot molds continue to offer promise?

MR. NOLAN: We are selling that as a standard item. We are using graphite for special steels.

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[1] American Brake Shoe Co., Mahwah, N. J.
[2] Battelle Memorial Institute, Columbus, Ohio.
[3] Howard Foundry Co., Chicago.
[4] Pickands Mather & Co., Chicago.
[5] Vesuvius Crucible Co., Swissvale, Pittsburgh, Pa.

FUNDAMENTALS OF HEAT TREATMENT OF GRAY CAST IRON

By

J. E. LaBelle*

There are three principal heat treatments which can be applied to cast iron: It can be softened, hardened, or relieved of stresses. In each of these treatments there are a large number of possible ramifications; however, the whole thing boils down to a few easily understood fundamentals, and our purpose is to make you acquainted with these fundamentals. But first, let us consider what cast iron really is.

Cast iron when examined under a microscope is not a hodge podge of silicon, carbon, iron, phosphorus, manganese, sulfur and everything else as many people imagine. It is primarily iron, with flakes of graphite and small particles of a material called iron carbide in it. The properties of the iron are determined by the size, shape, quantity, and distribution of the graphite and the iron carbide. The other elements in cast iron such as silicon, manganese, etc. are for the most part dissolved in the iron or the iron carbide. Their effect on the properties of the iron is indirect. To some extent they control the form and amount of iron carbide and graphite, which in turn influences the properties of the iron.

Now iron carbide, or just plain carbide and graphite, are closely related in cast iron. Both of them are formed from the carbon in the iron. Thus, given an iron with a certain amount of carbon as poured, part of the carbon forms graphite during solidification and the balance ultimately separates from the iron to form iron carbide.

Graphite Formation Characteristics

In gray iron, the graphite which forms assumes its final size, shape, and distribution at or near the solidification range. Thereafter it is not possible to alter the graphite except in quantity, short of re-melting. Normal carbide particles, on the other hand, do not usually separate from the iron until it has cooled to about 1350 F. It is possible, therefore, to do quite a bit of manipulation with the carbides by merely heating the iron to a temperature where they will redissolve in the iron and then causing them to reseparate on cooling in a predetermined manner. The dissolving and re-separating of carbides, as well as other things, takes place when the iron is entirely solid. Sometimes it is a little difficult to grasp the idea of dissolving something in a solid piece just like sugar in a cup of coffee, but that is exactly what happens!

Both graphite and carbide are formed from the carbon in the iron, and in this sense they are quite closely related. On the other hand, they are at opposite ends of the pole with respect to properties. The greater the amount of graphite—the softer will be the iron, and the greater the amount of carbide—the harder. As a matter of fact, in dead soft iron practically all of the carbon is in the form of graphite; and in chilled iron most of it is in the form of carbide.

There are actually two kinds of carbide particles in iron. The regular type are quite small and are formed from the carbon which remains dissolved in the iron after the graphite has separated in a normal manner. The particles separate from the iron at about 1350 F as previously mentioned and can be readily re-dissolved as we will see later. This type of carbide is the type present in the higher strength irons.

The other type of carbide particles forms at higher temperatures during solidification when the graphite fails to separate in a normal manner, and are large in size. These are the typical chill or massive carbides and they are not as easily re-dissolved as the finer normal carbide particles.

That pretty well covers what cast iron really is. It is iron containing graphite, and carbide particles; and the carbide particles can be either normal, massive, or both.

What can we do with the iron by heating to temperatures less than the melting temperature? Let us select four temperatures and three different types of iron to work on:

It is obvious from Table 1 that the following statements are true:

1. It is possible to change carbide to graphite.
2. It is possible to re-dissolve graphite in the iron.

* Chief Metallurgist, Detroit Diesel Engine Division, GMC, Detroit.

TABLE 1

Type of Iron	Changes at Temperatures Indicated 1100 F	1400 F	1550 F	1700 F
1. Dead soft iron (no carbides).	None	None	Graphite dissolving in iron.	More graphite dissolved.
2. Iron with normal carbides.	Very Little	Normal carbide breaking down into graphite.	Graphite dissolving in iron. All normal carbide gone.	More graphite dissolved.
3. Iron with both normal and massive carbides.	Very Little	Same as No. 2 but massive carbides unaffected.	Same as No. 2 but massive carbides breaking down into graphite.	Same as No. 2 but massive carbides breaking down into graphite at a faster rate.

3. It is necessary to get up to about 1700 F to break up chills.

4. It is possible to soften normal iron by heating to 1400 F, thus changing the carbide to graphite.

5. Iron can be heated to 1100 F without changing it to any appreciable degree.

6. Normal carbide will be either dissolved or broken down into graphite which is re-dissolved at 1550 F, and on cooling the dissolved carbon will separate as either graphite or carbide, depending on how the cooling is done.

Now let us consider cooling of the iron. We will consider only the dissolved carbon in the iron which is present in the ranges of about 1550 F or higher. Dissolved carbon just will not stay in the iron on cooling. Since it can only separate as carbide or graphite, what happens is not difficult to visualize.

Slow Cooling Effect on Graphite Formation

If the iron is cooled slowly enough to 1300 F, it passes through the range where carbide changes to graphite, and any dissolved carbon in the iron will separate as carbide and then revert to graphite. Thus, if the iron is first cooled slowly to 1300 F, the dissolved carbon will wind up as graphite and—regardless of how rapidly it is cooled thereafter—it will be dead soft iron.

Now suppose it is cooled a little faster and does not linger as long in the range where graphite can be formed and a good portion of the carbide does not get a chance to change to graphite—the cooled iron will have normal carbide present.

Now suppose we outdo ourselves on cooling and cool the iron as rapidly as possible, say by quenching. Before going into what happens, let me say that this is one of the most important points in hardening any kind of iron as well as steel, and by visualizing this change one of the principal changes in heat treatment—that of hardening—can be understood.

The dissolved carbon cannot stay dissolved in the iron but must separate on cooling as we previously mentioned. In the dissolved state, however, it is spread throughout the iron just like sugar in coffee. Now, ordinarily if the cooling is slow enough, carbides will form and other atoms of dissolved carbon will migrate to the carbides already forming, causing them to grow into normal size carbide particles. If on the other hand the cooling is extremely rapid, the dissolved carbon does not get a chance to migrate and must separate just about where it was in the dissolved state. Under these circumstances, carbide particles exist in an almost atomic state scattered throughout the iron. When this happens, no actual free iron is present at all, and an entirely new thing known as martensite exists. This martensite is extremely hard and we have what is known as hardened cast iron.

So there are three ways cast iron can be cooled from 1500 F or over:

1. Very slowly will cause all carbon to form graphite.

2. A little more rapidly will cause some dissolved carbon to form normal carbide.

3. Very rapidly will produce martensite which is very hard.

We already discussed what happens when an iron with no carbide and an iron with normal carbide are heated, but we did not discuss what happens when a hardened iron is heated.

During the hardening process the material is cooled so rapidly that the carbide particles are very small. When hardened iron is reheated, however, the migration will start and larger carbides will begin to form. The higher the temperature, the more will be the migration and combining of carbides until finally at a temperature of about 1200 F the carbide will not only be combining, but part of it starts to break down into graphite.

Tempering Process

As the temperature is increased and the carbides grow, the iron softens. This process is called tempering. Thus, it can be seen that it is possible to first harden the iron, and then by tempering produce any desired hardness from fully hardened to dead soft iron.

As we mentioned, the whole thing is concerned with breaking down, dissolving, and reforming carbide particles. The subject would not be complete, however, without mentioning the effect of alloys such as chromium, molybdenum, manganese, vanadium, nickel, etc. The effect of these alloys is to make the reactions more sluggish. Thus, it is more difficult to soften iron containing certain of these alloys. This also gives them greater resistance to temperature in applications where temperature resistance is important. Conversely, however, the migration of dissolved carbon is also slower when such alloys are present, and as a result the irons will harden to martensite at a slower cooling rate than is possible without them and they are therefore easier to harden.

Now let us consider stress relief. Everyone is aware that a casting of any complexity does not cool uniformly after pouring, and as a result when room tem-

perature is reached there is a complex situation of pulling and pushing going on inside the casting which we call internal stress.

The strength of cast iron starts to fall off when it is heated to 800 F and the higher the temperature, the weaker it will be. Of course, its plasticity increases at these higher temperatures so it is possible to stretch or bend it a little without breaking it at such temperatures. In stress relief if the casting is heated to these higher temperature ranges, portions which are being pushed will compress a little; and the weaker the iron, the more complete will be this compressing and stretching. This is the stress relieving process.

The highest temperature which can be used will give the most complete stress relief, since the higher the temperature—the weaker and more plastic the iron. Below 800 F, however, very little stress relief takes place, and above 1100 F a breakdown of carbides starts to occur. The temperature for stress relieving, therefore, should be as high as possible consistent with the final strength requirements. It follows, of course, that if the casting is cooled rapidly from the stress relieving temperatures it will again pick up stresses. It is not necessary to cool slowly below 800 F, however, since the casting has its strength back and can on longer deform to create stresses.

DISCUSSION

Chairman: G. A. Timmons, Climax Molybdenum Co., Detroit.
Co-Chairman: R. A. Clark, Electro Metallurgical Div., Union Carbide & Carbon Co., Detroit.
Recorder: T. Giszczak, Central Foundry Div., GMC, Defiance, Ohio.

Q: Is the graphite pattern of an iron changed on reheating?

A: Graphite loses the sharp corners that are obtained on initial solidification. There is no increase in the quantity of graphite.

Q: To what temperature must an iron be heated to remove massive carbides?

A: An iron must be heated to 1600 F for at least 55 min to remove massive carbides. However, the dendritic pattern will remain the same. The above assumes that approximately 2.50 per cent silicon is present in the iron.

Q: How soft is it practical to anneal cast iron and what factors effect hardness?

A: A cast iron can be softened to a point where only ferrite and graphite remain. The analysis of the iron will determine the final brinell hardness. A brinell hardness of 95 can be obtained with an iron whose total carbon content is 3.50 per cent and whose silicon content is 2.50 per cent. The extent to which annealing is carried out will depend upon the properties that are needed in the final casting and the speed with which the machine shop wishes to machine the casting.

Hardness is effected by the manganese, silicon, and chromium contents. To get a soft, readily annealable iron the manganese must be low, the silicon should be high, and the chromium must not exceed 0.25 per cent.

Q: What kind of heat treatment is required to get a spotty iron to soften?

A: This depends upon the microstructure of the iron. The castings can be heated to 1400 F and should be held at that temperature an hour for each inch of cross section. This will give the casting maximum softness providing there are no massive carbides present. These castings should preferably be furnace cooled. The above holds only for plain gray iron castings. If the temperature exceeds 1400 F distortion may become a problem.

Q: What happens to the tensile strength on heat treatment?

A: The physical properties of an iron are usually lowered on heat treatment. Stress relieving cycles do not greatly change tensile strength while annealing cycles will reduce it as much as 50 per cent depending upon the temperature and time used. A casting with an as-cast tensile strength of 35-37,000 psi will be reduced to a tensile strength of 33-34,000 psi on being treated at 1100 F for 1 hr. In 2 hr it will drop to 30,000 psi and can be reduced to lower strengths by holding the casting for longer periods of time. At 1400 F the tensile strength of the above casting can be reduced to 22,000 psi in 1 hr.

STRESS RELIEF HEAT TREATMENT OF GRAY CAST IRON

By

J. H. Schaum*

ABSTRACT

Undersirable effects of internal stresses in gray iron castings, the way in which they are generated, and techniques for relieving them by treatment are explained.

The requirements of the modern engineering age are placing increasing demands on the materials with which the machinery of the era is constructed. When one looks at cast iron with this thought in mind, it becomes evident that present requirements are much greater with respect to warpage and breaking-load than were those of a few years ago. Thus it becomes essential that those who have a prime interest in either the production or the use of iron castings should have a thorough understanding of the influence of internal stresses.

In approaching the present subject of internal stresses in cast iron, it will be advantageous to discuss briefly several of the mechanical properties of metals insofar as they are influenced by internal stresses.

Consider, for example, the reaction of a common door spring to an externally applied tensile load. By applying force at the ends of the spring, it is elongated, and when this force is relieved the spring contracts to its original length, provided the stress in the spring remains below a certain limiting value, known as the elastic limit. This property is known as elasticity and has been defined as "that property of a body by virtue of which it tends to recover its original size and shape after deformation.[1] If the tensile load applied on the ends of this spring is increased sufficiently, something happens, and when the load is removed the spring does not contract completely to its original length but is now permanently longer. This "something" that happened is known as plastic deformation, and since the elongation is permanent it is often called "permanent set." By definition, "Plasticity is that property of a body by virtue of which it tends to retain its deformation after reduction of the deforming stress to its yield stress."[1] Plastic deformation takes place because the applied stress on the material exceeds the "elastic limit." In the case of some steels the elastic limit may be over 100,000 psi, while for some aluminum alloys it might be well under 40,000 psi. If the load on the door spring is sufficiently increased, its tensile strength will be exceeded and the spring broken. In the same way, if the internal stresses generated in a casting during cooling exceed its tensile strength, a cracked casting will result.

* Metallurgist, U. S. Department of Commerce, National Bureau of Standards, Washington, D. C.

Significance of Notch Effect

In understanding casting stresses one must also appreciate the significance of the notch effect. If a door spring is stretched around a sharp corner, the spring may become permanently deformed or lengthened at the point where it made contact with the corner. This is a notch effect and results in concentrating the tensile stresses at one point so that, if the elastic limit is exceeded, plastic deformation results. Notch effect also is clearly demonstrated in glass cutting where the surface is scratched along the line where fracture is desired. This scratch is a notch which concentrates applied stresses to a higher level than anywhere else in the glass causing it to break preferentially along the scratch. It is this same notch effect which leads to the fracture of castings at sharp corners or changes in cross section.

The foregoing are gross effects which can be seen, but a similar notch effect on a microscopic scale is responsible for the permanent deformation which takes place in gray iron at low stress levels. Each graphite flake is a brittle, low strength, irregular shaped discontinuity in the ferrite matrix acting as a notch within the cast structure, thereby causing local stress concentrations in the matrix at the extremities of the flakes. If these local notch effects cause the stress to exceed the elastic limit of the matrix for cast iron, then local plastic deformation results. Thus, if a cast iron bar is extended by applying a tensile stress and the load is removed, the bar will contract in an elastic manner to a length that is slightly greater than its original length. This phenomenon takes place at all load levels with an increasing proportion of the total deformation being plastic as the load increases.

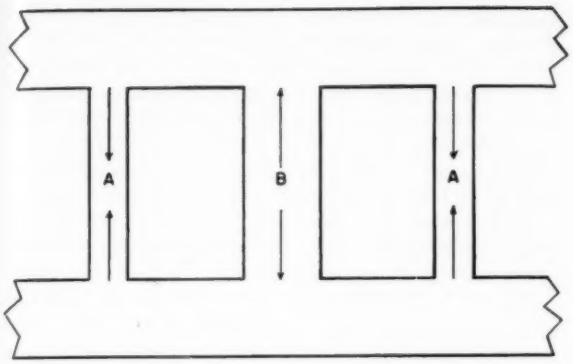


Fig. 1—Schematic diagram to illustrate direction of internal stresses in a casting with different sized cross sections. "A" members in compression and "B" member in tension.

Although stresses and strains have been demonstrated with springs large enough to see, one must realize that the internal strains developed in a casting cannot be visually observed. Internal stresses are on an inter-atomic scale and their effect is to displace the atoms in the metal crystal lattice from their normal position and hold them there. If we supply enough energy in the form of heat, then these atoms move back into their original position, or take up new locations on the lattice structure; thus the internal stresses within a casting are removed. This stress relieving movement may be called recovery or relaxation.

Nature of Internal Stresses

With this basic concept of elastic and plastic strain, it is easier to understand the nature of internal stresses in castings. Stresses result from non-uniform contraction rates within various sections of a casting during free cooling to room temperature. Since these contractive stresses often exceed the elastic limit of the material, localized plastic as well as elastic deformation takes place, and the casting will contain internal stresses when it reaches room temperature. These contractive forces generate internal stresses because of:

1. Non-uniform cooling rate of the surface with respect to the center of any cross section.
2. Different cooling rates between sections of the same casting due to different cross sections or locations in the mold.
3. Resistance of the mold to the contraction of the casting during cooling.

The first cause of internal stresses is inherent to any casting as it cools in a sand mold. Since the outside cools at a different rate than the inside, a temperature gradient exists through all cross sections. This temperature gradient leads to a stress gradient. At the transformation temperature there is an expansion which briefly reverses the stress gradient in the cross section following which the usual cooling and contraction are resumed. In the early stages of cooling the outer layers of metal are cooling faster and their contraction imposes compressive stresses on the inner layers. Because of the elevated temperature, the inner layers of metal are readily deformed in a plastic manner. At a later stage in cooling, when the inner layers are cooling faster than the outer and the entire cross section is behaving in an elastic manner, compressive stresses are induced in the outer layer by the contraction of the inner layers. When room temperature has been established throughout the casting, the outer layers are in compression and the inner layers are in tension. In general, the metal which cools more rapidly during the early stages of cooling retains compressive stresses; whereas the part which cools more rapidly in the later stages retains residual tensile stresses. The non-uniform elastic and plastic strains resulting from contractions and expansions lead to warping, cracking, or complete fracture of the casting before it is ever put in service. Although these tensile and compressive stresses may balance each other in the "as cast" condition, an operation, such as machining one side of a flat plate, unbalances the stress system, and the casting may warp in a direction to restore this balance. Usually this type of internal stress causes trouble only in castings requiring good dimensional stability or close tolerances in machining.

The second cause of internal stresses is much more common and is illustrated in an exaggerated form in Fig. 1. In the early stages of cooling the two smaller connecting ribs "A" will normally cool faster than the heavier section "B." This temperature differential may generate stresses high enough to deform "B" plastically. As the temperature of "B" drops sufficiently to acquire a degree of elasticity, its thermal contraction will impress compressive forces on the "A" members. In the latter stages of cooling the "A" members offer resistance to the contracting "B" member so that tensile stresses develop within it. If these stresses become great enough to exceed the elastic limit, they will deform or break the ribs. Even if no fracture or distortion does result, the existence of these internal stresses will materially weaken the structure if they are in the same direction as that in which the member is stressed in service. Measurements have been made on this shape of casting

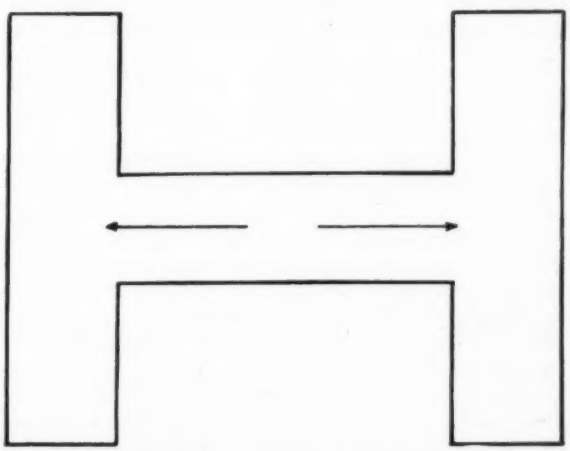


Fig. 2—Schematic diagram to illustrate direction of internal stresses in a casting due to resistance of mold to contraction of casting.

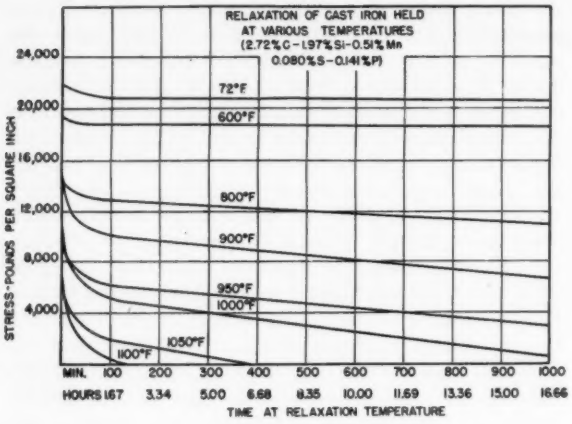


Fig. 3—Relaxation of cast iron at various temperatures.[2]

and as much as 30,000 psi tensile stress may exist in the heavy section with 15,000 psi compressive stress in each of the thin sections. Stresses of this type may also be developed in sections of similar size as a result of differences in cooling rate arising from the influences of gating, risering, and casting design.

The third cause of internal stresses—resistance of mold to contraction of the castings—is illustrated in Fig. 2. One can readily see that the contraction of the horizontal connecting member will tend to pull the two vertical members together. However, if the molding sand resists the movement of the outer members toward each other, tensile stresses will develop within the connecting member. If this stress becomes large enough, the casting may crack in the corners where the joining of the horizontal and vertical members constitutes a stress-raising notch-effect.

With this background of the nature and causes of internal stresses in casting, one is better equipped to understand the techniques for relieving them. These stresses, once generated during cooling, are essentially "frozen" in the casting; and if they are sufficiently high they may partially dissipate themselves at a very slow rate at normal temperatures. Advantage is taken of this relaxation characteristic in the old custom of aging or weathering gray iron castings for periods of three months to three years. Modern research methods have indicated that only 15 per cent of the internal stresses in these castings are relieved by this time-consuming practice.

Since the trend of most industrial operations has been toward higher rates of production, it is to be expected that the casting industry is vitally interested in developing more efficient means of stress relief. Stress relief can be appreciably accelerated by heating the casting to temperatures sufficiently high to permit the distorted internal structure to return rapidly to its normal condition. The uniform removal of internal stresses is best accomplished by heating the casting slowly and uniformly to temperatures below the transformation, holding for sufficient length of time to insure the desired stress-relief, and following by slow and uniform cooling in order to prevent recurrence of stresses.

Since internal stresses cannot be seen in a casting, it is difficult to measure the progress of stress relief during a heat treatment. However, this stress condition can be simulated by means of a device known as a relaxation machine.[2] In this machine a specially designed specimen is strained an arbitrary amount at a predetermined temperature, and a continuous record is made of the stress required to maintain the strain. In this way a continuous record of the amount of stress relief as a function of time at the desired temperature is obtained. For further details on relaxation testing see reference 2.

Figure 3 illustrates the type of data obtained in stress relief tests using the relaxation machine. These curves show the effect of time and temperature on stress reductions in gray iron specimens of the same composition held at eight different temperatures. It is apparent from these tests that as the heat treating temperature increases the stress relief is more nearly complete and takes place more rapidly. It is also observed that the greatest portion of stress relief takes place during the first hour at temperature, and that further stress relief takes place at a slower rate with longer holding times. Therefore, it is usually more efficient to increase the temperature and thereby obtain a decrease in the time required for stress relief than it is to hold a casting at a lower temperature for an extended period of time. The results of the 72 F and the 600 F heat treatments show the relative ineffectiveness of low temperature heat treatment for stress relief.

Magnitude of Initial "As-Cast" Stress

Figure 4 shows the effect of the magnitude of the initial "as-cast" stress on the relaxation period. By comparing the three curves it becomes evident that the higher the initial stress, the higher is the initial rate of stress relief, but the longer the time required to reduce it to a certain stress level.

These characteristics of stress relief heat treatments are similar for most compositions of cast iron although they differ in degree. In the higher strength irons, such alloying elements as nickel, chromium, molybdenum, etc., appear to retard the recovery at elevated temperatures, and therefore require higher heat treating temperatures or longer holding times to reduce internal stresses to levels comparable with unalloyed cast iron. This influence of alloying elements is demonstrated in Fig. 5, where relaxation

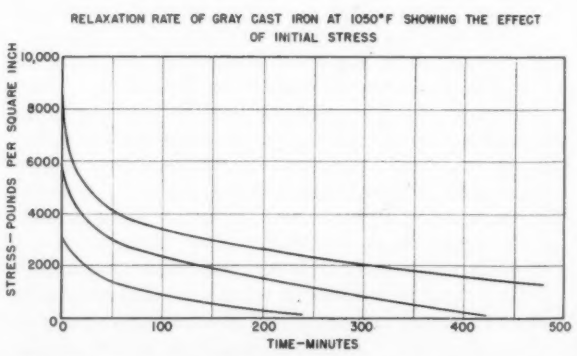


Fig. 4—Effect of magnitude of initial stress on stress relief at 1050 F.[2]

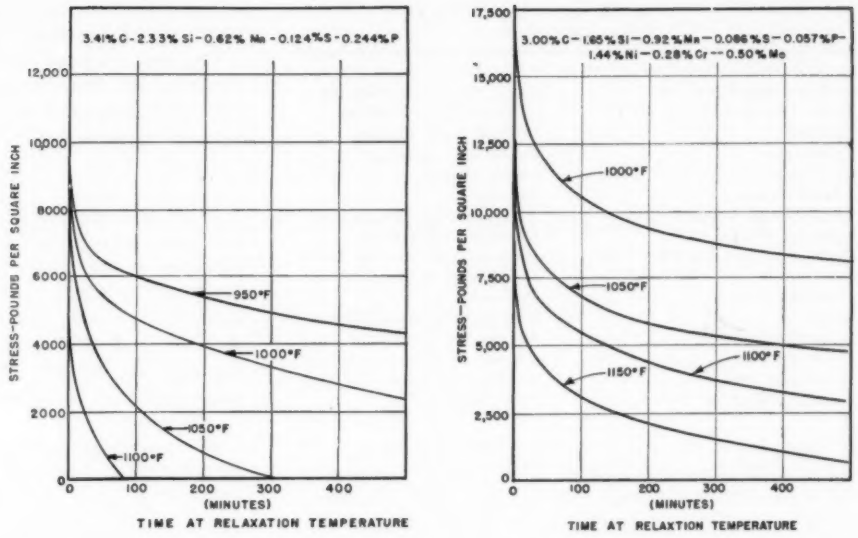


Fig. 5—Comparison of stress relief rate of alloyed and unalloyed gray cast iron.[2]

curves of an alloyed and unalloyed iron are compared. Referring to Fig. 5, the unalloyed iron is apparently completely stress relieved at 1050 F in 300 min, whereas the alloyed iron contains about 5000 psi of unrelieved stress after holding 400 min at the same temperature.

Since the strength of some of the unalloyed irons is lowered by heating to temperatures as high as 1100 F, one may find it preferable to effect only 80 to 90 per cent stress relief in a casting by heat treating below 1100 F for an increased time, or by decreasing the time at the higher temperatures. Differences of opinion exist as to the necessity of removing all the internal stresses in castings. Whether or not all internal stress should (or *must*) be removed depends entirely on the specific job the casting must do. If the casting must take a load, then the magnitude and direction of the internal stress, plus that of the service load, must remain within the specified safe limits. In many applications 50 per cent stress removal may eliminate all danger of service failure, while in other instances requiring extreme dimensional stability or heavy load carrying capacity, stress removal should be as nearly complete as possible.

Heat Castings 1 Hr/In. of Thickness

The custom of holding castings the minimum of 1 hr per inch of thickness after the furnace has reached the maximum temperature of the teat treatment is usually considered to be ample assurance that the charge will have reached a uniform temperature. After the casting has soaked for an adequate length of time at the proper temperature it should be relatively stress-free. Furthermore, in order to prevent generation of new stresses in the casting during return to room temperature after stress relief heat treatment, it is essential to effect a slow and uniform cooling so as to avoid damaging temperature-gradients in the castings. Usually, the free cooling of the charge in a well-insulated heat treating furnace is sufficiently slow for this purpose. The castings should not be removed until their temperature reaches approximately 300 F.

In the event that a foundry may desire to evaluate the effectiveness of its stress relief treatment, or devise a time-temperature cycle specifically applicable to the casting design and metal compositions being produced, a simple test casting can be made similar to the one shown in Fig. 6. This casting will contain appreciable "as cast" tensile stresses in the larger horizontal member, with corresponding compressive stresses in the smaller. If a saw cut is made through the larger member, it will relieve these stresses by allowing the member to spring open a measurable amount. The amount it opens is a measure of the internal stresses which were present before cutting. If the casting (before cutting) had been subjected to a stress relief heat treatment which would reduce this internal stress, then the casting would open less when cut. If no movement takes place between the two severed members, the casting has been nearly completely stress relieved. By heat treating a number of these test castings, the foundry can determine the minimum time and temperature required for essentially complete stress relief, or for any "percentage" relief deemed adequate for the service conditions

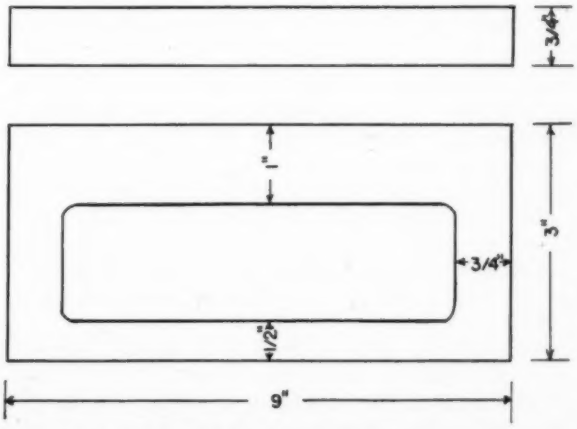


Fig. 6—Test casting for evaluating effectiveness of stress-relief heat treatment.

TABLE 1—TYPES OF CASTINGS SUBMITTED TO STRESS RELIEF[3]

Air Compressor Cylinders and Heads	Lawn Mower Knife Bars
Ball Bearing Housings	Light Machinery Castings
Brake Drums and Wheels	Machine Tool Castings
Bedbars	Nodular Iron Castings
Crankcases	Paper Mill Castings
Crankshafts	Pillow Blocks
Clutch Plates	Pistons
Cutter Head Slides	Precision Spindle Castings
Cylinder Sleeves	Pressure Vessel Discs
Diesel and Gas Engine Castings	Protractor Bodies and Dials
Distributor and Starter Castings	Pump and Motor Castings
Drawing Dies	Punches
Gears	Refrigerator Compressors
Generator Castings	Sewing Machine Parts
Governor Parts	Textile Machinery Castings
Hydraulic Cylinders	Turbine Castings
Knitting Machine Beams	Valve Castings

contemplated for the castings. The dimensions of the test casting are optional. The "as cast" stress can be increased by lengthening the horizontal members and making a greater difference in their cross sections. If lower stresses are desired, dimensional changes would be the opposite.

In some instances the development of stresses due to the resistance of the mold to contraction of different parts of a casting can be minimized by loosening the sand between or around members in order to permit freedom of movement in the direction dictated by the major stress, thereby preventing the generation of a dangerous level of internal stress. Of course, care must be taken to avoid damaging the casting while performing this operation. Another practice is to place relief cavities or low strength collapsible cores in strategic locations so as to permit unrestricted expansion or contraction.

For the purpose of emphasizing the growing importance of stress relief heat treatments, Table 1[3] has been prepared as a partial list of the types of castings receiving such treatment in this country.

Conclusion

From this discussion of the principles of stress relief heat treatment, one realizes that there is some latitude in the choice of the heat treating temperature and the time of holding the casting at temperature. This choice is governed by the composition of the cast iron, the magnitude of the "as cast" stresses, the residual stresses which can be tolerated, and the importance of any loss in tensile strength which might result.

In conclusion the factors essential for stress-relief heat-treatment of cast iron should be emphasized, namely: heating the casting slowly and uniformly to the temperature range of 1100-1200 F, holding it at this temperature for 1 to 2 hr per inch of casting thickness, and then permitting it to uniformly furnace-cool to at least 300 F.

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1. "Terms Relating to Rheological Properties of Matter", A.S.T.M. Designation: E24-42.

2. J. H. Schaum, "Stress Relief of Gray Cast Iron", TRANSACTIONS of the American Foundrymen's Society, vol 56, p. 265 (1948).

3. From a manuscript now in preparation on "Heat Treatment of Gray Iron", by C. O. Burgess, Technical Director of the Gray Iron Founders' Society, Inc., by special permission of the Society.

ANNEALING IRON CASTINGS FOR MACHINABILITY

By

A. W. Demmler*

It is difficult to know where to start on this subject. One might diplomatically ask whether machinability or metallurgy dominates your particular shop; the two often represent different and sometimes pretty divergent thoughts. Let us take "dead soft" for an example. I have in mind a centrifugal iron casting that is to be hardened before final finishing. Let us further realize there is a distinct difference between what is possible and what is practical. This dead soft sleeve could possibly be under 150 Brinell in hardness, just graphite and ferrite, very little pearlite. The tensile strength would be pretty low and the pressure applied in chucking may actually cause cracking by plain overstressing the relatively nonductile cast iron. Short of cracking or "exploding" the piece from heavy tool loads, stresses may be developed in the material that can cause serious complications as subsequent stages of operation come in. This is why stress relieving ahead of final finishing operations is a "must" on some jobs; here we are not asking for recrystallization, just removal of stresses introduced during rough machining.

Stress Due to Uneven Cooling

In the absence of an alloy content, or more precisely carbide forming elements in the iron, considerable ferrite will be obtained by heating in the range of 1200-1400 F, and slow cooling is desirable to avoid stress development from uneven cooling of different thickness sections of a casting. This temperature range however will not, in normal annealing time, break down carbides that may have developed in the initial rapid cooling or solidification of some light sections of the casting. For decomposition of carbides an appreciably higher temperature its required and here then one must consider how much growth can be tolerated and in turn possible warpage. Atmosphere control is a possibility, especially as the temperature rises, but is much more common when only the final machining is yet to be done, perhaps after hardening. Rough castings are not so frequently processed in controlled atmospheres which might also be interpreted to include salt baths. One should also recognize that a 2-hr soak at 1650 F for carbide elimination may not be as effective as 1 hr at 1750 F, yet the latter may really have an advantage from the angle of warpage. Of course at these temperatures, the castings must be properly supported in the tray or possibly car bottom. Carbide forming alloying elements have a stabilizing influence so that higher temperatures are forced to accomplish carbide elimination. In other words a chilled condition at 0.25 pct chrome can be overcome with an anneal from 1650 F, yet with 1.0 pct chrome, 1750 F will be necessary to accomplish the same end.

Stress in Chilled Structure

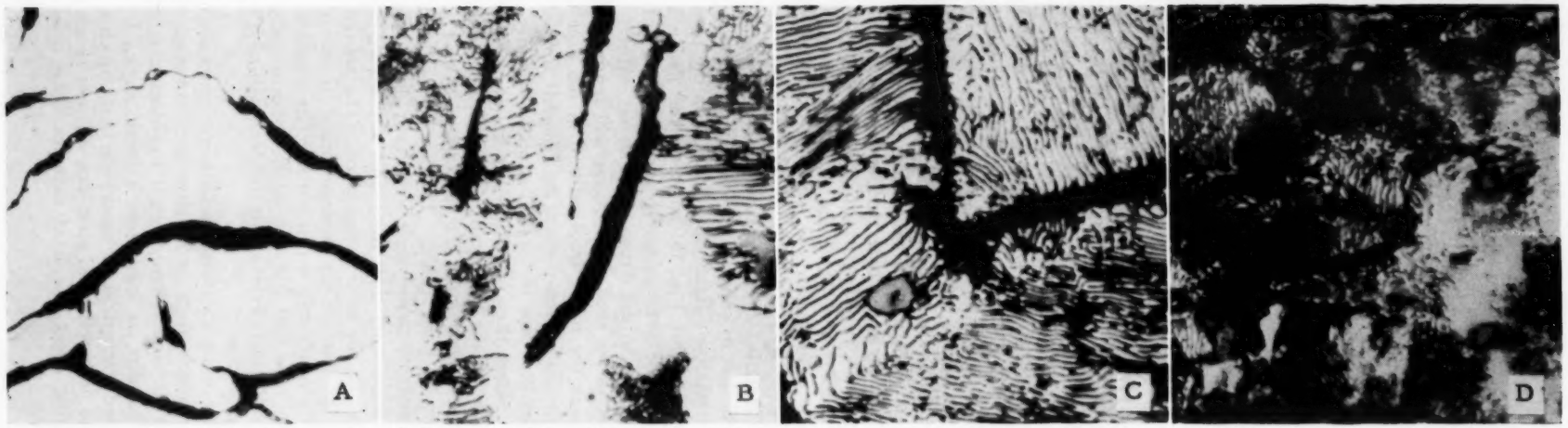
Incidentally, a chilled structure is often highly stressed. Let us consider an alloy iron flanged elbow with a relatively light wall (¼ in.) on the pipe section and about 1-in. thickness on the flange. The wall may be predominately carbides with only 20 pct carbides in the flange. The slower natural cooling rate in the sand of the flange will result in lower stresses in the flange sections than in the pipe wall. Perhaps this reference seems far removed from machinability, but not so. It will probably require 1700 F to combat these carbides (chromium containing) and those in the light wall will be reduced to only a couple per cent while the flange section will retain perhaps 7-8 pct carbides. The less stressed carbides are seemingly more stable and resist decomposition more tenaciously than the highly stressed light wall carbides of the same composition. The thinking is along the lines of the process anneal in wire drawing. Unfortunately excess carbides in a casting do not improve tool life and again unfortunately the pipe wall generally does not involve machining while the flange does.

These stubborn excess carbides are generally massive too and as such are rougher on a tool edge. Massive ferrite is not hard, but it too does not benefit machinability, at least from a finish angle. It may drag and tear and at the same time build up on a tool edge to accomplish no good but will not result in a shattering effect like hard carbides. This does not

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Decreasing Tool Life ⟶

Index: 20 | Index: 10 | Index: 2 | Index: 1.5



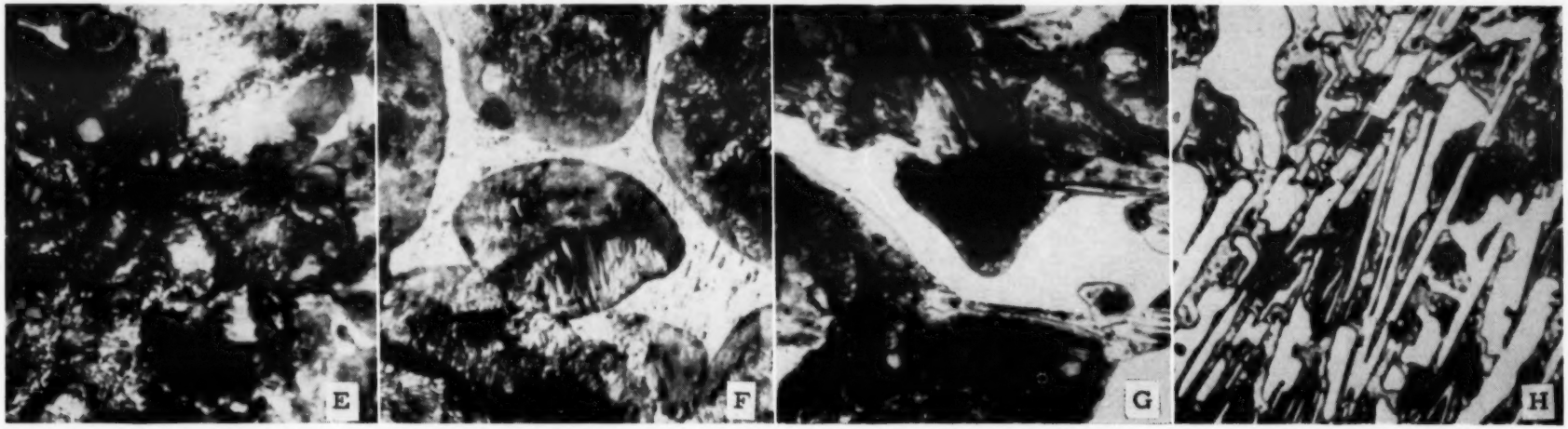
Ferrite, graphite, 120 Bhn. Full anneal.

50% ferrite, 50% pearlite, graphite. 150 Bhn. Partial anneal.

Coarse pearlite, graphite. 195 Bhn.

Medium pearlite, graphite. 215 Bhn.

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Fine pearlite, graphite. 218 Bhn.

Fine pearlite, 5% steadite, graphite. 197 Bhn.

Fine pearlite, 5% free carbide, graphite. 240 Bhn.

White iron. Bhn over 500.

Reproduction of pages 122-123 in "Increased Production, Reduced Costs Through a Better Understanding of the Machining Process and Control of Materials, Tools, Machines," Prepared by Curtiss-Wright Corporation.

mean, however, that one should avoid all carbides. After all, we have all sorts of service requirements and one cannot always give the machine shop the number one position. The writer's plant produces cast iron camshafts. They are not machined as readily as a piece of cylinder iron. The cam is a relatively high alloyed iron and is hypereutectoid, carrying a high combined carbon even in the annealed condition. The service life of this part is recognized and accepted by the machine shop. The machine shop is reconciled to the fact that for the good of service life some sacrifice of machinability is fully warranted.

An annealing cycle to attempt to eliminate all the carbides from an iron of this type is just about out of the question and undesirable. An excessively high temperature would be required which would practically ruin the fine physical properties; also in the absence of a controlled atmosphere, scaling (and its removal) would be a serious problem. As a compromise a uniform distribution of carbides in reasonably spheriodal form is the plan, in such applications as heavy duty aviation piston rings centrifugally cast in pot form. Depending on the cycle, the matrix also may be spheroidized or simply pearlitic though the former will provide somewhat better machining characteristics. But these pots are not a free machining item and again the nature of the final application dictates the course. In general when we think of annealing for machining, we are accustomed to look for a soft (low hardness) iron. These pots for example seldom fall under 269 Brinell and there is no free ferrite present; instead roughly 8 pct of extremely stable alloy carbides will be observed.

Machinability on Production Basis

If we think of machinability on a tonnage basis, the last few items above, while decidely important applications, only represent a minority of the field. The most common hardness will be from about 150 to 200 Brinell and there will be massive ferrite and no carbides present. The most common annealing temperature will be 1550-1600 F and time must depend on section size. A coarser pearlite will, in general, offer superior machinability to a pearlite of very fine lamellae. The coarser pearlite will develop from a higher soaking temperature followed by slow cooling, of course. If a spheroidized matrix is desired, the cooling is generally arrested around 1300 F for a few hours followed by controlled cycle cooling over the next 200-300 F. If a fairly high percentage of ferrite is precipitated, there is no real need for a spheroidized matrix.

So far we have ignored phosphorus completely. Phosphides are a very stable compound. At 0.10 pct phosphorus, there will be little or no evidence of phosphides in the microstructure and they will not be particularly conspicuous at 0.20 pct phosphorus, but at 0.35 pct, phosphide skeletons will be readily observed in the microstructure. In any customary annealing cycle these eutectic structures will not be altered. They are hard and brittle and cannot contribute to improving machinability.

While most of us are quite conscious of the literally shattering stresses that may be developed in drastic quenching of cast iron, we seem too often to lose sight of the damage that can be done in too rapid heating of the casting for annealing. The interior of the casting comes to temperature from a higher temperature on the surface until the entire piece is up to temperature. This temperature gradient in the theoretical absence of any residual stresses in the green casting will develop stresses and these stresses piled on top of actual casting stresses may reach the cracking point. When you hear a casting "let go," you know what the writer means.

The initial stresses of the green casting may be traced to a number of causes and design itself may well be one of them. Too early shake-out without some protection against subsequent uneven cooling is another and cooling in the sand which is usually considered as commendable, can be a hazard in the case of a hard rammed mold so bonded as to possess high hot strength which will resist normal free shrinkage of the casting. To further emphasize this point, I would like to refer to some work at the U. S. Naval Research Laboratory nearly 20 years ago where contraction characteristics were observed on steel castings, in the form of smooth symmetrical bars, as they cooled from the freezing point under various degrees of (spring) resistance to free contraction. The figures below are for a plain 0.35 pct carbon steel:—

Restraining Force	Contraction above Transformation Range	Expansion Passing through Transformation Range	Contraction below Transformation Range	Total Contraction
None	1.47%	0.10%	1.03%	2.40%
Light	0.92	0.18	0.90	1.64
Medium	0.46	0.24	0.71	0.93
Strong	0.20	0.15	0.42	0.47

These differences are large and when one thinks of irregular sections it is not difficult to realize the existence of severe localized stresses. Wish there were time to review these values critically.

After a very good annealing or stress-relieving job has been done we sometimes subsequently "abuse" the casting so that doubt is focussed on the quality of the anneal. I have in mind shot blasting for cleaning; it is in fact a peening test stressing the skin. A simple example is a straight wall liner, anneal it, then cut it in half across the axle. Next slit one half and observe no movement. Blast the other half with the ends plugged to confine the blasting to the O.D. and slit this piece. There will be definite movement, but it is not necessarily damaging as the peening is confined to the skin layer which will be machined off, taking the stresses along.

The accompanying series of photomicrographs (500×) are from the U. S. Air Force Machinability Report—1950, as prepared by Curtiss-Wright Corp. Here is a set of examples of much that has just been considered. The gamut is run of ferrite, graphite, pearlite, etc., and with these, an index of relative tool life for a given set of conditions, considering fine pearlite as par 1).

HARDENING GRAY CAST IRON WITH MINIMUM DISTORTION

By

A. A. Armstrong*

ABSTRACT

Methods of hardening cast iron are discussed showing some of the advantages and limitations of oil quenching and tempering, martempering and austempering. Control of warpage in hardened cylinder liners is discussed with reference to the TTT (time, temperature, transformation) curve. For good wear characteristics, a hardened gray cast iron with random Type A graphite in a matrix of tempered martensite or bainite is recommended.

■ The development and use of gray cast iron for the fabrication of many moving and wear parts throughout the machine industry brought with it the desire for greater strength and greater resistance to wear. To improve these properties and lengthen service life, the through hardening of such cast parts was performed by heating them above the upper critical point and quenching out the matrix structure to produce a hard martensite which upon suitable tempering became tough. Problems of cracking, distortion, shrinkage, collapse, instability of dimension upon ageing in the field and failure of parts to harden have caused the metallurgist to employ many variations of heat treating procedure in order to produce more serviceable parts.

Inasmuch as the hardened structure of gray cast iron parts depends to a very great extent upon the as-cast structure, graphite flake size, distribution and orientation, toughness and composition of the matrix, the first requisite for a good finished part lies with the foundry source in producing a good casting of proper chemistry and soundness, free from large and/or excess carbides, and free from chill.

Having a good sound casting of fine structure, it is important to perform a minimum of heat tempering operations in order to prevent any deterioration of such structure. A stress relief draw of one hour per inch or less of casting thickness, preferably applied after rough machining away the as-cast surface, and after the removal of any major amounts of stock, tends to reduce distortion at the hardening operation, and if performed at temperatures of 800F to 1250F does not seem to appreciably harm the structure.

* Thompson Products, Inc., Cleveland.

Heating cast iron to a minimum hardening temperature tends to promote a more thorough quench out and minimizes the tendency to retain austenite. Raising the austenitizing temperature considerably slows down the transformation of the austenite on quenching and may result in retained austenite which will often gradually transform at a later date causing distortion.

Oil and water quenching followed by the tempering of simple uniformly proportioned parts is often satisfactory; however, distortion, warpage, and cracking rapidly appear as the part sections begin to vary and become more complicated.

Use Isothermal Treatment

Efforts to reduce quenching strains point to the use of some isothermal process, which will condition the thick and thin sections of a part so that the final desired quenched matrix structure, may be attained as quickly and as uniformly as possible throughout the entire piece . . . quickly, in order to make the operation commercially feasible and economical and also to attain some high degree of hardness as uniformly as possible, in order to minimize the tendency to form hard expanding martensite needles in thin sections, while such transformation has not yet begun in the heavier more plastic sections. These hard and highly stressed sections, if permitted to develop as they are in the normal hardening operation, will often cause such extreme stresses as to rupture or render the part useless from a distortion standpoint.

An isothermal treatment refers to a process at a fixed temperature level for sufficient time to permit the part or parts to attain a uniform temperature and often a uniform metallurgical structure throughout. In isothermal heat treatments this is often obtained by quenching parts directly from the hardening temperature into liquid salt, oil or metal baths, maintained at constant temperatures considerably above room temperature. This may be followed by either a rapid quench to room temperature, an isothermal quench at a higher temperature, or an air cool to room temperature. The following example demonstrates such a treatment and its advantages.

Figure 1 shows a thin, dry, cast iron cylinder liner of the type used in commercial internal combustion engines. This is a hardened liner calling for a

hardness of R_c 47-52. The casting has been finish machined leaving only grinding and honing stock for removal after the hardening operation. Thus, most of the stock removal has been performed in the soft or as-cast state with ease and speed. Table 1 illustrates the relative amounts of distortion experienced by a direct oil quench and air draw for 1 hour at 600F after preheating at 1250F for six minutes, and austenitizing at 1500F for eight minutes in neutral salt baths compared with the distortion experienced with the same preheat and austenitizing procedure followed by an isothermal quench of 28 minutes at 480F, a water wash and a 1 hour air draw at 600F, both hardening operations having been performed with the parts in the same free state.

The data in Table 1 show the slight amount of distortion inherent in the thin parts as they are received ready for the heat treatment. The out-of-round figures under the oil quench heading points out not only a large amount of distortion but also a large variation in the amount of distortion. Tempering has to some slight extent reduced this distortion but the necessary stock removal for clean-up is large, varies from piece to piece, and is difficult machining because of the hardness.

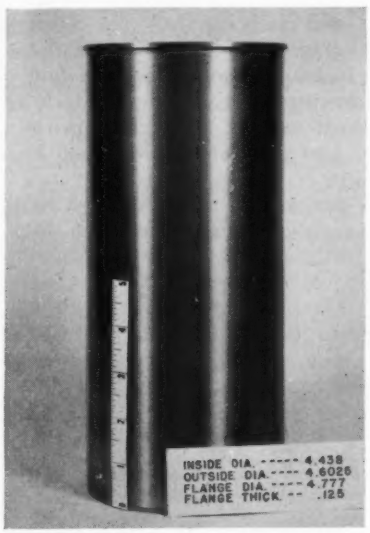


Fig. 1—Cast iron cylinder liner (left) is finish machined prior to hardening, leaving only grinding and honing stock. Table 1 shows distortion in hardening.

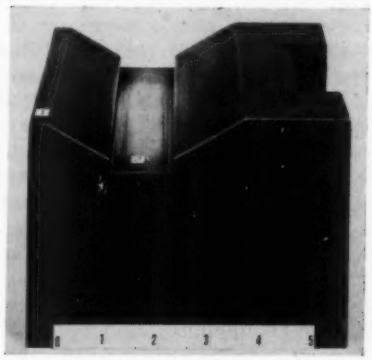


Fig. 2—Slide areas of machine tool fixture are hardened to same level by isothermal quenching.

A comparison of the isothermally treated parts shows: (1) Much less total distortion; (2) Much less variation in the amount of distortion from piece to piece; (3) Much less distortion relative to the thick and thin sections of the parts; and (4) Practically no further change in dimension with the draw operation. These facts all tend toward a more uniform stock removal at final grinding or honing and permit a closer control on machining operations presenting a more efficient method.

Figure 2 pictures a machine tool fixture with slide areas 1 and 2 required to have a hardness of R_c 48-52 when this part is made of gray cast iron. Little or no trouble is experienced in through hardening it to obtain the desired hardness on area 1 but hardness of R_c 38-42 can be expected in areaway 2 together with some distortion, unless the quenching technique is varied so that the various sections of the part travel through the Ms critical at somewhere near the same time.

Isothermal quenching reduces the central mass heat level and enables the whole part to be more thoroughly quenched out at once. The higher desired hardness can then be obtained in the recessed areaways. Drawing hardened work for 1 hour per inch or less of thickness produces a stable serviceable part.

Figure 3 from data of E. F. Houghton Co.[1] diagramatically represents four important quenching methods as related to the time, temperature, transformation curves. The first graph represents the normal oil or water quench to form martensite and to temper it to the desired hardness.

The second graph shows the martempering method where the part to be hardened is quenched into a bath slightly above the Ms point only long enough to equalize the temperature throughout the part and then quench it to room temperature forming martensite which is then tempered to the desired hardness.

The third graph demonstrates austempering with a quench higher up above the Ms point 600F to 900F for a sufficient time to complete the transformation into bainite and cool the work to room temperature without including a tempering operation.

The fourth method portrays an isothermal quench followed immediately by an isothermal draw at a higher temperature.

It is well at this time to examine the S or TTT curves for cast iron. Figure 4 is a graph showing S-

Table 1—DISTORTION OF THIN CYLINDER LINERS

	Top		Bottom	
	ID	OD	ID	OD
Before Heat Treatment				
Average Out of Round	.002	.001	.003	.001
After Oil Quench				
Maximum Out of Round	.018	.020	.064	.065
Minimum Out of Round	.003	.005	.020	.007
Average Out of Round	.012	.012	.040	.033
After Isothermal Quench				
Maximum Out of Round	.014	.009	.009	.005
Minimum Out of Round	.003	.001	.003	.002
Average Out of Round	.008	.006	.006	.003
After Oil Quench and Draw				
Maximum Out of Round	.017	.023	.061	.071
Minimum Out of Round	.006	.004	.012	.019
Average Out of Round	.011	.010	.039	.025
After Isothermal Quench and Draw				
Maximum Out of Round	.014	.008	.007	.005
Minimum Out of Round	.003	.003	.003	.003
Average Out of Round	.008	.005	.005	.004

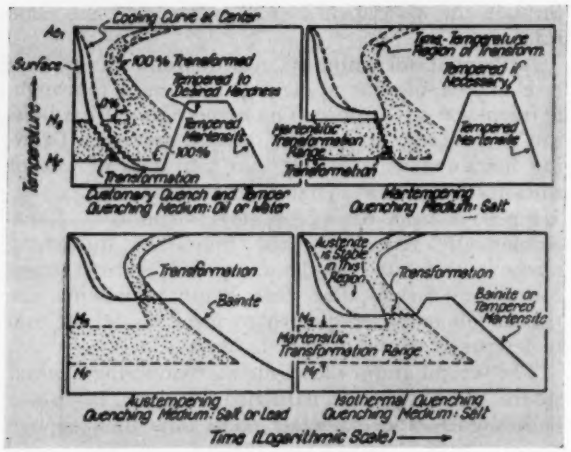


Fig. 3—Four quenching methods related to TTT curves.

curves for low alloy gray cast iron prepared from the work of C. R. Austin.[2] Superimposed upon this graph is curve 3 which represents a martempering hardening cycle such as is most commonly applied today. From curve 3 (Fig. 4) one can see that the total isothermal furnace time is reasonably low exclusive of the draw operation which can readily be an air draw.

For example, a gray cast iron part with a maximum thickness of one-half inch could be martempered by preheating for six minutes at 1250F, austenitizing for eight minutes at 1500F, marquenching for 12 minutes at 480F cooling to room temperature and tempering 1 hour at temperature. This procedure can be expected to produce a stable finished part of maximum hardness and minimum distortion. Hardness of R_C 47-55 may be expected depending upon the initial structure of the casting. The matrix structure of the finished part will be tempered martensite.

Curve 4 (Fig. 4) demonstrates an austempering cycle featuring a 600F isothermal austempering quench requiring approximately 9,000 seconds or 2½ hours for its completion. Further tempering is not required to stabilize the matrix structure which will be tempered martensite and bainite with a hardness approximately equal to a normally hardened part with a 600F tempering treatment. If a lower matrix hardness is desired, an isothermal draw at an elevated temperature may immediately follow this austempering cycle. The advantages of this cycle lie in the ability to harden parts without the formation of martensite and its accompanying expansion which may cause distortion and cracking.

However, in the use of Fig. 4, it is well to remember that these curves are representative of low alloy gray cast iron and may be considerably displaced by variations in both amount and type of alloy content. This increased alloy content may raise or lower the Ms transformation and will suppress the S-curves so as to lengthen the transformation time. It is, therefore, possible with increased alloy content to underestimate the transformation time and particularly in an austempering cycle to end up with a product containing retained austenite later transformed into untempered martensite.

In conclusion, it can be said that a through hardened gray cast iron part having a good random distribution of Type A graphite size 4 to 6 in a stable, well-tempered martensitic or bainitic matrix containing no retained austenite or untempered martensite and having enough alloy of the proper kind to produce the necessary strength and toughness will show good service and long wear life.

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2. C. R. Austin, "Isothermal Heat Treatment of Meehanite Metal," *Metal Progress*, August 1952.

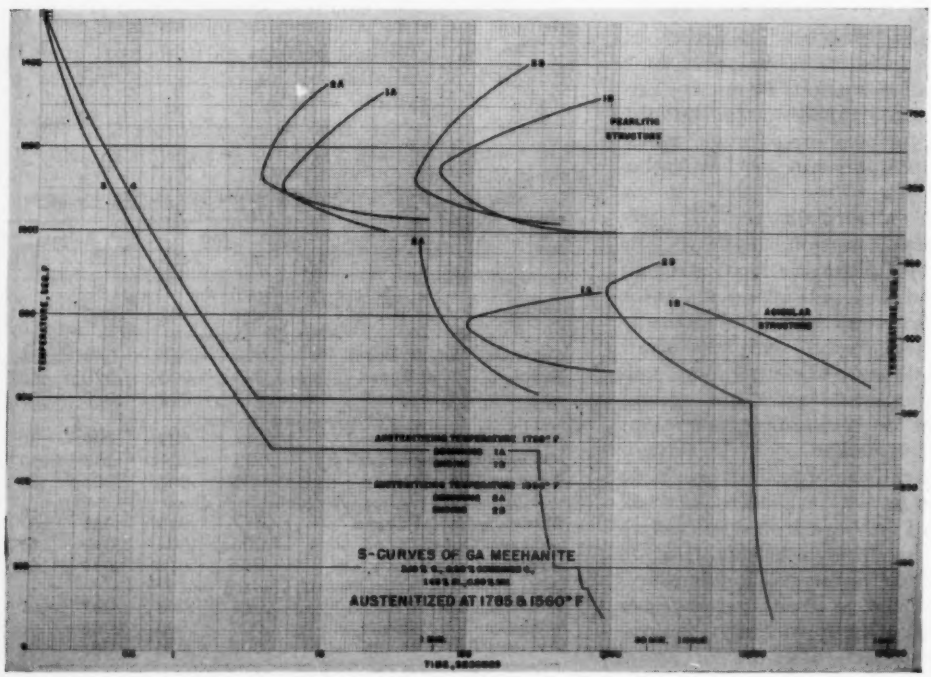


Fig. 4—S-curves for low alloy, inoculated gray iron with martempering hardening curve (3) and austempering cycle (4) shown. Curves can be considerably displaced by composition changes.

FLAME HARDENING OF CAST IRON

By

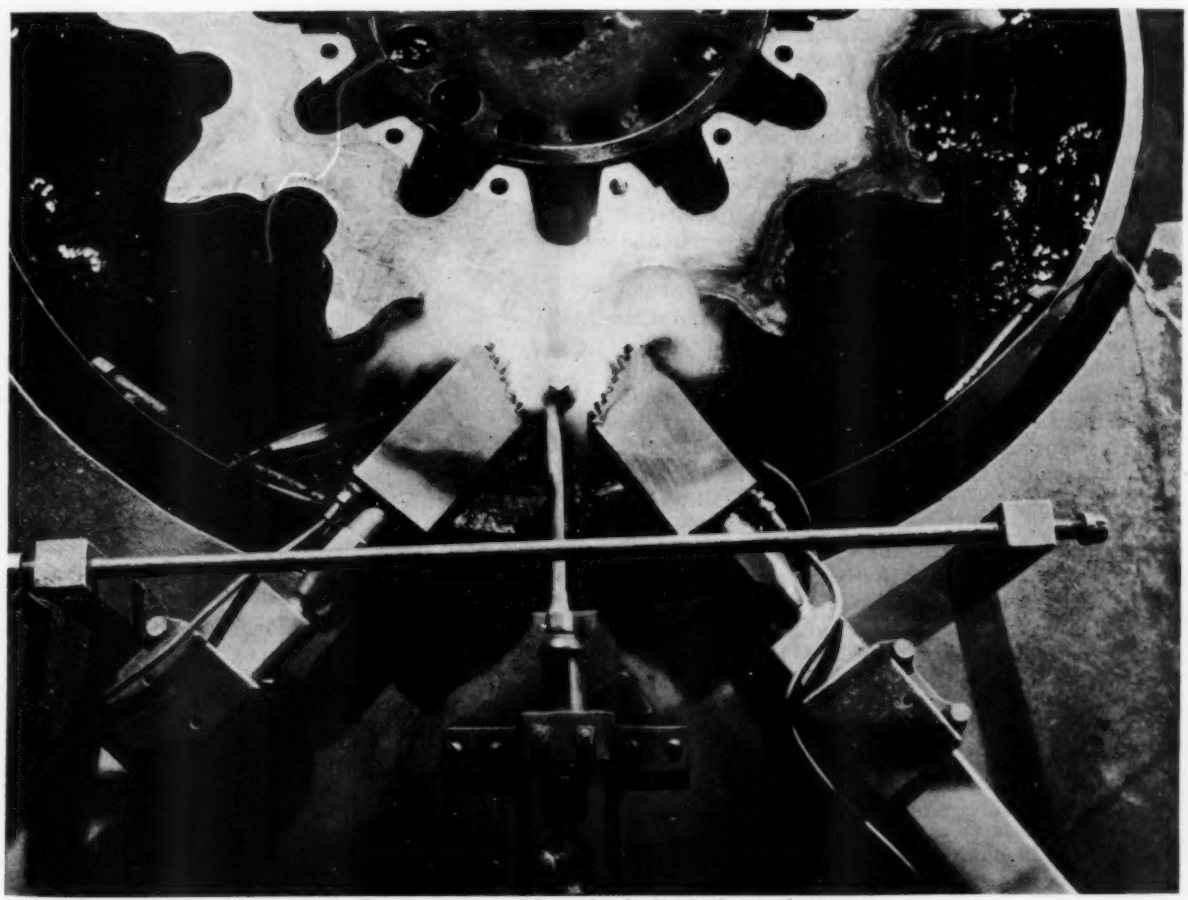
M. R. Scott*

■ In the past 15 years the foundry industry has mechanized its equipment and quietly carried on a program of research and development which should now place it in a fine competitive position. Many of the new techniques applied to steel fabrication can, and some are, being used successfully in cast iron. One of these is the flame hardening process. The recurrent shortage of alloys has enabled the metallurgists to take advantage of, and coordinate, the inherent qualities of cast iron and flame hardening. Very real and substantial savings can result when a basically sound and practically applied process, such as flame hardening, is used with a full understanding of the results expected and attained.

Flame hardening consists of localized heating followed by quenching to provide a physical change in the surface of the metal. The limits of application of the process are wide and may be determined only by such practical aspects as availability of gases and manufacture of suitable equipment. The process offers wide diversification in the treatment of low, medium, and high carbon irons and steels. As in other hardening by heat treatment, the quench may be air, water, or oil.

Oxygen and acetylene are the two most commonly used gases because they produce the highest flame

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Overhead view of flame-hardening tank sprockets.

temperature (approx. 6000 F) and a high rate of flame propagation. Many of the petroleum gases are used for flame hardening but they produce a lower flame temperature and consequently require longer heating periods. The economics of using petroleum gases combined with oxygen, or acetylene and oxygen, can probably best be determined by a study of the application for which they are intended.

Since it is necessary to use a highly oxidizing flame to secure the maximum flame temperature with petroleum types of gases, a decarburized skin will result on the surface of the part heated. On the other hand, the oxy-acetylene flame can be adjusted to provide either a neutral, oxidizing, or reducing flame without seriously affecting the flame temperature and therefore the rate of heating. Generally speaking, the oxy-acetylene flame can be used to greater advantage in the treating of cast iron, since the more rapid heating will produce a satisfactory depth of hardness with the minimum distortion.

Cast Iron Metallurgy in Brief

Gray iron constitutes by far the largest tonnage in the present foundry industry, therefore the comments in this paper will be directed toward gray iron, although they may also apply to the special irons now coming to the attention of metallurgists and design engineers.

Cast irons containing a considerable amount of graphitic carbon are known as gray cast irons because the appearance of their fracture is grayish or blackish and coarsely crystalline. Cast irons with all the carbon in the graphitic condition (no combined carbon) are extreme types, seldom produced, and not readily flame hardened. The structure of cast iron practically free from combined carbon consists of an iron or ferrite matrix in which are embedded many irregular and generally elongated curved plates of graphite (Fig. 1). These graphite plates break up the continuity of the metallic mass so effectively that the ductility and malleability of the iron is completely destroyed.

The brittleness of graphitic cast iron is not due to the proportion of graphite it contains but rather to the shape and dispersion of the graphite particles (Fig. 2). Conversely, when the graphite occurs in small rounded particles as in malleable or nodular irons the ferrite matrix may retain considerable ductility and malleability.

Cast iron has the facility of being able to withstand metal-to-metal wear primarily because of the embedded particles of graphite. However, the comparatively soft matrix offers little resistance to hard, foreign particles which may become wedged between sliding parts. When such a material is hardened, it is reasonable to expect that graphite particles embedded in martensite will provide a nearly perfect surface for wear resistance, superior to steel of equal hardness. Figure 3 illustrates the martensitic structure immediately below the surface of a flame hardened specimen.

Best Combined Carbon for Hardening

Most of the cast iron produced in this country contains both graphitic carbon and combined carbon, the latter in the range of 0.25 to 0.85 per cent. The factors affecting this distribution of carbon between the combined and graphitic states are: (1) Rate of cooling during and below solidification; (2) Presence of silicon, manganese, and sulphur. Silicon promotes the formation of graphitic carbon, while magnanese and sulphur oppose the formation of graphitic carbon.

Since gray iron may be considered as composed of a steel matrix in which numerous plates of graphite are embedded, it is possible, through suitable heat treat-

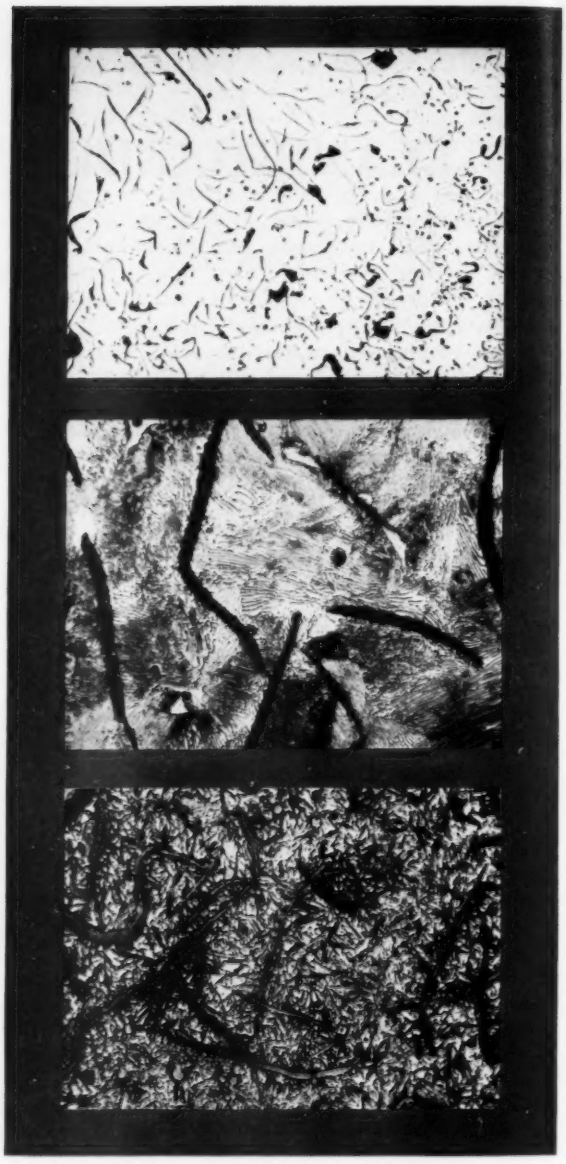
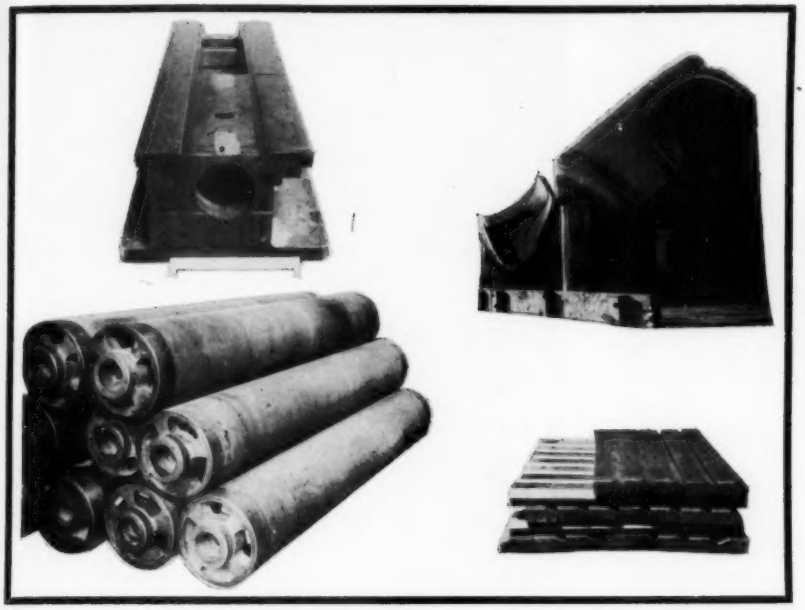


Fig. 1 (Top)—Unetched specimen, showing attitude of graphite dispersion. (X 500).

Fig. 2 (Center)—Structure of unhardened iron which is essentially all lamellar pearlite with small patches of ferrite. Etchant—1% Nital. (X 500).

Fig. 3 (Bottom)—Martensitic structure immediately below the flame hardened surface, showing stringers of graphite. Etchant—1% Nital. (X 500).



(Upper left): Typical cast iron die that has been flame hardened for longer life. (Lower left): Rolls cast soft, machined to final dimensions, then flame hardened. (Upper right): Full length cast iron ways as integral part of base. (Lower right): Ways cast sectionally.

ment, to greatly modify the physical properties of the iron. It is possible to produce a pearlitic or a martensitic matrix and secure softness or hardness in accordance with requirements.

Flame hardening can be successfully accomplished on cast irons not in the range of 0.25 to 0.85 per cent combined carbon, but such irons require special treatment and do not permit utilization of the process under optimum conditions. Unfortunately, the extremely rapid heating by oxy-acetylene flames provides a minimum of time for diffusion of carbon to form reasonably homogenous austenite prior to quenching. On the other hand, when the rapid heating is applied to cast iron of suitable analysis (CC 0.25 to 0.85) sufficient combined carbon exists to form a hard surface case of controllable depth. Since cast iron differs greatly from steel, some of the variables encountered in applying and testing the flame hardening process will be discussed.

Which Hardness Test to Use

The surface of cast iron that has been machined or ground contains tiny graphite pits susceptible to incipient fusion. Unless flame hardening is done with a thorough understanding of the process these pits may form hard spots that will be indicated by the testing equipment. Since cast iron differs from steel in this respect, the same types of tests will not prove suitable, and those who write the hardness specifications for cast iron must expect some latitude in the hardness range.

The degree of variation depends upon the type of testing equipment used. The most common error in this respect is in requiring a certain hardness reading on a Rockwell or diamond point tester. As a result, rejections occur in final acceptance of a part which should be entirely satisfactory from the standpoint of wear resistance. The most commonly used tests are the Brinell, the scleroscope, and the Rockwell. Each has its field of usefulness, but none is entirely satisfactory for the testing of a flame hardened cast iron surface. Figure 4 illustrates the type of indentation for the Brinell and Rockwell tests.

Brinell. The Brinell hardness number (Bhn) is calculated from the diameter of the impression left after a 10-mm diameter ball is pressed into the surface of the metal in not less than 6 seconds, and then held at the maximum load of 3000 kg for 30 seconds. The Brinell number is calculated as the load divided by the spherical area of the impression. The harder the metal, the smaller the indentation for a given load, and the higher the Bhn.

Variations that may occur with this method of testing are: (1) Surface hardness in excess of 500 Bhn may cause deformation of the ball with consequent erroneous readings; (2) The normal flame hardened depth of case will range from 0.030 to 0.120 in. of maximum hardness and the 10-mm ball may break through the case. From the practical aspect, the Brinell machine is not generally applicable because most cast iron parts are finished and the indentation mark may not be permissible. In addition, many cast iron parts, due to their size may not be placed in the Brinell machine.

Scleroscope. In this type of test, a diamond-tipped hammer is dropped from a definite height upon the surface to be tested, and the hardness is measured by the height of the rebound, the scale being graduated in arbitrary units. The depth of penetration is very small and the rebound is regulated more by the elastic limit of the specimen than by its tensile strength. Hence, the scleroscope does not measure the same type of hardness as do the indentation methods. Its disadvantages are the "anvil" effect when used on light sections, and the fact that it must be checked constantly against test blocks to insure its accuracy. However, its portability and the fact that it does not indent the surface of the iron deeply make it the most suitable for testing flame-hardened cast iron castings.

Rockwell. A rapid and highly regarded method for testing is the Rockwell C test which is similar to the

Vickers except that a diamond cone (120 degree) instead of a pyramid is used for the penetrator. In the Rockwell test, the penetrator is first seated by a small minor load, the dial set to zero, then the larger or major load (usually 150 kg) is applied for a specified time. When the major load is removed, the hardness number is read directly from the dial.

Application of the major load in the order of 150 kg causes variation in the readings. Figure 7 shows the type of variation that may occur when taking hardness readings at ⅛-in. intervals along a polished surface of flame hardened cast iron. It reveals that the A scale has less variation since the depth of the penetrator is less and should therefore be used instead of the Rockwell C scale. For Rockwell machines equipped with the 30N or Superficial scale (30 kg), the readings could be expected to be more accurate. However, when the latter scale is used, the surface tested must be highly polished to avoid errors created by grinding or machining marks.

Compare Rockwell and Scleroscope

Since the Rockwell tester is well known, and usually available, hardness is generally specified in one of its scales. However, the actual readings may be taken by scleroscope and converted to Rockwell but the variation in this conversion must be taken into consideration in testing cast iron. An example of this is illustrated in Fig. 5, which shows the variation in conversion from Rockwell C to scleroscope, and the actual scleroscope readings. It is believed that the actual scleroscope readings are more representative of the true measure of the surface hardness than Rockwell C. It is also noticeable that, the higher the hardness of the surface, the greater will be the variation between the two methods in conversion.

The problem of hardness testing cannot be discussed without mention of the practical and probably the most universally used method—file testing. While testing with a file cannot be accepted for high accuracy, in the hands of an experienced individual who understands testing and the metallurgy of cast iron, it can be a valuable aid in determining the physical characteristics of the part being tested.

Several file manufacturers supply hardness testing files in ranges equivalent to 35 to 65 R_c. This is a definite improvement over the single file method and may be useful in checking parts which cannot be tested by the usual methods.

How Deep You Should Harden

The depth of the flame hardened case is usually determined by the service to which the part may be subjected, but in the actual hardening process, it becomes a function of time, temperature, and quench and is both controllable and reproducible. The normal flame hardened case will be in the range of 0.030 to 0.120 in., although for special applications it may be increased to 0.250 in. or greater. In hardening cast iron to a specified depth, certain precautions must be observed if the surface condition is to be left in a perfect state for wear resistance.

It has been indicated that the ratio of oxygen and acetylene is variable within limits. Since depth of hardness is primarily a function of time at temperature, and the temperature of the oxy-acetylene flame is approximately 6000 F, it follows that cast iron may have some of the carbon burned from the surface if subjected to a neutral or oxidizing flame for a long period of time.

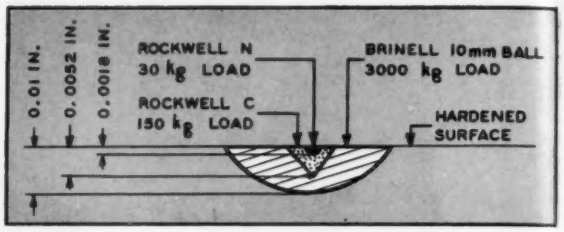


Fig. 4—Relative depths of Brinell, Rockwell C and Rockwell N hardness impressions.

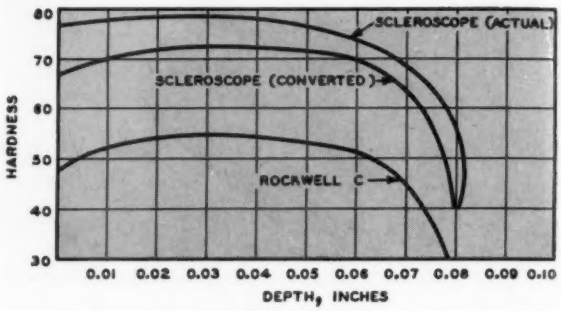


Fig. 5—Rockwell C—Converted vs. Actual Scleroscope

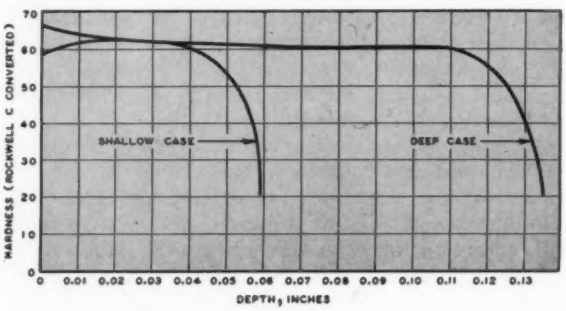


Fig. 6—Surface hardness—Deep vs. light case depth.

The normal type of hardness curve is illustrated in Fig. 6, which reveals a slightly lower surface hardness in a total case depth of 0.060 in. If this case depth were extended to 0.125 in., and the neutral or oxidizing flame adjustment maintained, the depth of decarburization would be increased, since the carbon would be burned from the surface more rapidly than it could be combined from graphitic carbon in the matrix.

Or the oxy-acetylene flame may be adjusted to provide a reducing atmosphere and thereby protect the iron surface from decarburization. In fact, some evidence exists to prove that the surface carbon may be increased somewhat though the time at temperature is short. The probability exists that there is sufficient time to convert a higher percentage of graphitic carbon to combined carbon. The top curve in Fig. 6 reveals this condition of high surface hardness combined with greater depth when subjected to a slightly

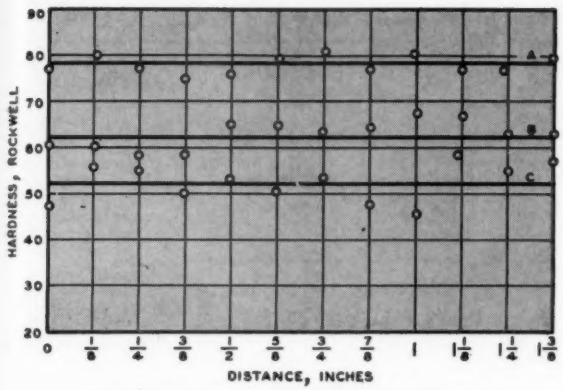


Fig. 7—Variations in Rockwell A, B and C.

reducing flame during the hardening operation.

Further practical aspects may dictate the depth of case to be specified. Unless the casting is sufficiently massive, or the part to be hardened is properly confined, a heavy case depth may cause distortion well beyond manufacturing tolerences. The optimum case depth of 0.030 to 0.120 in. will produce the minimum distortion combined with suitable hardness.

Where Flame Hardening is Used

With the knowledge and experience of so few years, it is impossible to describe or predict the extent to which flame hardening can be utilized by the iron industry. However, it seems reasonable to assume that in the near future it may become far more important than even the most optimistic metallurgists care to predict. The initial experiments of several companies some 12 to 15 years ago, in flame hardening machine ways, has become the accepted practice of today. This field seems unlimited and its growth will depend upon the ability of the foundry industry to point out the advantages of using cast iron when combined with suitable design features for various types of machines.

The versatility of the flame hardening process lends itself to the machine tool industry, which must provide surface protection for parts which may be curved or straight, light or heavy, and cannot be economically hardened by other methods.

All industry is crowded with possibilities for use of this adaptable process and the design engineers who fully understand flame hardening agree that it can be a tremendous aid to the foundry industry in securing acceptance of castings in new fields. It may be worthwhile to illustrate a few of relatively new applications.

It has been common practice for the industry to produce chilled rolls for the paper, steel, and allied industries. This method produces a fine roll with a hardened surface created by pouring into suitable molds. These rolls accelerate the cooling of the roll surface and thereby provide a fairly high hardness. Most rolls must have a very smooth surface and quite often the chilled roll has enough hardness to prevent final sizing by machining. The result is reliance upon grinding.

There is sufficient evidence to prove that some types of rolls could be produced more cheaply, without sacrifice of quality, by casting the roll in soft condition, machining to final dimensions, and flame hardening. Page 659 illustrates cast iron rolls produced by this method.

Both the foundry and the machine tool industries are cooperating in providing machines with cast iron bases and flame hardened ways. However, to date, only a few have recognized that further savings can be achieved by casting the ways as an integral part of the base. Obviously, there are cases when this is impossible and a choice then lies between casting sectional or full length ways. In either case, it is possible to machine the ways to very close tolerences, flame harden, and then finish grind. Both types of fabrication are illustrated.

Die Life

One of the first uses of flame hardening in cast iron was to increase the life of dies used in forming steel. While this use has continued, it has never been promoted because of the inability of most heat treating shops to handle large dies. With the threat of plastic dies, and in the face of the continuing shortage of alloys, it is well for the foundry industry to evaluate the use of flame hardening in conjunction with lower-cost cast iron to provide dies applicable to the automotive and appliance industries.

The initial cost of a die is usually extremely high, and any process that will increase its productive life many times warrants study and application. Normally, the wear on dies is confined to a relatively small area of the surface and, if this area is hardened, the entire die life is entended. The sizes and shapes of dies vary considerably but the flame hardening process is adaptable to the small or large die with equal facility. Some of the cast iron dies used in the automotive and appliance industries that have been flame hardened for longer life are shown.

Conclusions

1. Cast irons covering a wide range of chemical analyses can be successfully flame hardened.
2. The desirable range of combined carbon in gray iron to be flame hardened is from 0.25 to 0.85 per cent.
3. Cast irons that do not contain combined carbon in the range of 0.25 to 0.85 per cent can be flame hardened if subjected to suitable treatment.
4. The hardness gradient found in flame hardened cast iron is sharper than that found in flame hardened steel.
5. In general, the oxy-acetylene flame is the most suitable gas combination for flame hardening.
6. The Shore scleroscope hardness tester is the most satisfactory method for testing flame hardened cast iron although it has limitations.
7. Scleroscope readings converted to Rockwell readings will be more accurate than direct Rockwell readings.
8. The Brinell is not a satisfactory method for testing the average flame hardened case.
9. The depth of case and the surface condition of flame hardened cast irons are controllable.
10. The flame hardening process is applicable to practically all gray iron castings that require selected areas to be protected from wear.

AIR POLLUTION AND PUBLIC RELATIONS

By

John M. Kane*

■ For many years the foundry industry has recognized the need for and has consistently installed effective air pollution control equipment for its dust producing operations. The recognition of the need preceded the current national emphasis on air pollution control by such an interval that the achievements of the industry go unnoticed.

Few air pollution control officials and probably too few foundrymen appreciate that:

1. Some 10 per cent of all capital expenditures for foundry equipment has been allocated to air pollution control equipment. Table 1 shows that such practice has been true for many years. Actually air pollution control expenditures are higher than Table I indicates, as installation costs are seldom included in air pollution control equipment while many other types of foundry equipment are sold on an installed basis.

2. Development and improvements of the high efficiency dust collector designs such as the fabric arrester and present day wet type dust collectors were largely due to demands of the forward looking foundry industry and foundry equipment supplier. It is no mere coincidence that many of the major fabric arrester manufacturers are also makers of heavy dust producing foundry equipment such as abrasive cleaning and tumbling units. It was the advent of the central shakeout and the sand conditioning material handling systems in the 1930's that accelerated interest in and development of wet type collectors of high efficiency to cope with the steam, hot gases, and fine particles released from those operations.

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Air Pollution As Public Sees It

For most cities today, air pollution problems are largely of the "public nuisance" types—emissions from industrial stacks of dusts that settle readily on neighborhood cars, laundry, porches, window sills, and home interiors when windows are open. Some idea of the public relations troubles that could be incurred with a "do enough to get by" attitude can be visualized when effluent loadings are estimated and typical dust collector performance is evaluated against minimum standards of acceptable concentrations outlined by many city ordinances.

While foundry dust loadings vary to the extreme with the range of metal poured and with the size and quantity of castings produced, the data in Table 2 will be typical of many a medium sized foundry. The outstanding control that is obtained is significant

TABLE 1—CAPITAL EXPENDITURES BY THE FOUNDRY INDUSTRY FOR AIR POLLUTION CONTROL EQUIPMENT COMPARED WITH ALL FOUNDRY EQUIPMENT

Year	Per Cent Equipment Expenditures for Air Cleaning Equipment
1952	7.9 per cent
1951	7.9 per cent
1950	9.5 per cent
1949	14.9 per cent
1948	8.6 per cent

From statistical data of Foundry Equipment Manufacturers Association.

TABLE 2 — AIR POLLUTION CONTROL IN A TYPICAL FOUNDRY

Operation	Standard Air Cfm	Pounds per hour Collected	Pounds per hour Discharged	Effluent Air Gr/cu ft Standard Air	Effluent Air Gr/cu ft corrected to 500F	Approximate Equipment Cost Per Cfm Standard Air	Approximate Equipment Cost In 1952 Dollars
Shakeout	15,000-30,000	100- 700	10-25	0.05-0.10	0.03 -0.06	$.25-$.40	$ 4,000-$12,000
Sand System	6,000-10,000	150- 500	5-15	0.1 -0.2	0.06 -0.12	.35- .50	2,000- 5,000
Grit Blast Room	10,000-	200- 500	2- 5	0.0005-0.1	0.003-0.06	.35- .50	3,000- 5,000
Airless Blast	3,000	200- 300	2- 3	0.05 - 0.2	0.03 -0.12	.40- .60	1,000- 2,000
Grinding Wheels	3,500	10- 15	0.5- 1	0.002- 0.1	0.001-0.06	.15- .25	500- 1,000
TOTALS	37,000-56,000	660-2015	19.5-49				$10,500-$25,000
Add Cupola (5T/hr)*	7,500	30-150	15	0.2 -0.4	0.12 -0.22	$.75-$1.50	$10,000-$20,000
Or Add Electric Furnace (5T/hr)*	15,000	12- 30	8.0	0.05 -0.1	0.12 -0.22	$.35-$ .50	$ 5,000-$ 7,500

*The control equipment considered for this melting unit will not greatly alter the discharge appearance of the stacks.

when compared with permissible fly ash emissions from boiler stacks under many existing air pollution control ordinances. A case history study, Table 3, of a moderate sized steel foundry employing 700 people and producing 40 tons of castings per 8 hour shift confirms the general pattern projected in Table 2.

In the light of the outstanding performance of the foundry industry as recorded above, it is disappointing to find the foundry one of the first industries cited by air pollution control boards. However, it is seldom that control of the major sources of dust generation is even discussed. Almost invariably the target is the visible plume from the melting furnace stacks. In the translation from "smoke abatement" enforcement to over-all "air pollution control," the convenient visual tool for checking efficient combustion of a fuel burning boiler has been stretched to encompass stacks from industrial operations. This visual or capacity yardstick is used in spite of:

1. No known correlation between weight emissions and visual appearance.
2. Substantial concentrations of coarse "public nuisance" types of dust will not be visible in a stack effluent.
3. No opportunity exists to improve the appearance by more efficient operation of the industrial process. This comment applies to a more limited extent to the foundry cupola where better control during lighting up may be indicated or where afterburning in the cupola stack can eliminate unburned combustible materials.

What Future Holds

Looking into the future, the foundry with high efficiency, dry centrifugal, good wet collectors or fabric arresters controlling snagging, swing frame, and portable grinding have little to worry about future more stringent controls for those operations. Likewise the use of good wet collectors or fabric arresters will adequately control abrasive cleaning operations and good wet collection will give high efficiency and excellent collection for sand systems and most shakeout operations.

On the other hand, control of melting furnaces and eventually smokes from pouring operations will be expensive per pound of contaminant involved. The cost will be especially high if industry does not make a more determined effort to counteract present trends of control authorities and public to view visible plume from melting furnace stacks as a major contributor to the community air pollution problem.

If the few pounds of metal fume from ferrous melting stacks must be removed to satisfy a visual standard of performance, control will be expensive and complicated, where related to the few pounds of sub-micron particles involved. Current foundry trends of wet collector applications to cupolas and electric furnace stacks to remove all coarse particles have been noticeably retarded as acceptance of that degree of control has been debated by air pollution control authorities.

To date, the foundry industry has made little effort to tell community or control groups about its extensive and effective air pollution control installations. It is possible that such a program at the community level would do much "public relations" good, would forestall public criticism based on lack of information, and could even be helpful in giving the control authorities a better understanding of the air pollution control efforts and expenditures which actually are far above those of many other segments of dust producing industries.

Table 3 DUST LOADING IN A MODERATE SIZE STEEL FOUNDRY

Operation	Dust Load to Collector lb/hr	Dust Load to Collector Grains/cu ft	Dust Load to Atmosphere lb/hr	Dust Load to Atmosphere Grains/cu ft
Shakeout, Sand System[4]	130	.544	3.9	0.016
Sand Mixer Cooling Hoods[3]	75.0	0.405	1.5	0.008
New Sand System[2]				
Shakeout 8 x 10[4]	104	0.304	4.2	0.012
Electric Furnaces[1]	10.2	0.296	2.4	0.07
Wheelabrator 48 x 48 in.[1]	35.0	0.639	1.1	0.026
Blast Room 13 x 15 ft[4]	91.0	0.945	1.8	0.019
Blast Room 12 x 14 ft[4]	78.0	0.928	1.6	0.019
Blast Room 14 x 15 ft[4]	91.0	0.840	1.8	0.017
Airless Blast 48 x 96 in[4] and Tumbling Mills	70.0	1.045	1.4	0.021
Swing Frame Grinders[1]	15.1	0.126	1.6	0.014
Snag Grinding & Cut-off[1]	36.2	0.516	0.4	0.0054
TOTAL	725.5		21.7	

1. Weights obtained by actual sampling of inlet and outlet ducts.
2. No data available due to infrequent use of new sand system since sand reclaim system installed.
3. Weights obtained by sampling one collector system and assumed same performance on remaining two systems.
4. Loadings obtained by weighing dust actually collected for 24 hour period, assumed collector efficiency same as that tested on similar systems.

EFFECT OF CASTING "FIBER" ON MECHANICAL PROPERTIES OF ALUMINUM-4 PER CENT COPPER ALLOYS

By

William D. Walther,* Clyde M. Adams* and Howard F. Taylor*

ABSTRACT

This paper describes part of a larger research program designed to determine specifically why castings of aluminum-copper alloys usually possess lower strength and ductility than might be expected. Once the causes of this phenomenon are accurately isolated, it is hoped the information will prove valuable in designing means for the foundryman to improve his cast product and thus widen the application of aluminum castings in engineering. The problem of strength and ductility of aluminum-copper castings is symbolic of conditions in manufacture of many alloys having a relatively wide temperature range of solidification.

This paper is concerned with the existence and significance of a cast fibred structure in a high purity aluminum-copper alloy. Specifically it shows:

1. A cast fibred structure results from a preferred distribution of gas porosity along grain boundaries. The special apparatus and casting procedure used gives rise to three different types of macrostructure, two having highly preferred crystallographic orientations, and from the preferred crystallographic orientations result the regimented gas porosity.

2. Conditions necessary to produce the various macrostructures have been delineated. Some interpretations of the structures encountered and mechanism of their existence have been proposed.

3. A consequence of the cast fibred structure is a marked directionality of mechanical properties; that is, test bars from cast ingots exhibit different mechanical properties depending upon how the test piece is cut from the specimen.

4. Favorably oriented test bars have mechanical properties approaching those of the wrought material.

Introduction

Gas porosity has often been designed as the chief cause of low mechanical properties in cast aluminum alloys. Many technical papers and books have dealt with the possibility of decreasing gas (or "interdendritic") porosity in cast aluminum alloys to increase strength. Normally only at very low gas contents does the strength of cast aluminum alloy increase significantly.[1] The extreme measures necessary to completely rid aluminum alloys of gas porosity have been strikingly set forth by Whittenberger and Rhines in a recent publication.[2] They found it necessary to use vacuum techniques and to boil part of the metal away to completely purge the metal of hydrogen gas. In this case the bubbles of metal vapor acted as a fluxing agent. In practice, chlorine or an inert gas is bubbled through the ladle of molten metal and acts as the fluxing agent.

Mechanical properties of cast aluminum alloys can also be improved by reducing grain size. Reduced grain size increases strength even though the total amount of gas remains the same. The increased strength of fine-grained aluminum alloys, whether the fine grains are produced by chilling or by alloy additions, has been attributed to a favorable dispersion of gas porosity at grain boundaries.[3]

A usual distinguishing feature of cast materials is the lack of directionality of mechanical properties; i.e., castings usually exhibit equal strengths in all directions. Directionality is common in rolled sheet due to porosity, impurities, segregates, or nonmetallics distributed in a fiber structure along the rolling direction, leading to higher mechanical properties in the rolling direction than in the transverse direction. The following paper deals with a favorable distribution of gas porosity along certain directions of a cast aluminum alloy ingot in such manner as to produce marked improvement in mechanical properties in these directions. This preferred or selective distribution of porosity or impurity is designated as a cast fiber structure. Cast structures that could lead to a fibered condition have been reported in the literature.[4,5,6] Structures particularly susceptible to directionality are columnar and twin grain types, and both have been obtained before in aluminum-copper alloys.[6,7,8]

Experimental Procedure

A. Apparatus. The apparatus used for the experiments described herein is illustrated in Fig. 1. The essential elements are: 1) a copper disk 6 in. in diameter, cooled by a water jet delivering water at approximately ¾ lb/sec, and 2) a pre-heated silica sand mold, 3⅜-in. diameter by 6 in. high, open at both ends. The steel box upon which the mold and plate rest is a converted Jominy end-quench apparatus, with a pump for re-circulating water. Temperature measurements were recorded with a multipoint recorder using a chromel-alumel thermocouple. All heats were melted in a gas-fired crucible furnace using a clay-graphite crucible.

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B. Materials. Aluminum of 99.996 per cent purity and cathode copper (99.5 per cent purity) were used in the preparation of the aluminum-4 per cent copper alloy. Copper-aluminum hardener (50/50 grade) was produced and used to facilitate additions of copper to the melt. Calcium was added as calcium chips in amounts varying from 0.01 to 0.1 per cent (depending upon recovery achieved) for a series of experiments on the effect of calcium additions.

C. Procedure. Silica sand was rammed within a 6½-in. diameter steel shell to form a mold of 3⅜-in. inside diameter. The mold was dried 3 hr at 400 F prior to use. The mold was then heated with the metal during melting in a gas-fired crucible furnace. Argon was bubbled through the molten metal for 10 min to insure that the melt attained complete uniformity. The melt was then superheated to the temperature desired, the mold taken from the gas furnace and placed on the copper disk, metal temperature recorded, and the metal poured into the mold. A radiation shield was placed over the mold as soon as pouring was completed to prevent chilling by the air, and water flow was started immediately.

The above procedure insured unidirectional heat flow. The ingot maintained good contact with the copper disk during freezing, because the molten metal penetrated between the sand mold and copper disk during pouring; on solidification of this metal the mold was lifted up slightly and the bottom of the ingot held snugly against the copper disk.

Preliminary experiments indicated that different structures could be obtained by varying only the pouring temperature, provided rapid directional solidification prevailed. In the series of experiments discussed below the pouring temperature was varied between 1180 F (the liquidus temperature of the aluminum-4 per cent copper alloy) and 1670 F.

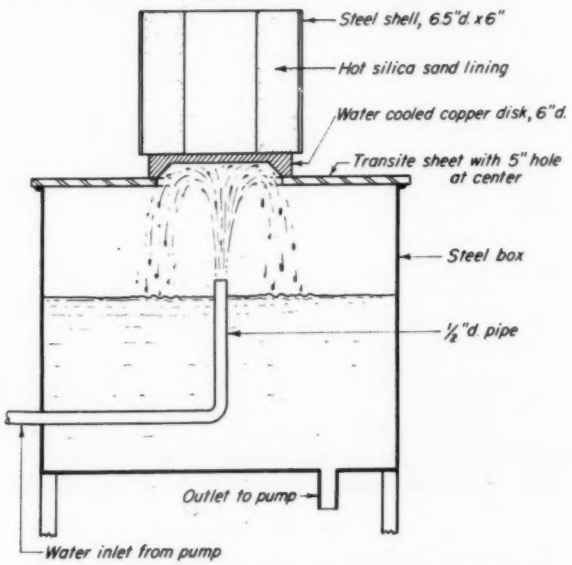


Fig. 1—Schematic illustration of apparatus used for unidirectional freezing of ingot. The apparatus consists of a converted Jominy end-quench box, copper disk, and hot sand mold. The water impinges against the copper disk at a velocity of 6½ ft/sec.

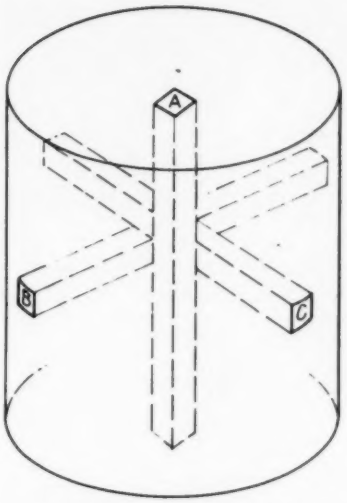


Fig. 2—Illustration of location of test bars in the ingot. Bar A is vertical and parallel to the axis of the ingot. Bars B and C are horizontal and perpendicular to each other.

The structure of each ingot was determined by etching the outside surface before sectioning. Mechanical tests were made on three mutually perpendicular test bars taken from each ingot. One bar was vertical (parallel to the axis of the ingot) and the other two were horizontal and perpendicular to each other (Fig. 2). A 0.505-in. diameter test bar was used, but the size of the ingot made it necessary to employ a short gage length of 1.5 in. Mechanical properties obtained were hardness (Rockwell F), ultimate tensile strength, and per cent elongation. The test bars were solution heat-treated for 24 hr at 950 ± 10 F in an air furnace, quenched in cold running water, and aged for 12 hr at 310 ± 10 F in an air furnace.

Discussion of Results

A. Structures Obtained by Varying Pouring Temperature. The experimental procedure insured complete unidirectional solidification. No shrinkage pipe was formed; rather, as freezing proceeded from the bottom of the mold, the liquid level in the mold dropped uniformly and only a flat crown was left at the top of the ingot. The top ½-in. of each ingot contained large equiaxed crystals. It took from 4 to 6 min for the ingot to freeze, which allowed sufficient heat loss from the top of the ingot for the equiaxed grains to nucleate, even though a radiation shield of transite was placed over the mold after pouring.

Three distinctly different macrostructures formed by varying the pouring temperature (Figs. 3, 4, and 5). For about every 55 F (30 C) rise in temperature a different structure developed. In view of the rapid unidirectional solidification it was unusual to obtain fine equiaxed crystals in high purity alloy. Only at very low pouring temperatures where there are sufficient nuclei present was it possible to obtain fine equiaxed crystals. As soon as the temperature was raised 30 to 50 F above the liquidus, columnar grains developed.

A grain structure, partially or completely columnar, was produced at will under proper conditions of freezing. Columnar grains resulted from intermediate pouring temperatures and rapid directional solidification. The initial chill of pouring the molten

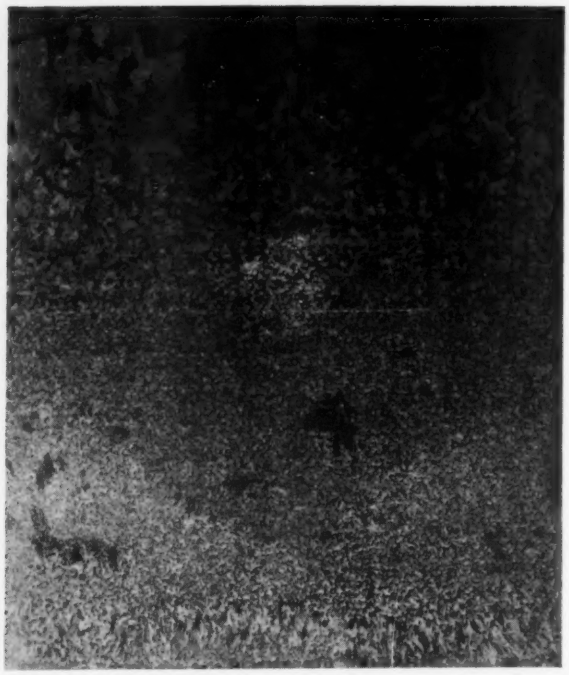


Fig. 3—Ingot No. 17, pouring temperature, 1211 F (655 C). The photograph shows the fine equiaxed structure obtained for this low pouring temperature. With slightly higher pouring temperatures columnar crystals are obtained. True size.



Fig. 4—Ingot No. 18, pouring temperature, 1274 F (690 C). The photograph shows the columnar structure obtained at this pouring temperature. At higher pouring temperatures twin grains arise. True size.

metal upon the copper base plate initiated fine, nearly equiaxed grains as shown at the bottom of the ingot in Fig. 4. Those grains at the bottom of the ingot oriented with a [100] direction perpendicular to the chill surface are favorably oriented for further growth under the conditions imposed.[5] Thus, the initial chill grains give way to favorably oriented grains which grow with a [100] direction as the long axis of the crystal. The columnar grains continue to grow as long as the heat gradient is maintained, and as long as no nucleation of crystals above them block further growth.

The macrostructure of Fig. 5 is one of columnar and twinned grains. Recent literature contains some references to the formation of twins in semi-continuous cast ingots.[3,6] X-ray work[6] has determined the twin plane in semicontinuous cast commercial aluminum to be a (111) plane with a [112] direction parallel to the direction of growth. X-ray measurements were made as a part of this study to confirm these findings. Twinned grains in cast aluminum alloys form by an accident of growth; minuteness of the twin nucleating crystal and a high degree of super-saturation (or undercooling) have been postulated

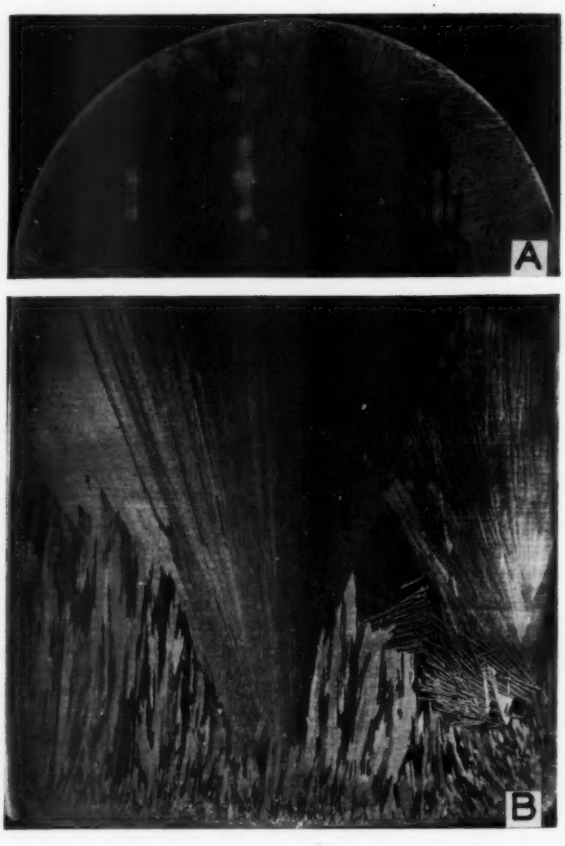


Fig. 5—Ingot No. 15, pouring temperature, 1319 F (715 C). Macro-etched surface (A) is a horizontal section at the top of the ingot, and (B) is a vertical section through the ingot. The pictures indicate that the twinned crystals grow preferentially to the columnar crystals, and may extend as flat plates clear across the diameter of the ingot. True size.



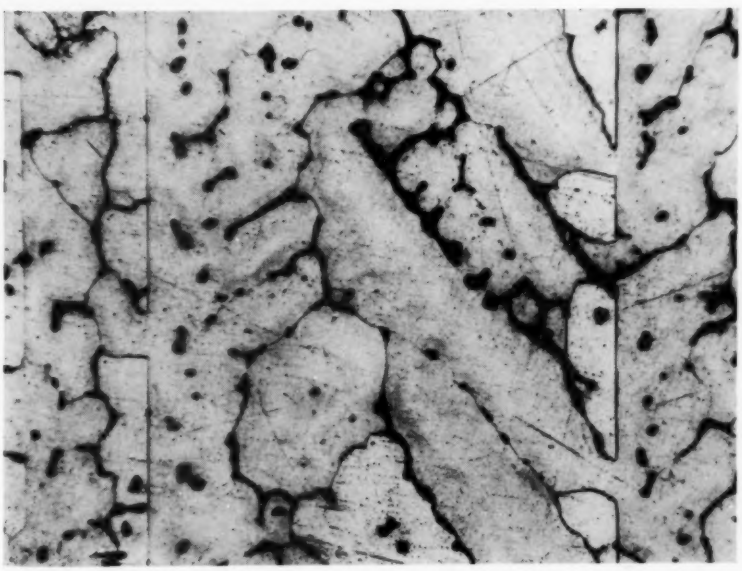
Fig. 6—A typical columnar zone showing the distribution of $CuAl_2$ along grain boundary lines. 75X.



Fig. 7—Well formed twin grains from ingot 16. The spherical spots are $CuAl_2$ phase apparently randomly deposited throughout the groundmass. 75X.

as necessary conditions for nucleation of growth twins.[9,10,11] This is covered in more detail in the Appendix.

The twinned grains in the aluminum-4 per cent copper alloy take the form of thin, flat, parallel plates stacked together like pages in a book. They extend relatively long distances in two dimensions, but are quite thin in the third (perpendicular) direction. The traces of the twinned grains show as bright and dark lines in Figs. 5A and 5B. Three different groups of twins have nucleated in the ingot shown in Fig. 5, two groups of which grew almost perpendicular to each other. The two groups of twins growing almost perpendicular to each other is clearly in evidence in Fig. 5A. The twinned grains



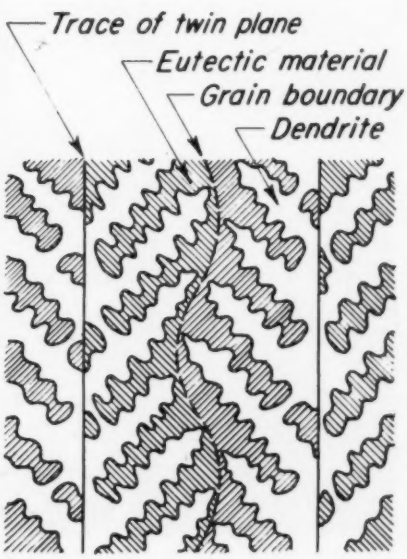


Fig. 8—(A) Twin formation in ingot 15. The eutectic deposits at an angle to the twin along 100 planes, forming a herringbone dendritic pattern. 125X. (B) An idealized drawing of twin formation, showing the mode of growth of the twin grains and the deposition of the eutectic phase.

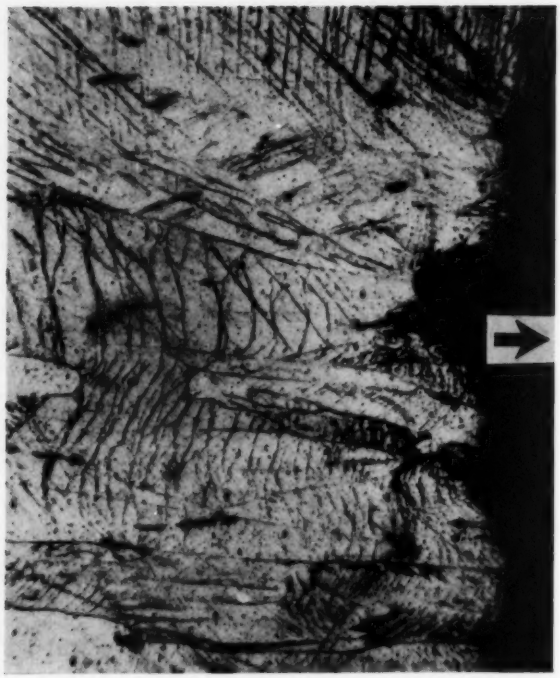


Fig. 9—Fracture zone of test bar 9 E. The slip lines and deformation bands indicate the amount of cold work. The bar necked down at fracture and gave elongation of 21 per cent. 75X.

nucleated ¼ to ½-in. from the chill surface, and fanned out until at the top they completely encompassed the diameter. One or more groups may be nucleated in an ingot. From Fig. 5 it is apparent that twinned grain growth was preferred to columnar growth under conditions imposed by the experiment. The twinned grains grew faster than the columnar grains and thus blocked further columnar growth. A possible mechanism for this will be considered later.

Microstructures of the columnar and twinned grains are illustrated in Figs. 6 and 7. The photomicrograph of Fig. 6 shows a typical columnar zone, and the distribution of spherical $CuAl_2$ phase and eutectic. The gas porosity distributes itself along grain boundaries,[3] and so will deposit along the grain boundary lines of the columnar grains; this will then cause the greatest effect on mechanical properties in the direction transverse to the columnar grains.

The photomicrograph of Fig. 7 illustrates well formed twinned grains with an apparent random distribution of $CuAl_2$ phase. More typical twin grain structures are shown in Figs. 8, 10, and 11. The latter photomicrographs, especially of Fig. 8, indicate how the eutectic structure is often deposited. The grain boundary in the twin grained structure is somewhat ragged; it might be surmised the deposition of hydrogen gas along the grain boundary during solidification will have a greater effect on the strength properties of a twinned structure than it would on a columnar structure.

Before considering mechanical properties of the



Fig. 10—Photomicrograph of a region in back of the fracture area of Fig. 9. Porosity clearly is in evidence between two grains. Arrows indicate the direction of pull. 50X.

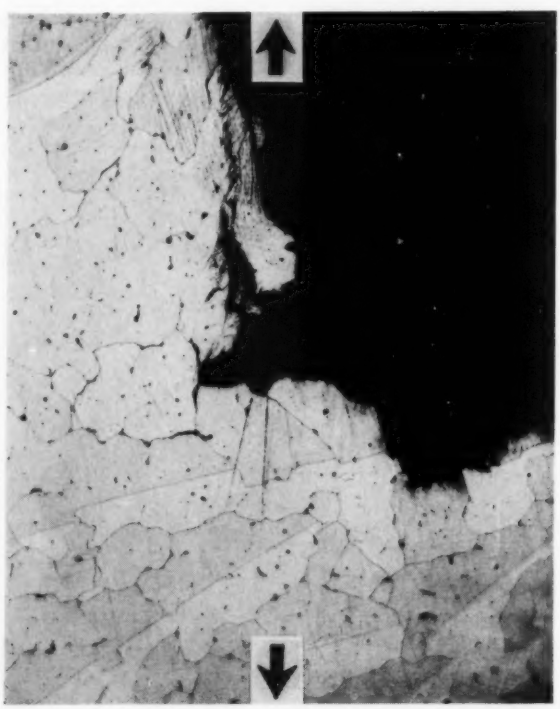


Fig. 11—Dendritic fracture of bar 26D. Arrows indicate direction of pull. Gas porosity deposited along grain boundaries initiate fracture and cause weakening of the structure. 50X.

different structures, it should be pointed out that alloy additions or impurities markedly alter the temperature limits at which equiaxed, columnar, and twinned grains are formed. For example, work done by Rosenthal[7] indicated that titanium additions raise the temperature limits at which columnar and twinned grains form to above the temperature at which titanium is no longer a grain refiner. As little as 0.01 per cent to 1 per cent calcium addition raises the temperature limit for twinning from about 1310 F (710 C) for the high purity copper-aluminum alloy to about 1400 F (760 C) for the copper-aluminum alloy containing a small amount of calcium. The calcium addition also improved and accelerated the aging behavior of the alloy.

B. **Mechanical Properties of the Different Structures.** The mechanical properties of the three different structures obtained in the experiment described herein are given in Table 1. Optimum and consistent hardness values were not obtained, due to the erratic aging behavior of the high purity aluminum-4 per cent copper alloy. Nevertheless, some strength and elongation values compare favorably with maximum values for wrought aluminum-4 per cent copper alloy; mechanical properties are approximately 53,000 psi ultimate tensile strength and 15 per cent elongation.[12] Strength and elongation values of the end-quenched ingots are especially favorable considering that no special degassing techniques were used.

Different structures exhibit different strengths, but only one structure gave nondirectional properties (Table 1, ingot 21). Ingot 21 contained all equiaxed crystals, and equal mechanical properties were obtained in axial and transverse directions. Strength values from test bars were about as would be expected for a sand casting with comparable grain size.[1]

In sharp contrast to the equiaxed structure are the mechanical properties of the columnar and twin crystal structures, which exhibit marked fiber behavior (ingots 9 and 24, Table 1). For example, the ultimate tensile strength of test bar 9b, taken with the axis of the test bar perpendicular to the twinned plate crystals, was only 25,500 psi, the elongation was about 3 per cent, and the fracture was intergranular.

Test bar 9e taken from the same ingot with the axis of the test bar parallel to the columnar and twinned plate crystals, exhibited a tensile strength of 46,500 psi, elongation of 21 per cent, and a fully ductile fracture. Bars from ingot 24 also gave directional properties; ultimate tensile strengths varied from 32,000 to 40,000 psi, and elongation from 18 to 36 per cent. As evidenced by the high ductility and low hardness, ingot 24 was underaged as compared to ingot 9, and so had lower strength.

Usually columnar structures exhibited less marked directional properties than twinned structures. Also, the columnar structures had slightly higher strength than the twin structures in both transverse and axial directions. Extensive microscopic examination of fractured test bars indicated why columnar grains gave slightly higher mechanical properties, and why both structures exhibited marked directionality.

Those ingots containing a small calcium addition likewise demonstrated the tendency of columnar grains to exhibit better mechanical properties than twinned grains (Table 2). High strength and ductility was found in both columnar and twinned structures, with the columnar structure showing significantly better properties. The high strength and hardness values (especially of ingot 22) indicate calcium accelerates and improves the aging behavior as compared to high purity aluminum-copper alloy.

Table 1

(Typical mechanical properties for the different structures of high purity aluminum-4.5 per cent copper alloy are given. Tensile bars were taken in the axial and transverse directions for the equiaxed and columnar crystals and in three mutually perpendicular directions for the twin grain ingots.)

Ingot No.	Bar Location	Ultimate Tensile Strength, psi	Elongation, %	R_f Hardness	Fracture
21b)	equiaxed transverse	31,000	1	85	Intergranular
e)	equiaxed axial	31,000	1	88	Intergranular
9b)	perpendicular to plates	25,500	3	83	Intergranular
c)	parallel to plates, perpendicular to columnar	28,000	5	85	Intergranular
e)	parallel to columnar and plates	46,500	21	86	Ductile
19b)	transverse to columnar	36,400	16	85	Ductile and intergranular
e)	parallel to columnar	45,600	28	86	Ductile
26b)	perpendicular to plates	29,600	8	84	Intergranular
c)	parallel to plates (transverse)	34,400	18	84	Ductile and intergranular
e)	parallel to plates (axial)	43,800	20	82	Ductile
24b)	transverse to columnar	32,000	18	72	Ductile and intergranular
e)	parallel to columnar	40,000	36	73	Ductile

Table 2

(Mechanical properties of an aluminum-4.5 per cent copper alloy with a small calcium addition are given. Test bars were taken from columnar and twin grain ingots.)

Ingot No.	Bar Location	Ultimate Tensile Strength, psi	Elongation, %	R_f Hardness	Fracture
22b)	transverse to columnar grains	44,800	16	89	Intergranular
e)	parallel to columnar grains	49,800	22	89	Ductile
23b)	perpendicular to twins	30,000	3	83	Intergranular
c)	perpendicular to twins	28,400	2	83	Intergranular
e)	parallel to twins	46,500	21	84	Ductile

C. **Types of Fractures.** Fracture characteristics of all test bars are tabulated as ductile, intergranular, or partly both (Table 1). Fractures were designated as ductile when elongation was high, necking appreciable, and no grain boundary cracks occurred. Bars that showed little ductility and had failures obviously along dendrite boundaries were designated as intergranular. Some test specimens gave good elongation

values, but a multitude of grain boundary separations along the gage length. Thus, measured elongation was not a good indication of the ductility. These test bars were designated as intergranular and ductile.

As a rule test bars containing twin or columnar grains, taken parallel to the axis of the ingot, exhibited greatest ductility. These test bars necked markedly prior to fracture, often having a reduction of area of 75 per cent. Elongation for these bars was from 20 to 35 per cent. A photomicrograph of the fracture zone of a test specimen containing a typical ductile fracture is given in Fig. 9 (specimen 9e).

The ductile type fracture occurred in a zone containing both columnar and twinned crystals. The slip lines and deformation bands present indicate the large amount of plastic deformation that took place prior to fracture. The fracture was definitely not along grain boundaries but was transgranular. A region of columnar grains slightly behind the fracture area of test bar 9e is shown in Fig. 10. The micrograph clearly shows gas porosity between the grain boundaries of two columnar grains. Because of the favorable distribution of the gas porosity, strung out along the grain boundary in a direction parallel to the direction of tensile pull, test bar 9e exhibited good strength.

It is obvious from Fig. 10 that the porosity would have a much greater effect on strength and ductility if the tensile pull were perpendicular to the axis of the columnar crystals. This is because the porosity strung out along the grain boundary as it is in Fig. 10 would have a much greater effect in reducing the effective cross-section of the test section and would also act as a severe stress raiser, thus decreasing the strength and ductility.

An intergranular or dendritic type fracture in a twin structure is shown in Fig. 11. Fissures opening along dendrite boundaries behind the fracture area are illustrated in Fig. 12. Arrows in Figs. 11 and 12 show the direction of pull with respect to the fractures. The twin grains of the test specimen of Figs. 11 and 12 (bar 26b) were poorly oriented with respect to the direction of pull. Twinned grains are thin, flat crystals and gas porosity forms along the irregular flat grain boundaries; this causes a relatively large reduction of the effective cross section of the test bar and exerts a large effect as a stress concentrator, thus giving rise to the low strength and ductility of bar 26b.

Figures 11 and 12 indicate the twin planes, *per se*, do not cause the low ductility and strength values, for the twin planes may be plastically deformed (bent in Fig. 11) without showing any fissures in the twin planes. Rather, the deposition of the non-equilibrium eutectic structure, and gas porosity along dendrite boundaries during solidification give rise to the low mechanical properties of bars oriented unfavorably.

The slightly inferior mechanical properties of twinned structures as compared to columnar is probably due to the less regular structure of the twinned grains; gas porosity deposits in more ragged disposition along grain boundaries and dendrite arms in this case.

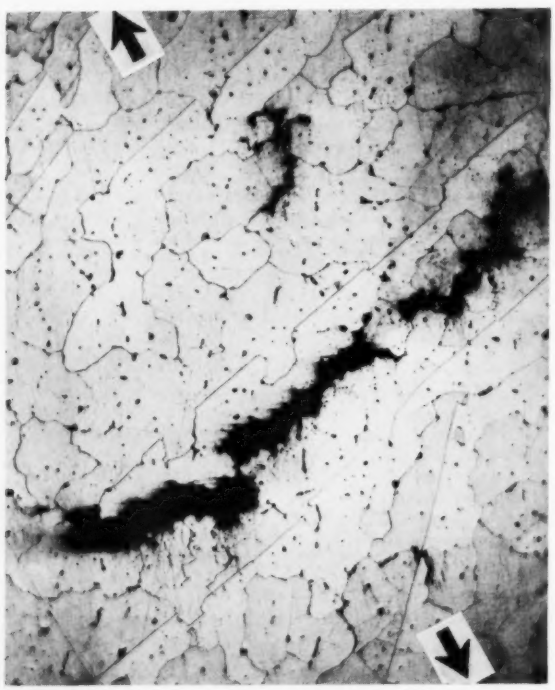


Fig. 12—Fissures opening along grain boundaries in a region in back of fracture area of Fig. 11. Arrows indicate direction of pull. 50X.



Fig. 13—Fracture area of a typical, fine equiaxed grain structure. The fracture is intergranular, and fissures have opened along grain boundaries directly behind the fracture area. 50X.

In equiaxed grains gas porosity is not oriented at all. Thus, especially with large grained samples, the thin deposit of gas at the grain boundaries acts like a notch or stress concentrator and weakens the structure equally in all directions. A typical fracture of an equiaxed structure is presented in the photomicrograph of Fig. 13. Fissures have been opened along grain boundaries and the intergranular fracture is evident in Fig. 13.

D. **Effect of Crystallographic Orientations.** The structures of the columnar and twin grain ingots are oriented crystallographically. Columnar grains are oriented with a [100] direction parallel to the axis of the crystal;[5] twin grains are oriented with the (111) plane as the twin plane and a [112] direction contained in the twin plane parallel to the heat gradient.[6] Thus, one would expect directionality of mechanical properties from a crystallographic point of view. Northcott[5] obtained oriented, columnar grained structures in cast copper and solid solution copper alloys. Copper alloys used by Northcott were less susceptible to gas porosity than aluminum alloys.

The mechanical properties of the cast copper alloy ingots were dependent upon crystallographic orientation only, and not upon a fiber structure resulting from gas. The oriented copper alloy structure gave differences in strength in the transverse and axial directions, but opposite to the differences obtained for the aluminum-copper ingots. Some of the conclusions of Northcott were: 1) equiaxed copper alloy crystals exhibited the highest strength, columnar crystals oriented perpendicular to the direction of pull gave intermediate strength, columnar crystals axial to the direction of pull gave the lowest strength; 2) columnar crystals oriented axially gave highest elongation, transverse columnar crystals were intermediate, and equiaxed crystals exhibited lowest per cent elongation.

From crystallographic considerations only, one would expect the aluminum-copper ingots to exhibit the same relative directional properties as the copper alloy ingots. (Both copper and aluminum are face-centered cubic metals.) Gas porosity offsets the effect of crystallographic orientation in the aluminum-copper alloy ingots, and the mechanical properties of the aluminum alloy test bars are mainly dependent upon distribution of porosity.

Conclusions

General conclusions from the work herein described are:

1. In high purity aluminum-4 per cent copper alloy, mechanical properties were obtained in cast structures approaching properties comparably tested in wrought material.

2. The reason for relatively low mechanical properties in cast aluminum-copper alloy is segregation of porosity at grain boundaries and between dendrite arms.

3. Three distinctly different macrostructures were obtained by unidirectional rapid cooling. When the only variable was pouring temperature, at 1211 F (655 C) drastically end-quenched ingots were equiaxed, at 63 F (35 C) higher temperature, i.e., at 1274 F (690 C), the macrostructure was columnar, and at 45 F (25 C) higher temperature, i.e., at 1319 F (715 C), twinned grains developed in profusion. These structures were reproducible.

4. A fiber type structure in the cast material, resulting from regimented porosity, gave directional properties similar to the familiar conditions in wrought material; that is, mechanical properties were relatively low in a direction perpendicular to the axis of columnar or twin growth, and high in a parallel direction. Equiaxed structures gave lower mechanical properties than either twinned or columnar types; this is contrary to the findings of Northcott for alloys of copper where preferred crystallographic orientation gave lower mechanical properties than equiaxed structures. The difference is that regimented gas porosity in the aluminum alloy overshadowed crystallographic orientation in its effect on strength properties.

5. The casting technique described herein for unidirectional heat flow from a freezing casting is a promising tool for studying solidification phenomena.

6. Alloying elements and impurities raise the temperature limits at which equiaxed, columnar, and twinned crystals arise. Alloying elements probably have a greater effect in shifting the temperature limits upward than in spreading the temperature increments apart.

7. A note is included on the mechanism of formation of twinned structures in cast aluminum-copper alloy and experimental evidence given in support of the theory.

Acknowledgment

The authors gratefully acknowledge the generous assistance of Harold Markus, Hiram Rosenthal, Frankford Arsenal, and of Prof. Michael Bever, M.I.T., in an advisory capacity, and the cooperation of George E. Schmidt, Jr. who prepared the illustrations. Appreciation is also expressed to Frankford Arsenal and the U. S. Government for sponsoring the research leading to this publication.

APPENDIX

Interpretation of the Mode of Growth of the Twin Structure

Some conditions for twin growth in mineral systems are supersaturated solution, minuteness of crystal, and rapid growth. The conditions for the formation of growth twins in aluminum and aluminum alloys are apparently the same as for the formation of growth twins in mineral crystals. According to Buerger,[10] supersaturation of solution is one of the factors for formation of growth twins in mineral systems.

It is quite likely that supersaturation is also a requirement for the development of twins in aluminum alloys. Some evidence exists supporting this hypothesis. For example, twin growth does not occur in alloys containing titanium unless the alloy has been superheated above the temperature at which titanium is no longer a grain refiner,[7] called the "coarsening" temperature. The work of Cibula[3] has shown that when aluminum alloys containing titanium are heated to a temperature below the "coarsening" temperature, they exhibit no undercooling during solidi-

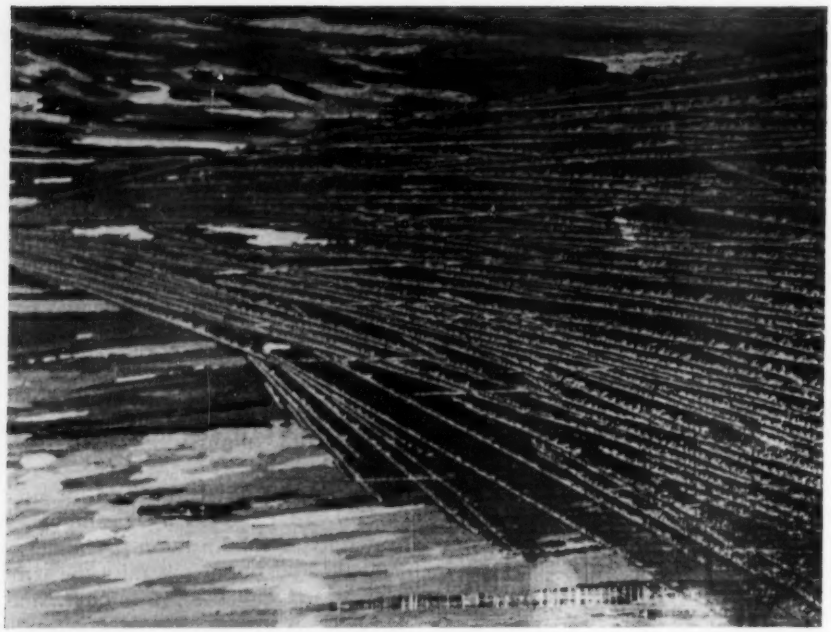


Fig. 14—A view of a group of twinned crystals in an ingot. At the right side of the group of twinned crystals, some crystals show an abrupt change in direction of growth. The change in direction of growth is probably due to strains arising during solidification, causing a fracture or deformation of the crystals. 3X.

fication, but when heated above the "coarsening" temperature undercooling occurs on solidification.

Cibula also determined that high purity aluminum-copper alloys exhibit undercooling on solidification without the necessity of superheating. Since supersaturation is a consequence of undercooling, it becomes apparent that a condition for twinning in aluminum alloys has been met. Buerger proposes that the obvious time for emanation of growth twins is just before, or at the start of nucleation of a large number of crystals, when supersaturation is highest.

This condition is met in aluminum since groups of twinned crystals in the aluminum alloy ingots always nucleated very near the chill surface. Therefore, nucleation of these twins has taken place when the supersaturation is likely to be high and in a zone of very fine grain size. A twinned crystal has a higher energy than a columnar crystal, and is subject to dissolution unless conditions of rapid growth are maintained.[10] Conditions of the experiment established a high heat gradient, thus maintaining rapid growth. A consideration of these facts leads one to the conclusion that the analogy of twin growth in mineral crystals to that in aluminum alloy is valid.

Aminoff and Brome[11] in a study of twinning in mineral crystals, stated that growth twins originate as an accident of growth and continue to grow by metal atoms depositing along the defect, building up a twin in further layers (on an atomic basis). They state the defect may be repeated, allowing further development of twin growth.

The aluminum twinned crystals must also emerge as an accident of growth (on an atomic basis), but since conditions necessary for nucleation can be maintained from experiment to experiment, twinned crystals arise as a reproducible accident of growth. Furthermore, once in being, they continue to exist when conditions are proper. Chalmers[13] has established that crystal faces having the highest melting point will grow in advance of others. The nature of growth of twins permit twinned grains to extend further into the melt than columnar grains, and to suppress their less favorably oriented neighbors; metal atoms deposit on the sides of these extensions in a twinned relation, and form more pages to the book of twinned crystals.

One other mechanism exists for the twin to fan out from the nucleus to eventually encompass the entire diameter of the ingot. Strains may arise during contraction of the solid portion of the ingot causing a thin flat twinned grain to be bent or broken; and consequently the bent crystal continues to grow at an angle to its previous direction of growth. Figure 14 illustrates this type of process, since it shows twinned grains emanating at an angle to the main group of twinned crystals.

An example of the dendrite formation of the twinned structure is given in Fig. 8, allowing an interpretation of the mode of formation of the twin. An idealized drawing of Fig. 8A is given in Fig. 8B. The photomicrograph and idealized drawing show the traces of the twin planes, dendrite arms extending at an angle to the traces of the twin planes, and the eutectic material deposited between the dendrite arms also at an angle to the trace of the twin planes.

According to Chaudron[8] the eutectic material deposits on the [100] planes of the twinned crystal at an angle to the twin plane. Since the eutectic deposits on the [100] planes, the dendrite arms in a twinned crystal must still be growing in the [100] direction, but at an angle to the direction of heat flow. A dendrite lattice on one side of the twin plane develops, related by mirror symmetry to the lattice on the other side, and is made up of small dendrite arms projecting into the melt along [100] directions.

The dendrite network is quite thin, but may extend horizontally and vertically completely across the diameter of the ingot, as the twinned grains do. This means a twinned crystal grows, not by the twin plane

itself projecting into the melt, but by the dendrite arms extending into the melt, and the twin plane is a consequence of the mirror symmetry of the dendrite lattices. This is an extraordinary mode of dendritic growth, quite unlike the dendritic growth of a columnar grain which grows by an extension of the main stem of the dendrite in a [100] direction which is essentially parallel to the direction of heat flow.

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DISCUSSION

Chairman: W. A. MADER, Oberdorfer Foundries, Inc., Syracuse, N. Y.

Co-Chairman: D. L. LAVELLE, American Smelting & Refining Co., Barber, N. J.

Recorder: J. L. SCHMIEDER, JR., Oberdorfer Foundries, Inc., Syracuse, N. Y.

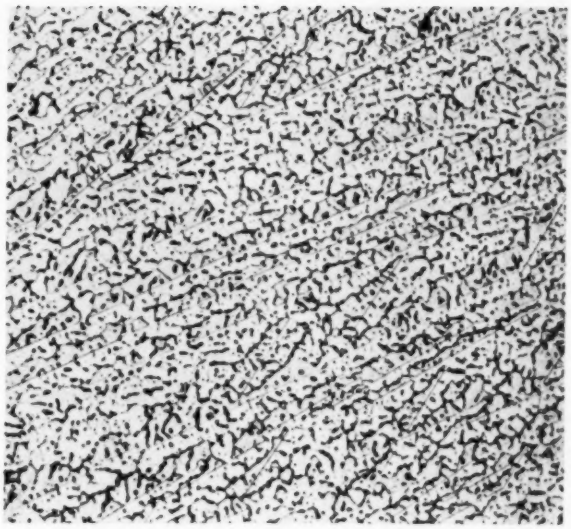


Fig. A

G. L. SALL:[1] Is this phenomenon ever seen in sand castings?

MR. WALTHER: Yes, it can happen. It is possible for columnar structures to form in sand castings if conditions are right.

P. D. FROST:[2] About ten years ago, I observed a similar twinned-dendrite structure in a number of arc welds in thin sheets of 24S alloy (See Fig. A). The formation of the twins was attributed to the extremely rapid solidification. I should be interested in hearing your explanation of the mechanism by which the twinned dendrites you have shown were formed.

MR. WALTHER: Pouring temperature, cooling rate and composition all determine possibility of reproducing columnar twin grain structure growth.

CO-CHAIRMAN LAVELLE: Does fine grain structure necessarily give high physical properties?

MR. WALTHER: No.

[1] George Sall Metals Co., Inc., Philadelphia.
[2] Battelle Memorial Institute, Columbus, Ohio.

EFFECTS OF MAGNESIUM ON EUTECTIC MODIFICATION AND GRAPHITIZATION OF CERTAIN FE-C ALLOYS

By

H. W. Weart* and R. W. Heine*

The effect of magnesium as an alloying element in cast irons has been the subject of considerable interest since the advent of nodular cast irons. The action of magnesium in causing the graphite in gray iron to precipitate as spheroids rather than flakes has been the subject of much research. A number of theories for explaining this effect of magnesium have been advanced. Considerable data have been accumulated covering the gross effects of adding magnesium to cast iron, but the basic mechanism by which magnesium exerts its influence has not become apparent. Results of research which throw some light on the important role of magnesium in solidification and graphitization in iron-carbon alloys are presented in this paper.

Experimental Procedure

In order to avoid the complicating effects of silicon, manganese, phosphorus, sulphur, and other elements present in commercial cast irons, it was decided to work with iron-carbon-magnesium alloys. The problem of preparing suitable Fe-C-Mg alloys arises immediately. Alloying iron and carbon with magnesium is difficult because the boiling point of magnesium, 2030 F, is lower than the lowest melting eutectic iron-carbon alloy, 2066 F. It may be for this reason that other investigators have stated that magnesium has a negligible solubility in iron.[1] A number of alloying procedures were tried in the hope of producing an iron-carbon alloy of highest possible magnesium content. The methods employed were the following:

1. Addition of pure chunk magnesium to the molten iron-carbon alloy. The maximum magnesium content achieved by this method was 0.026 per cent in a 3.89 per cent carbon-iron alloy (Heat 16, Table I). The effect of metal temperature at the time of the addition was found to be insignificant since little difference in recovery was noted over the temperature range of 2310 to 3090 F.

2. Reduction of magnesium from the slag into the iron. Slag covers of magnesium salts such as $MgCl_2$, MgF_2, and mixtures with $BaCl_2$ were used for the purpose. A maximum magnesium content of 0.040 per cent in a 4.50 per cent carbon-iron alloy was obtained by the method.

3. Addition of powdered metal compacts to the molten iron-carbon alloys. Compacts of 100 mesh material containing 7.10 per cent Mg, the balance iron-carbon alloy (3.50 per cent carbon-iron alloy) were made under 80,000 psi pressure. The compact was added to the molten iron in the form of chunks that were forcibly submerged in the melt. This method yielded an alloy containing a maximum of 0.125 per cent Mg in a 4.22 per cent carbon-iron alloy. In view of the alloying difficulties this was thought to be a sufficient magnesium percentage to carry out the desired studies.

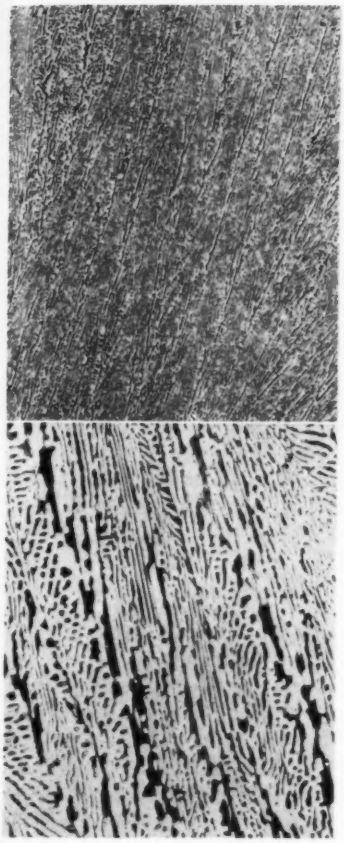
Procedures for preparing the alloys consisted of induction melting ingot iron in a magnesia or graphite crucible and raising its carbon content by the addition of powdered electrode graphite. Alloying with magnesium was performed after the iron-carbon alloy was prepared. By this means, the alloys in Table I were obtained. The molten iron-carbon-magnesium alloy was poured into core sand molds of two types,

TABLE 1—ANALYSIS OF FE-C-MG ALLOYS

Heat No.	Method of Magnesium Addition	Carbon, %	Magnesium, %
2	Chunk	2.92	nil
3	Chunk	2.94	trace
16	Chunk	3.89	0.026
17	Chunk	3.89	0.006
19	$MgCl_2$ Slag	4.34	0
20	$MgCl_2$ Slag	4.37	0
21	$MgCl_2$-$BaCl_2$ Slag	3.43	0.015
22	Sintered Compact	4.25	0.100
24	Unsintered Compact	4.28	0.013
25	Unsintered Compact	4.10	0.030
26	Unsintered Compact	4.22	0.125
27	MgF_2 Slag	4.50	0.040
39	Unsintered Compact	4.32	——*
40	Unsintered Compact	4.22	——*
43	Unsintered Compact	4.51	——*

NOTE: Analysis for magnesium was performed using the wet gravimetric method described in reference 2.
* Magnesium Analysis not performed.

*Instructor and Assistant Professor of Metallurgical Engineering, respectively, Department of Mining and Metallurgy, University of Wisconsin.



150X

250X

Fig. 1 — Heat No. 20 as-cast; 4.37 per cent carbon, 0.0 per cent magnesium. Etched, 2 per cent nital. The structure is the characteristic ledeburite eutectic of iron-carbon alloys.

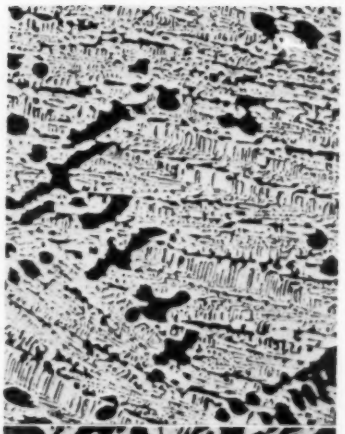


Fig. 2 — Heat No. 25 as-cast; 4.10 per cent carbon, 0.30 per cent magnesium. Etched, 2 per cent nital. 150X. Note coarsening of carbide and separation of carbide and austenite as compared with Fig. 1.

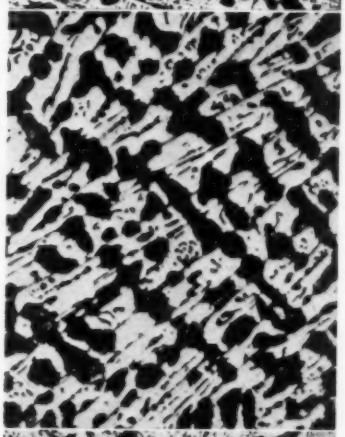


Fig. 3 — Heat No. 22 as-cast; 4.25 per cent carbon, 0.10 per cent magnesium. Etched, 2 per cent nital. 150X. Compare with Figs. 1 and 2.

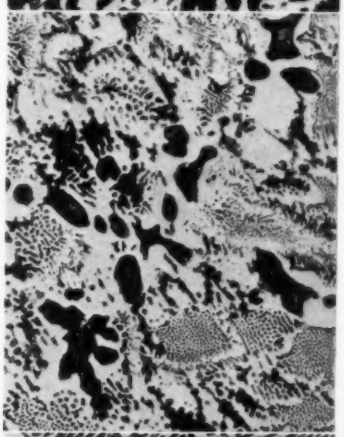


Fig. 4 — Heat No. 26 as-cast; 4.22 per cent carbon, 0.125 per cent magnesium. Etched, 2 per cent nital. 150X. Note massive carbides and unusual distribution of austenite (pearlite) and carbide.

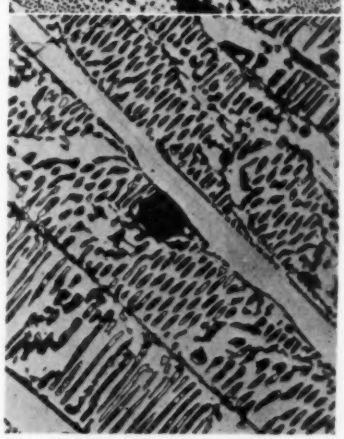


Fig. 5 — Heat No. 43 as-cast; 4.51 per cent carbon. Etched, 2 per cent nital. 150 X. Note modification of the eutectic structure and graphite spheroid. Compare with Figs. 1 to 4.

one being a round bar $1\frac{3}{16}$-in. in diameter by 3 in. long, and the other being a triangular cross-sectioned bar 3 in. long with one edge of the triangle being $1\frac{5}{16}$-in. These test bars were employed for the studies of as-cast microstructure and for graphitization by heat treatment.

As-Cast Microstructure

Since the alloys contained no silicon, they were white irons and their microstructure should be that anticipated from the Fe-C equilibrium diagram except where magnesium might exert an alloying influence. The microstructure of an iron-carbon alloy which is close to eutectic carbon content is shown in Fig. 1. The ledeburite eutectic occurs at 4.30 per cent C in the iron-carbon system, and its structure formed during solidification is well illustrated in Fig. 1. The eutectic consists of iron carbide and austenite precipitated in finely divided form during solidification. Upon cooling the austenite present transforms to pearlite and the final structure appears as in Fig. 1.

Magnesium was found to exert a profound effect on the appearance of the as-cast microstructure. The ledeburite eutectic was observed to change in distribution and fineness of the carbide phase as shown in Fig. 2 through 4. Increasing percentage of magnesium in the alloy caused the carbide and austenite in the eutectic to become much coarser and more segregated. The carbide phase showed a distinct tendency to agglomerate and be separated from the aus-

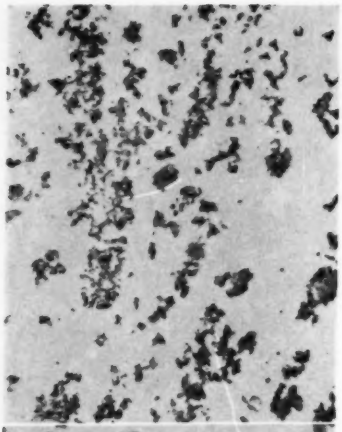


Fig. 6 — Heat No. 24 graphitized in the solid state; 4.28 per cent carbon, 0.013 per cent magnesium. Unetched. 150X. Note flake-like graphite and rough graphite-ferrite interface.



Fig. 7 — Heat No. 25 graphitized in the solid state; 4.10 per cent carbon, 0.03 per cent magnesium. Unetched. 150X. Note beginning of compaction of graphite and presence of some spheroids. Compare with Fig. 6.

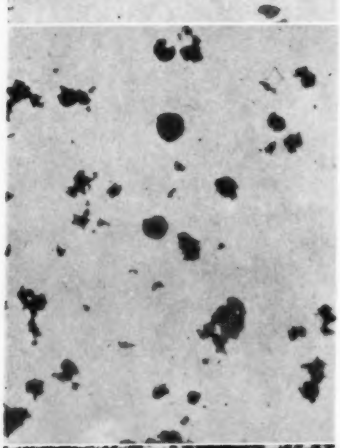


Fig. 8 — Heat No. 22 graphitized in the solid state; 4.25 per cent carbon, 0.10 per cent magnesium. Unetched. 150X. Much of the graphite present as spheroids with smooth graphite-ferrite interface. Compare with Figs. 6 and 7.



Fig. 9—Commercial spheroidal graphite iron; composition, per cent, 3.62 C, 2.65 Si, 0.018 S, 0.08 P, 0.31 Mn, 0.061 Mg. Chilled portion of as-cast 0.50-in. diameter round bar. Etched, 2 per cent nital—5 per cent picral (2-1). 150X. Note massive carbides. Compare with Figs. 1 to 4.

tenite rather than occurring in finely divided form intermingled with the austenite. The change in the eutectic with increasing percentage of magnesium begins at contents as low as 0.013 per cent magnesium (Heat 24). Figure 2 shows the appearance of the eutectic with 0.030 per cent magnesium present (Heat 25). Note the coarsening of the microstructure evident in Fig. 2 as compared with Fig. 1. The most pronounced effects were obtained in the alloys containing 0.10 and 0.125 per cent magnesium as illustrated in Figs. 3 and 4. Comparison of Figs. 1 through 4 shows the marked change in fineness of the carbide phase which results in alloys of increasing magnesium content. Carbon content of these alloys is not a factor in the comparison since they are all at similar carbon levels. The maximum separation of carbide and austenite occurred in the 0.10 per cent Mg, 4.25 per cent C alloy (Fig. 3, Heat 22). A higher magnesium content alloy (0.125 per cent magnesium Heat 26) also showed marked segregation of carbide, but not to the same extent as in Heat 22. It may be that a partial reversal of the eutectic modification occurs at the highest magnesium contents.

The present data show that the solidification process of the iron-carbon eutectic is greatly modified by the presence of magnesium. Modification of eutectic freezing in binary alloy systems by very small percentages of certain elements is a well-known phenomenon. For example, the Al-Si eutectic may be modified by the addition of a small percentage of sodium to the molten alloy.[3] Thus, the influence of magnesium of modifying the iron-carbon eutectic appears to be an extension of the general metallurgical principle of binary eutectic modification caused by small percentages of certain elements.

Graphitization

Graphitization during solidification of the iron-carbon-magnesium alloys was not observed except in a hyper-eutectic alloy (Heat 43) containing 4.51 per cent carbon. The structure of this alloy is shown in Fig. 5. Eutectic modification is also evident in the microstructure shown in Fig. 5. Since no silicon was present in these alloys, graphitization during solidification was not expected. However, it was thought that heat treatments simulating malleabilizing treatment would cause graphitization to proceed in the solid state. Therefore, a series of samples were sealed in a gas-tight container to prevent scaling and were given a graphitizing heat treatment. The treatment consisted of heating to 1700 F in 2 hr, holding at 1700 F for 60 hr, cooling to 1325 F and holding for 40 hr, holding at 1300 F for 20 hr, and then furnace cooling to room temperature. The heat-treated samples were then examined metallographically. The samples containing magnesium were found to be completely graphitized, including all alloys from 0.013 per cent to 0.125 per cent magnesium. No carbides of any kind remained in the microstructure after the heat treatment. Only ferrite and graphite existed. However, in samples containing no magnesium, graphitization did not occur or even begin.

The nature of the graphite produced by heat treatment is shown in Figs. 6, 7, and 8. With increasing

percentage of magnesium the shape of the graphite spheroids is observed to change. At low magnesium content (0.013 per cent, Heat 24) the graphite spheroids have a loose flake-like appearance, as illustrated in Fig. 6. With increasing magnesium content (0.03 per cent magnesium, Heat 25; and 0.10 per cent magnesium, Heat 22) the graphite spheroids become more compact and, in fact, carbon spheroids were developed (Figs. 7 and 8). Thus, it is seen that the shape of the graphite particles developed by heat treating in the solid state may vary from flake-like groupings to compact spheroidal shaped particles as the magnesium content of the alloys increases in the range of 0.01 to 0.10 per cent magnesium. This effect appears to have some use in the malleable iron field where an agent which would cause the most compact spheroids to develop during malleabilizing heat treatment should serve a useful purpose in obtaining the best properties from the iron. It is also evident that the shape of the graphite spheroid in irons may be varied in the solid state as much as in the liquid state by the presence of appropriate elements.

The changes in graphite type appear to occur over the same magnesium content range (0.01-0.10 per cent), which causes modification of the eutectic. This effect also seems to exist to a degree in commercial spheroidal graphite irons. The microstructure of iron which froze largely white in a 0.50-in. diameter section is shown in Fig. 9. Here, the absence of eutectic areas and the pronounced segregation of carbides seems to be similar to that obtained in the pure iron-carbon magnesium alloys. Modification of the eutectic and formation of spheroidal carbon thus seem to carry over into the commercial alloys.

Conclusion

The aim of this paper has been to show alloying effects of magnesium on the iron-carbon eutectic and the nature of graphitization in these alloys. Of course, much remains to be done but certain principles seem to be established. Conclusions which may be stated at this point include the following:

1. Magnesium acts on the solidifying ledeburite eutectic causing a coarser distribution of the austenite and carbide. This "agglomerating" action increases with increasing magnesium percentage.

2. The presence of silicon, manganese, phosphorus, sulphur, and other elements found in commercial spheroidal graphite iron is not essential to changing the freezing mechanism of the eutectic or in altering the shape of the graphite developed.

3. The graphite obtained by heat treatment of samples containing the lowest magnesium percentages is very irregularly shaped, not compact, has a rough interface with the ferrite matrix, and appears dendritically distributed. As the magnesium percentage increases, the graphite obtained by heat treatment becomes more nearly spherical in shape, more compact, less dendritically distributed, and has a smooth interface with the ferrite.

4. It is possible to produce spheroids of graphite in the solid state by heat treatment of an iron-carbon-magnesium alloy of suitable composition. Spheroids may also be produced in the as-cast condition provided the carbon content, magnesium content, and cooling rate are of suitable magnitude.

Acknowledgments

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PRELIMINARY SURVEY OF SOME METALLURGICAL BASES FOR A TITANIUM CASTINGS INDUSTRY

By

R. F. Malone,* H. T. Clark,* S. V. Arnold** and W. L. Finlay*

ABSTRACT

Employing the previously-developed principle of "skull" melting for the contamination-free casting of titanium, an investigation was made of several key metallurgical considerations which appeared to be of controlling importance in the future of titanium castings. These were: did a non-contaminating mold material exist; could contamination-free and chemically homogeneous titanium alloy castings be made by a procedure offering the possibility of large scale-up; could non-porous, good-surfaced, accurately-dimensioned titanium castings be made; and, finally, would titanium castings have the promising mechanical properties of their wrought counterparts? Affirmative answers were obtained to each question. Accordingly it was concluded that, although commercial shaped titanium castings were at least several years away, a sound metallurgical basis existed on which a titanium castings industry could be built.

Introduction

The scope of this paper is a survey of the chemistry, structural soundness and mechanical properties of some right cylindrical castings of titanium and two of its alloys. The purpose of the survey was to secure some indication whether a good metallurgical basis for a future titanium casting industry existed.

Formidable equipment and economic barriers block the genesis and growth of such an industry. On these grounds, alone, commercial titanium shaped castings now appear to be at least several years distant. Before mounting an attack on the equipment and economic problems, therefore, it is pertinent to inquire into the metallurgical characteristics of titanium castings to determine whether they merit the additional extensive effort and expenditure needed to investigate these and other important aspects.

The foregoing introductory paragraphs indicate the embryonic status of the titanium casting art at the present time. When this project was started under Contract No. DA-19-059-ORD-188, no shaped titanium casting had ever been made. Hence the elementary considerations discussed in this paper were and still are quite pertinent.

Basic Problem

The casting of shapes which could be used without further fabrication was a major means whereby most metals made their first useful contribution to man. As in so many other instances, however, titanium is proving to be the exception. The reason, of course, is now very familiar. It is the fact that molten titanium cannot be melted without embrittling contamination in any crucible except one made of itself.

Since the casting of shapes requires that there be a molten mass of metal at least equal in volume to that of the cast shape and its sprues, gates and risers, the lack of any non-contaminating container for large quantities of molten titanium metal has been a serious deterrent to the development of the titanium casting field. Aggravating this basically difficult situation is the fact that all atmospheres embrittle titanium except the noble gases, and, of the latter, only helium and argon are commercially available.

Thus the great reactivity of titanium imposes some severe limitations on the titanium foundryman. To achieve strong, tough castings he must melt and cast in a closed system that rigorously excludes air, moisture, CO, CO_2 and, in fact, all but the noble gases; moreover he must not allow the molten metal to contact even the most stable refractories during melting. Indeed, such contact must be limited to the brief moment during pouring and prior to external solidification of the casting.

A promising solution to the major problem of devising equipment to melt and pour relatively large volumes of contamination-free titanium and titanium-base alloys has been experimentally worked out by Vordahl and Darby.[1]

This procedure is termed "skull" melting. It involves melting the center of the metal charge while always maintaining a relatively thick layer, or skull, of unmelted metal between the melt and the crucible. Contamination from the latter is thereby avoided. The thermal stirring inherent in this practice assures a chemically homogeneous melt all of which can be poured at one time.

These features differ from the cold-mold, arc-melting method of casting titanium wherein close control is necessary to maintain ingot homogeneity and wherein only the uppermost portion of the built-up ingot is molten at any one time.

Most of our castings to date have been in the 2 to

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** Watertown Arsenal Laboratory, Watertown, Mass.

25-lb range, but we have cast up to 130 lb of titanium alloy from a skull furnace. Moreover, we believe the skull principle can probably be scaled up to any desired size.

Objectives

Using a skull furnace of nominal 25-lb capacity and three representative titanium-base materials, the following key considerations in the future of titanium castings were investigated:

1. **Mold Material.** What mold material would not contaminate the titanium castings?

2. **Chemistry.** Could chemically homogeneous and contamination-free titanium alloy castings be made by a procedure which offered the possibility of making large castings?

3. **Structural Soundness.** Could non-porous, good-surfaced, accurately-dimension titanium castings be made?

4. **Mechanical Properties.** Based on its properties in the wrought condition, titanium had been nominated the Middleweight Champion of the Structural Metals. Would this championship caliber carry over to the cast condition?

Experimental Details

Types of Titanium-Base Materials Evaluated—The pattern of titanium-base compositions which has found general acceptance to date has been based entirely on the properties of those compositions in the wrought condition.

As in the case of other metals, however, it is to be expected that the optimum cast compositions will be different from the optimum wrought compositions, since freezing segregation, fluidity, shrinkage and other important factors involved in good castability are, with the exception of non-correctable freezing segregation, of little consequence in wrought compositions.

But, since this was a pioneering investigation, there were available no guideposts in the form of as-cast or as-cast and heat-treated properties of any titanium-base alloys. With no points of departure indicated, we selected the materials to be investigated on the basis of their crystal structures.

The Three Basic Titanium Alloy Types—The concept that all titanium-base materials fall into one of three broad crystal structure classifications is now a familiar one.[2,3] It therefore requires only brief mention here.

The three types of titanium-base materials and their general characteristics are:

A — Alpha (hexagonal-close-packed) crystal structure which possesses good all around performance notably good weldability, excellent combinations of strength and toughness at both low and high temperatures and good oxidation resistance. Aluminum is often a constituent of all-alpha compositions[4] and its presence imparts both lower density and lower cost to the alloy. To a minor degree, it tends to increase the elastic modulus also.

B — Beta (body-centered-cubic) crystal structure which is outstanding for excellent combinations of strength and ductility. However, beta alloys are vulnerable to contamination, their weldability has not been established and they are big consumers of strategic, high-density, alloying elements.

C—Combined Alpha and Beta which gives compromise performance between all-alpha and all-beta alloys. Unlike all-alpha and all-beta alloys, the combined alpha-beta compositions usually show marked heat treatment responses. This is advantageous when attempting to attain high strengths but is disadvantageous in many other situations, as for example in welding, where heat treatment response is probably one of the major explanations for the generally brittle welds obtained with combined alpha-beta alloys. On the other hand, these alloys are in general the easiest to manufacture in various mill product forms and this is doubtless one of the major reasons why they constitute the only titanium alloy type which has yet been commercialized.

For this pioneering investigation, it was decided to employ the following three materials for the reasons given below:

1. *Unalloyed titanium.* Determination of the castability and as-cast mechanical properties of unalloyed titanium were considered necessary for a base line against which to compare the properties of titanium-base alloys.

2. *An all-alpha alloy, Ti-7Al.* The good strength, toughness and weld ductility of all-alpha compositions indicate outstanding promise and for this reason a simple, binary all-alpha was included as the first all-alpha to be tested as a casting.

3. *An alpha-strengthened, alpha-beta alloy, Ti-4 Al-4Mn.* In its simplest form, alpha-beta alloys have the alloying elements concentrated chiefly in the beta phase and accordingly the latter is appreciably stronger than the alpha constituent. The titanium-base forging alloy which has achieved the greatest tonnage use to date is RC-130-B, nominally Ti-4Al-4Mn. In this composition, the aluminum concentrates in the alpha and the manganese in the beta so that both are strengthened and the result is a well-balanced forging alloy. Because of the relatively large background of experience in the wrought field with this composition it was considered desirable to include it in the preliminary survey of as-cast titanium properties.

Mold Materials

Molten titanium is so reactive that even the momentary contact prior to solidification in a mold suffices for chemical attack on many refractories. To separate promising from unpromising mold refractories, a simple test was devised in which a small skull furnace delivered 1 to 2 oz of molten titanium into refractory crucibles of less than 1 cu in. capacity. If a refractory material failed in the 2-oz test, it is believed that it would certainly fail with larger melts and correspondingly longer molten contact. On the other hand, if a refractory material functioned satisfactorily in the 2-oz test, this was taken as an indication only of possible future success.

Among the criteria of success which were applied to this survey were the following:

1. *Hardness of the cast material.* A relatively soft sponge lot with an average Vickers hardness of 150 in the as-cast condition was employed. Titanium of

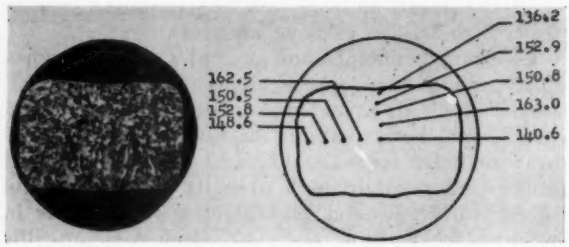


Fig. 1—Unalloyed titanium ingot cast into graphite mold shown before and after sectioning for a hardness survey. Mold was at room temperature, 1 atm pressure argon.

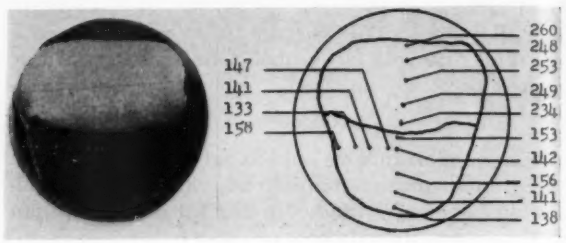


Fig. 2—Unalloyed titanium ingot cast into magnesia mold shown before and after sectioning for a hardness survey. Mold was at room temperature, 1 atm pressure argon.

this high purity is sensitive to small amounts of contamination, such as might be picked up by a slight amount of reaction with refractory material. Thus any hardness in the cast ingot averaging above 150 VHN would be indicative of reaction with the refractory.

2. *Meniscus shape.* Wetting of the refractory by molten titanium is probably an invariable prelude to reaction. Since a convex meniscus indicates nonwetting, the shape of the meniscus is indicative of the tendency towards reaction between the melt and the refractory material. If gases evolve during solidification after the top surface has frozen, the center of an otherwise concave meniscus may "cupcake" into a convex shape. Hence attention must be directed to the meniscus angle right at the container wall.

3. *Condition of the mold after the casting had been removed.* Any erosion, reaction product, loss of strength or other indication of reaction was considered to disqualify a candidate refractory material.

RESULTS OF SURVEY

Mold	R_A*	Wetability	Reaction	Notes
Graphite	41-43	None	None	Excellent
Carbon	42-45	None	None	Good
Alundum	80-85	Yes	Yes	Brittle, gassy, mold cracks easily, vigorous reaction.
MgO	45-60	Some		Same as alundum
Thorium Oxide	50 55	Some	Some	Fair to good—no mechanical strength
BeO	70-75	Some	Some	Some mold erosion—ingot appreciably harder than sponge but not readily friable
Zirconia	75-80	Yes	Yes	Brittle—reaction with mold approximately same order as alundum
TiC	50-60	Yes	Some	Same mold erosion
Steel Foundry Sand	90	Yes	Yes	Violent reaction, casting gassy, full of voids
Zirconium Silicate	80-85	Yes	Yes	Some reaction, gassy, mold erosion

*Rockwell A hardness taken on the cross section of an unalloyed titanium casting made in the mold indicated in the tabulation. Uncontaminated hardness of the unalloyed titanium was 41-45 R_A.

4. *Soundness of the casting.* As will be discussed later, when reaction occurs between molten titanium and the refractory, the latter is reduced. If the metal constituent of the refractory has an appreciable vapor pressure at the melting range of titanium alloys (as, for example, the Mg in MgO at 2500-3200 F), the metal vapor causes bubbles in the casting.

Since it was well known that titanium presented a difficult challange to any refractory material, rather severe criteria were applied as a basis for selection of refractory materials for tests. These included:

1. Commercial availability.
2. High melting point.
3. Low vapor pressure at the melting point of titanium.
4. Chemical stability as shown by free energy data.

The results of this survey are listed in accompanying tabulation and illustrative ingot cross-sections are shown in Figs. 2 and 3.

Figure 1 is of an unalloyed titanium ingot cast into a graphite mold. The etched cross section in Fig. 1 shows equiaxed grains throughout with sound metal right out to the surface of the ingot and with no evidence of reaction. The hardness survey of this cross section is also shown in Fig. 1. As indicated in this

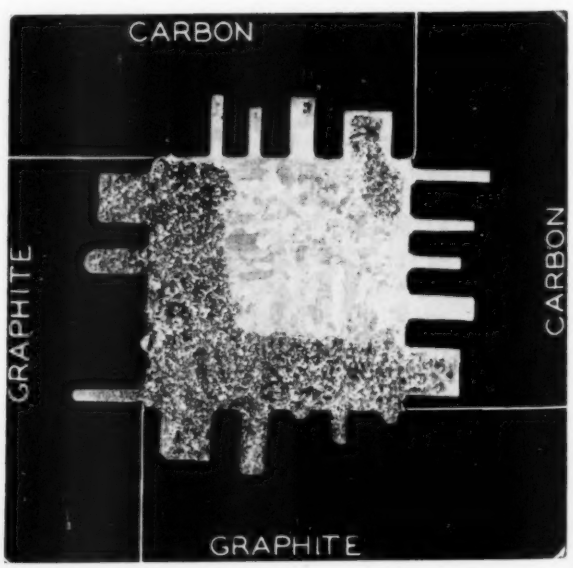


Fig. 3—Etched cross section of fluidity test ingot.

cross section, the hardness varied from about 135 to 165 VHN with a random distribution of high and low values. The as-cast hardness of 20-gram buttons of the same sponge lot melted on a water-cooled copper hearth averaged 150 VHN so it can safely be concluded that no significant amount of reaction occurred.

Figure 2 on the other hand shows a striking demonstration of borderline refractory stability under the test conditions. Figure 2 shows the etched cross section of an unalloyed titanium ingot cast into a magnesia mold. The lower half of this ingot is seen to have a metallic appearance and the hardness survey shows a satisfactorily low and random hardness distribution running from 135-160 VHN. The upper portion of this ingot, however, shows a frothy appearance in the etched cross section with hardness varying from 235 to 260.

Clearly what happened was that, when molten titanium was poured into the magnesia mold, the initial metal froze practically at once and so did not react with the magnesia. As additional molten titanium continued to pour into the mold, however, the mold became hotter and the momentary contact between molten titanium and the mold wall became longer. Finally, at a point about half-way up the mold, a rather violent reaction took place between the molten titanium and the mold, the titanium reducing the magnesium oxide by taking the oxygen into solution and boiling the magnesium off. The oxygen pick-up accounts for the increased hardness and the boiling off of the magnesium explains the frothy appearance on the top of the ingot.

From the foregoing it can be seen that carbon and graphite are quite promising mold materials for titanium. Although chemically alike, these materials have quite different thermal conductivities, graphite being up to 50 times more conductive than carbon. The effect of this difference is strikingly shown for a Ti-4Mn-4Al alloy cast into the special fluidity test mold shown in transverse section in Fig. 3.

This mold was preheated to 800 C (1472 F) prior to pouring. The illustration shows that the grain size adjacent to the lower-thermal-conductivity carbon grew much larger than that adjacent to the higher-thermal-conductivity graphite. Less striking but perhaps more important, the fluidity against carbon was much greater than against graphite as shown by the blunted and round-ended projections into the graphite relative to the full depth and square ends of the projections into the carbon.

In subsequent investigation of other factors, therefore, carbon or graphite molds were employed.

Chemical Composition as a Criterion of the Degrees of Contamination and Homogeneity — In the skull furnace employed in this investigation, the chief potential sources of contamination were the graphite crucible and the atmospheric gases: oxygen, nitrogen and water vapor. Oxygen content is difficult to determine in titanium, but fairly reliable and convenient analyses can be made for carbon and nitrogen. Chemical analyses for carbon and nitrogen and, where present, metallic alloying elements therefore permit a good appraisal of the degree of contamination and chemical homogeneity which exist in a particular casting.

Figure 4 shows the etched cross section of one of the unalloyed titanium ingots cast early in the program. The "rim" porosity and its elimination will be discussed later.

This ingot weighed 24 lb as cast. Tensile and impact specimens were cut from four regions of the sectioned ingot and machined chips from these specimens were analyzed. These four regions were: sound metal at the center; sound metal near the center; porous metal in the rim; and relatively sound metal near the edge.

The sponge from which the ingot shown in Fig. 4 was made analyzed 0.02 per cent carbon and 0.02 per cent nitrogen. The chips from the four different regions all analyzed within 0.03-0.04 per cent carbon and 0.02-0.03 per cent nitrogen. Below 0.10 per cent carbon, carbon determinations in titanium are not more reliable than ± 0.02 per cent; nitrogen contents in the same range are reliable within perhaps ± 0.005 per cent. It appears therefore that the ingot shown in Fig. 4 had been melted, poured and solidified in an essentially contamination-free manner.

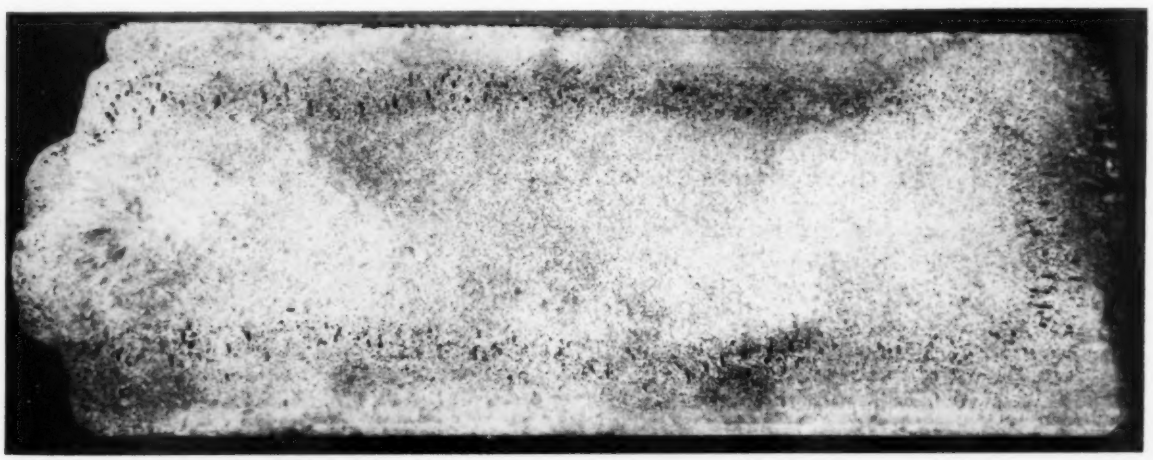


Fig. 4—Etched cross section of unalloyed titanium casting cast under reduced pressure.

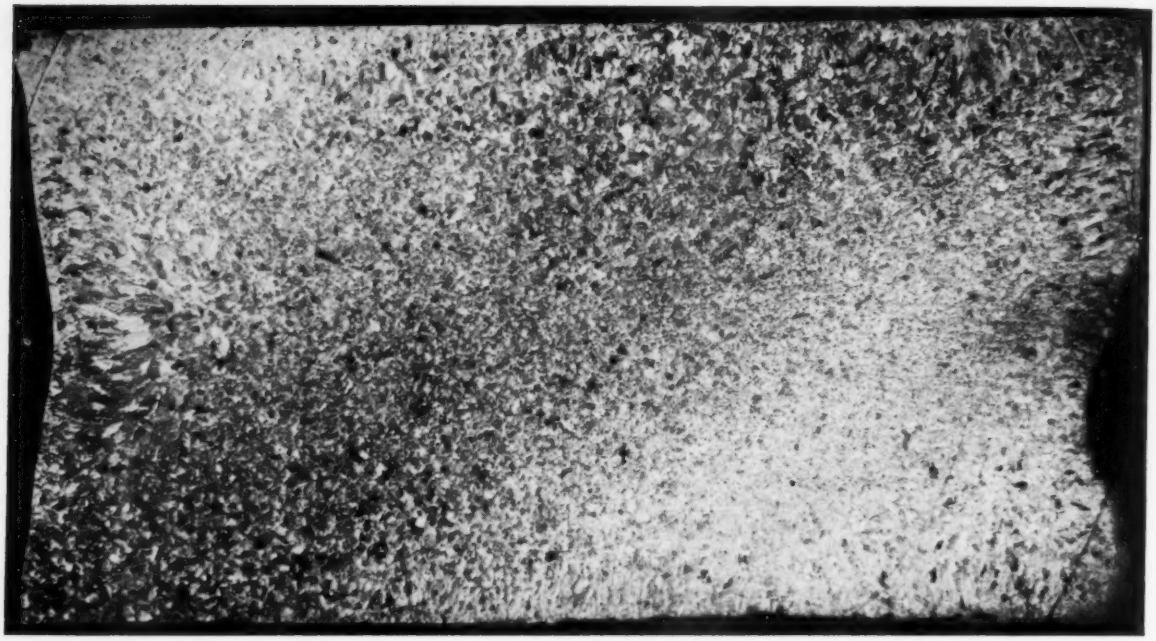


Fig. 5—Etched cross section of sound unalloyed titanium casting cast under atmosphere pressure of argon.

A similar cylindrical ingot, but containing aluminum and manganese as deliberate alloy additions, was made from the same sponge lot. It was sampled by turning off chips at several locations on the cylindrical surface. Results of this analysis were as follows:

Sample Location	C	N	Fe	Al	Mn
At top	0.04	0.014	0.20	2.35	3.35
1¾ in. below top	0.04	0.010	0.19	2.34	3.40
3¾ in. below top	0.05	0.012	0.20	2.43	3.45
7¼ in. below top	0.05	0.011	0.19	2.34	3.45
8¼ in. below top	0.05	0.013	0.24	2.56	3.55

From these analytical results it is concluded that skull melting and pouring can yield contamination-free and chemically homogeneous castings.

Structural Soundness

Porosity—Castings of both unalloyed titanium and the titanium alloys investigated during this program may evolve gases during solidification and these may be entrapped within the casting.

This evolution of gas is probably the result of reduced solubility of hydrogen with solidification.

The initial skull furnace operating technique in this investigation was to melt and cast under reduced pressure of argon into a right cylinder graphite mold. This graphite mold rested on a water-cooled copper pedestal. Because of this and the high thermal conductivity of graphite, the bottom of the casting freezes first and solidification commonly progresses inward from other surfaces also and this resulted in the gas entrapment shown in Fig. 4. Casting under atmospheric pressure of argon effected the improvement shown in Fig. 5. The casting is sound throughout except for some fine porosity in the top central region.

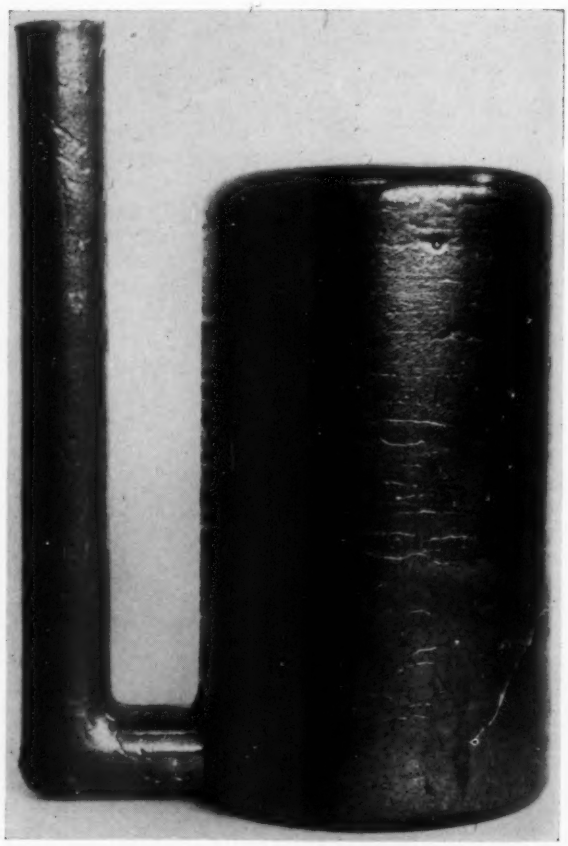


Fig. 6—Bottom-gated Ti-4Al-4Mn alloy casting. Graphite mold at room temperature. High superheat.

Similar sound ingots have been obtained for the two alloys involved in this study. It may be concluded that non-porous titanium castings can be made.

Surface—Because it is difficult to impart appreciable superheat to the bath in a skull melting practice, surface defects such as cold shuts and frozen splatter are commonly found on the casting. Pouring metal near the melting point directly into a cold graphite mold, for example, results in a poor surface. Bottom gating, mold preheating, superheating, using low thermal conductivity molds, etc. all tend to improve surface conditions.

Good surfaces have been obtained with various combinations of these expedients. Figure 6, for example, shows a 16½-lb, 4-in.-diam, right cylindrical casting made by pouring superheated Ti-4Mn-4Al into a cold graphite mold through a bottom gate.

Figure 7 shows a shaped titanium casting of the same composition and cast under the same conditions as that illustrated in Fig. 6. This casting weighs 4 lb and its central portion is 6¾ in. in diam and 1 in. thick. Its surface is considered quite acceptable.

Shrinkage — In order to assess titanium's general shrinkage characteristics, both diameter and length measurements were made on a series of unalloyed right cylindrical castings with diameters varying from 2 in. to 5 in. Results are given in the following table:

Composition	Diameter or Length, in.	Direction	Contraction in./in.
Unalloyed Ti	2	radial	0.0200
	3¼	radial	0.0190
	4	radial	0.0185
	5	radial	0.0165
	9½	length	0.0141
	11½	length	0.0126

It can be seen that, as in the case of other metals, titanium shrinks less, the greater the section thickness; also, that the shrinkage of titanium is comparable to that of cast steel, is greater than that of gray cast iron and aluminum and is less than that of magnesium.

Mechanical Properties

Tensile and V-notch Charpy impact specimens were machined from representative locations in many of the castings. In general they were tested in the as-cast condition.

Unalloyed Titanium—Specimens were taken from the following four regions of the longitudinal section of the 24-lb casting shown in Fig. 4:

a from sound metal near the center
b from sound metal at the center
c from the porous area
d from the edge of the porous area.

Results of the mechanical tests were:

Direction of test	0.2% Yield, 1000s psi	Tensile, 1000s psi	Elong., %	RA, %	Impact, ft-lb
Long. *a*	47.5	59.2	10	7	25.8
Long. *b*	53.6	64.7	16	21	22.0
Long. *c*	42.5	49.9	5	5	23.2
Long *d*	47.2	56.7	5	13	24.0
Trans.	47.5	64.3	16	24	20.0
Trans.					22.5

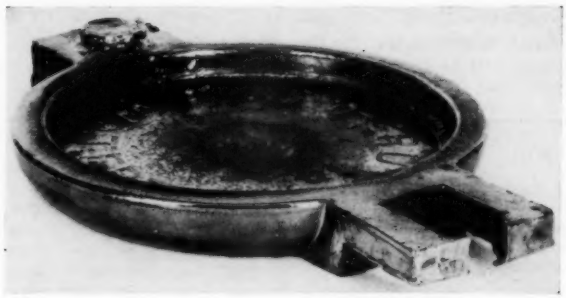


Fig. 7—Photograph of a shaped titanium casting 6¾ in. in diam and 4 lb in weight.

Examination of the foregoing table shows that the tensile coupons taken from sound metal near the center of the casting have better tensile elongation than those taken near or in the porous zone. But the impact values of all coupons are good, including those which were cut from porous areas. The explanation for this is probably that the porosity does not materially reduce the cross-sectional area, and the pores, being generally rounded in contour, do not act as stress raisers.

Specimens cut from the sound ingot shown in Fig. 5 gave the following properties:

Direction of test	0.2% Yield, 1000s psi	Tensile, 1000s psi	Elong., %	RA, %	Impact, ft-lb
Long.	60.3	72.3	19.0	25.5	36.0
	56.0	66.8	10.0	17.5	30.7
	58.0	69.5	15.0	23.0	27.0
	56.9	69.5	17.0	21.8	31.5
	56.7	71.5	21.0	24.6	32.7
Trans.	51.1	70.8	22.0	37.5	32.1
	57.4	71.1	20.0	28.1	36.1
	—	—	—	—	16.0
	—	—	—	—	33.5

The foregoing elongation and impact values are quite respectable at the indicated strength level even for the best wrought titanium.

Ti-7Al—The as-cast properties of a 25-lb, right cylindrical casting containing 0.05C, 0.015N and 6.85Al were found to be:

Direction of test	0.2% Yield, 1000s psi	Tensile, 1000s psi	Elong., %	RA, %	Impact, ft-lb
Long.	95.7	109.2	9	12	19.0
Long.	86.9	96.9	13	22	24.0
Long.	94.2	108.5	14	16	—
Trans.	94.7	108.7	15	22	30.2
Trans.	92.8	105.5	10	19	16.4
Trans.	95.0	105.0	6	12	18.2

These properties, particularly the V-notch Charpy impact, are considered to be quite creditable.

Ti-4Mn-4Al—The as-cast properties of a 16½-lb right cylindrical casting and of two 4-lb shaped castings of the nominal Ti-4Mn-4Al analysis were determined by the Watertown Arsenal Laboratory.

A photograph of the right cylindrical casting is shown in Fig. 6. A considerable number of tensile and impact specimens were cut from this casting. Specimens oriented parallel to the axis of the cylinder

were considered "longitudinal." Those disposed in a plane transversely to the axis were termed "transverse." The tensile results were:

TENSILE PROPERTIES

Direction of test	0.2% Yield, 1000s psi	Tensile, 1000s psi	Elong., %	RA, %
Longitudinal	146	155	6.0	10.9
	139	148	6.0	10.4
	139	151	6.0	10.8
	142	152	7.0	11.0
	144	154	6.0	12.6
	137	146	4.0	5.7
	145	152	5.0	9.6
	147	154	5.0	9.2
	142	147	3.0	2.6
	133	144	4.0	6.4
	133	143	3.0	5.7
	144	154	8.0	14.7
Transverse	130	140	4.0	4.2
	132	140	4.0	7.2
	146	154	6.0	8.0
	138	147	6.0	9.6
	138	145	3.0	8.8
	144	154	7.0	11.8

These tensile properties are somewhat higher in strength and lower in ductility than would have been anticipated for a wrought product of this composition. The variation in ductility is believed to be associated with porosity in the casting. In view of these and other results, it may well prove that, although Ti-4Mn-4Al is a useful composition for wrought products, it is not particularly well adapted to developing good as-cast mechanical properties. As in virtually every other aspect of titanium casting, much work remains to be done in this field.

The impact results were:

V-NOTCH CHARPY IMPACT TEST RESULTS

Direction of Test	Testing Temperature, C	Foot Pounds
Longitudinal	— 40 C (—104 F)	4.5
"	— 40	2.8
"	— 40	4.1
"	— 10 (—50 F)	4.7
"	— 10 C	5.1
"	— 10	4.9
"	23 (73 F)	6.0
"	23	6.7
"	23	5.7
"	50 (122 F)	8.0
"	80 (172 F)	12.4
"	100 (212 F)	12.1
Transverse	— 40 (—104 F)	3.7
"	— 40	2.6
"	— 40	3.0
"	— 40	1.5
"	23 (73 F)	8.0
"	23	7.0
"	23	5.8
"	23	6.8
"	100 (212 F)	13.3
"	150 (302 F)	23.6
"	200 (392 F)	24.7
"	250 (482 F)	31.8
"	300 (572 F)	29.9
"	350 (662 F)	38.2
"	400 (752 F)	28.6

* Although most fractures exhibited numerous very small pores, asterisked specimens possessed larger cavities considered likely to have affected appreciably the recorded value.

Both tensile and Charpy specimens were selected in various positions, directions and notch orientations within the ingot in an effort to determine whether these factors would influence test performance. No such influence was noted, indicating that, aside from porosity variations, the metal was quite uniform in behavior.

Two castings of this same composition and shaped as shown in Fig. 7 were also provided to Watertown Arsenal for examination.

In the early state of the art represented by these castings, numerous large voids existed in some portions. Samples were machined from sound regions for tensile and impact testing. The following results were obtained:

TENSILE PROPERTIES

	0.2% Yield, 1000s psi	Tensile, 1000s psi	Elong., %	RA, %
Cover Plate No. 1	136	146	6.0	10.4
	140	150	6.0	10.4
Cover Plate No. 2	120	135	10.0	19.2
	116	126	3.0	4.1
	117	132	5.0	10.4

Again, these properties are not quite up to those of the standard wrought product with this analysis although the tensile properties of cover plate No. 2 show, in one instance, promising strength and ductility. The lower ductility of the other specimens from this same plate is perhaps attributable to porosity. The impact toughness of these shaped castings is indicated by the following:

V-NOTCH CHARPY IMPACT TEST RESULTS

	Testing Temperature, C	Foot Pounds
Cover Plate No. 1	— 40 C (—104 F)	4.0
"	— 40	4.0
"	— 40	5.0
"	23 (73 F)	7.8
"	23	6.8
"	23	7.1
"	23	7.2
Cover Plate No. 2	— 40 C	10.0
"	— 40	10.0
"	— 40	11.0
"	— 40	11.0
"	— 40	9.0
"	23	14.8
"	23	14.2
"	23	12.2
"	23	13.5

The superior properties of cover plate No. 2 as suggested by the tensile data are borne out by the impact test results which disclose an appreciable order of toughness even at the —40 C (—104 F) testing temperature.

Summary and Conclusions

Sound, strong, tough, good-surfaced and accurately-dimensioned titanium and titanium alloy castings were made on a laboratory scale up to 25 lb in weight. The procedure employed of melting and pouring from a skull furnace into a carbon or graphite mold within an argon atmosphere appears capable of some scale-up. The basically good castability of titanium is therefore indicated.

Many formidable technical and economic problems in the casting of titanium have not yet been formulated, much less attacked. Whether these can be satisfactorily solved is a question which can only be resolved in the future by a large scale effort by many people over a considerable period of time. At best, commercial shaped titanium castings appear to be at least several years away but it is encouraging to report as a result of this investigation that there appears to be a sound metallurgical basis on which a titanium castings industry can be built.

Acknowledgments

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DISCUSSION

Chairman: W. A. DEAN, Aluminum Company of America, Cleveland.

Co-Chairman: R. A. LUBKER, Armour Research Foundation, Chicago.

Recorder: P. D. FROST, Battelle Memorial Institute, Columbus.

R. M. LANG *(Written Discussion):*[1] The data on the mechanical properties of cast titanium which have been presented by the authors are a valuable contribution to those working on the problem of casting titanium. This paper answers many questions which have arisen about the feasibility of casting titanium.

The results the authors obtained when casting 2 oz of titanium into oxide molds are somewhat different from the results we obtained in our experiments. We have made 2-lb titanium castings in unwashed shell molds composed of MgO, Al_2O_3, or ZrO_2 (all electrically fused), and only the outer skin of the castings was contaminated. Pinholes in these castings were less than 1/8 in. deep, and the interiors of the castings were as sound as those made in graphite molds.

Our experience with graphite molds has been that it is difficult to obtain a smooth surface on castings made in cold graphite molds. The graphite chills the casting so rapidly that there are numerous folds on the surface of the casting similar to the one on the cylinder shown in Fig. 6 of the paper. Has any work been done to determine the temperature to which graphite molds must be preheated in order to eliminate folds in the casting?

In our experience with titanium alloys, a Ti-7Mn alloy appeared to be more reactive than unalloyed titanium. A Ti-7Mn melt eroded a graphite crucible much more rapidly than unalloyed titanium melts. This Ti-7Mn melt also reacted violently when cast in a graphite-washed zirconia shell mold. Have the authors found that other alloys such as Ti-4Al-4Mn behave in a similar manner?

The data on solid shrinkage of unalloyed and alloyed titanium castings are very helpful. Have the authors done any work on solidification shrinkage of titanium alloys? These data would be of help in designing the gates and risers for titanium-alloy castings. Also, are the titanium alloys more fluid than unalloyed titanium, or can the same gating systems be used for both?

More information on the design and operation of the skull-melting furnace would be of considerable help to those now working on titanium casting or intending to do so. It is hoped that, in the near future, the authors will find time to write a paper giving more of the details of their furnaces.

CHAIRMAN DEAN: Have the authors made a study of the economics of a casting industry?

DR. CLARK: We have done a little preliminary work in this field. Our chief problem has been, "is it possible to have a casting industry?", rather than "where are the applications?". The economics of such an industry will require years of study.

CO-CHAIRMAN LUBKER: Has anyone given any attention to the melting of titanium scrap?

DR. CLARK: Yes. It will be necessary to use better scrap-handling methods for titanium than for other metals because of the irreversibility of the oxidation reaction. Scrap definitely will be used in casting production, and this study is under way.

O. W. SIMMONS:[2] Will the authors comment on the extremely large grain size of the casting made in the carbon mold, as compared with the normal grain size obtained when casting against graphite?

MR. MALONE: The heat conductivity of the carbon was only about 1/50th that of the graphite.

MR. SIMMONS: Can you reduce grain size by heat treatment or other ways?

MR. MALONE: I know of no heat treatment to reduce grain size. It can be reduced by mold practice.

DR. CLARK: In titanium, beta grains are transformed into fine alpha grains during cooling. If reheated, the alpha grains again form the coarse beta grains. Only mechanical work can reduce the beta grain size.

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[1] Battelle Memorial Institute, Columbus, Ohio.
[2] Rem-Cru Titanium, Inc., Midland, Pa.

FUNDAMENTALS OF RISER BEHAVIOR

By

Clyde M. Adams, Jr. and Howard F. Taylor.*

ABSTRACT

A quantitative approach to riser design is presented which makes use of a heat and material balance to evolve an engineering riser equation. Modifying effects of factors such as chills, insulators, radiation, and superheat are evaluated. Quantitative comparison with the results of Caine[2] shows good agreement between theory and practice for making castings of steel; this paper supplements Caine's work for steel and presents a "riser equation" applicable for risering castings of any metal under average conditions.

Introduction

Use of risers or reservoirs of liquid metal to feed solidifying castings is the most commonplace foundry procedure. Shrinkage and risering are fundamental aspects of casting. The purpose of this paper is to delineate and simplify some of the basic characteristics of riser behavior; it is aimed at furthering understanding of how a riser does its job, and how risering fits into the broad relationship between heat transfer and solidification. Other objects of the report are (1) development of an uncomplicated quantitative approach to risering castings, and (2) verification of Caine's[2] work on riser dimensioning presented several years ago.

For the purpose of complete documentation, the mathematical reasoning has been outlined in an Appendix. However, all the practical consequences of the analysis are included in the paper proper, and require little computational effort to be applied directly to the solution of foundry problems. A certain, inescapable amount of measurement will, of course, be required for *any* successful risering scheme; specifically, to take advantage of the relationships found in this paper, one need know only the *weight* (or *volume*) and *surface area* of a casting, in order to design an efficient riser. This was also the requirement of the Caine analysis.

Any analytical approach to the problem of dimensioning risers will necessarily involve considerations of heat flow. The heat flow problems associated with solidification may become quite complex, but, in the case of many sand castings, simplifying approximations may be made with little loss of accuracy. When such approximations are made, the well-known Chvorinov's Rule[1] may be derived, which relates solidification time, "θ", to the volume, "V", and area, "A", of the casting:

$$\theta = N\left(\frac{V}{A}\right)^2$$

N = constant, different for each combination of metal and mold-material. Since most analytical treatments of the risering problem depend upon the general validity of Chvorinov's Rule, it is well to understand the meaning of the simplifications involved. Chvorinov's Rule depends on three conditions:

(1) The metal is a much better conductor of heat than the mold. (This condition is met in a sand casting.)

(2) Changes in section size of the casting are not great.

(3) Heat penetrates only a shallow layer of the mold surface during freezing. (This condition is fairly valid for sand castings.) These three conditions are all aimed at making every square inch of mold surface area remove the same amount of heat from a given casting. This is the basic tenet of Chvorinov's Rule. The meaning of condition (3) is illustrated in Fig. 1. Concave mold surfaces can absorb heat more rapidly than convex surfaces, due to differences in the degree of restriction imposed on heat flow by geometry. If the heated layer of sand is shallow, the effects illustrated in Fig. 1 are slight.

Probably the most complete and useful correlation of risering information to date has been that of Caine,[2] just mentioned. Caine has presented curves, based on observations of a large number of variously shaped steel castings, using all types and sizes of risers. Having been experimentally determined, Caine's curves do not depend upon the validity of Chvorinov's Rule, nor indeed do they need any theoretical support. However, Caine's choice of variables for plotting the curves, in terms of volumes and surface areas, was clearly suggested by the Chvorinov relationship.[1] Caine's astute choice of variables lends great value to his work, since many observations

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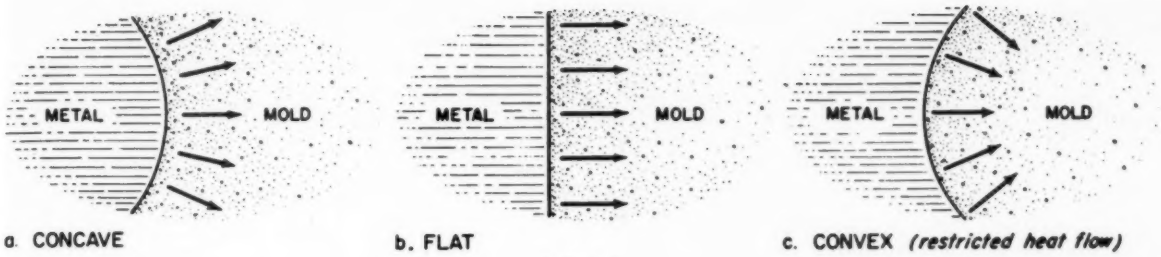


Figure 1

could be used in establishing a single curve. The theoretical treatment contained in this paper draws support from comparison with his experimental findings.

More than one theory has been advanced on the subject of dimensioning risers, but most of these have not been subjected to direct experimental verification. The most recent analytical study has been presented by Abcouwer.[3] Abcouwer's equations essentially conform with Chvorinov's Rule, but seem unnecessarily complex, with some of the mathematical reasoning open to question. The Abcouwer analysis further neglects the fact that shrinkage accompanies freezing in the riser as well as in the casting.

The Base Riser Equation

To analyze a problem basically, a model is usually taken as a starting point. The simplest possible riser-casting model (from an analytical point of view) is used herein to develop the base "skeleton" riser equation. The various modifications of the model, to be used in making conditions more realistic, can best be discussed in the light of this base equation. The limitations imposed upon the base equation are as follows:

(1) Pure metal.
(2) Zero superheat.
(3) Riser and casting both completely surrounded by sands. (No chills, insulators, or radiation losses.)
(4) Chvorinov's Rule is accurate.
  (a) No contour effect (Fig. 1).
  (b) No large change in section.
  (c) Metal conductivity is high compared to sand.
(5) No Safety Factor. The riser is exhausted of liquid metal the instant the casting freezes.

Within these limitations, using the analysis outlined in Part I of the Appendix, the following equation may be derived:

$$(1 - \beta)\frac{V_r}{V_c} = \frac{A_r}{A_c} + \beta$$

$$\beta = \frac{\text{per cent shrinkage}}{100}$$

V_r, V_c, A_r, A_c = volumes and areas of riser and casting respectively.

Comparison with Caine's Curve

The unmodified base equation may be compared with the curve in the Caine report (Steel Founders' Society of America, Research Report No. 13, Fig. 42) pertaining to *top-blind risers* on *steel castings*. The reason for making the comparison with blind-risered castings is to conform with limitation (3)—a riser completely surrounded by sand. This does not limit the analysis in any way to problems of blind risering, but a direct comparison with experimental results using open risers is less convenient since radiation losses must then be computed.

The Caine curve is replotted in Fig. 2, using somewhat simpler coordinates than in the original report. Good agreement is obtained with the theoretical straight line relationship (the base equation) shown on the same Figure. The same two curves (Caine's and the base equation) are plotted again in Fig. 3, using different coordinates. Figure 2 is more accurate for use in risering castings having large $\frac{V_c}{A_c}$ values (i.e. chunky castings), and Fig. 3 is more accurate for castings having low $\frac{V_c}{A_c}$ values (plate-like castings). Both Figures may be regarded as maps, with the curves separating regions of shrinkage from regions of soundness.

Several points may be observed in comparing the curves:

(1) Agreement is excellent for castings having less than 75 per cent yield.
(2) The theoretical curve is more optimistic than

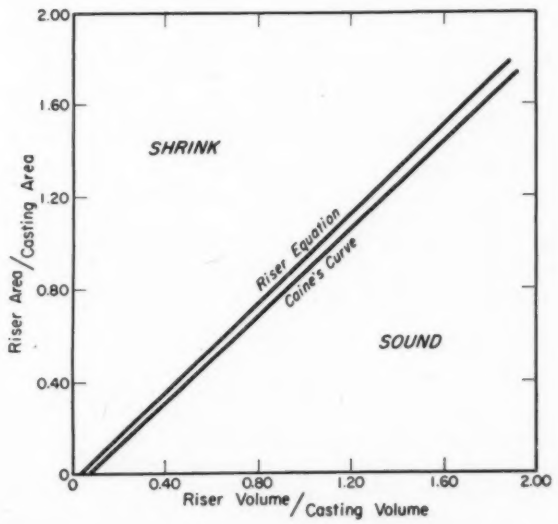


Fig. 2—Steel castings fed with top blind risers; 3 pct solidification shrinkage.

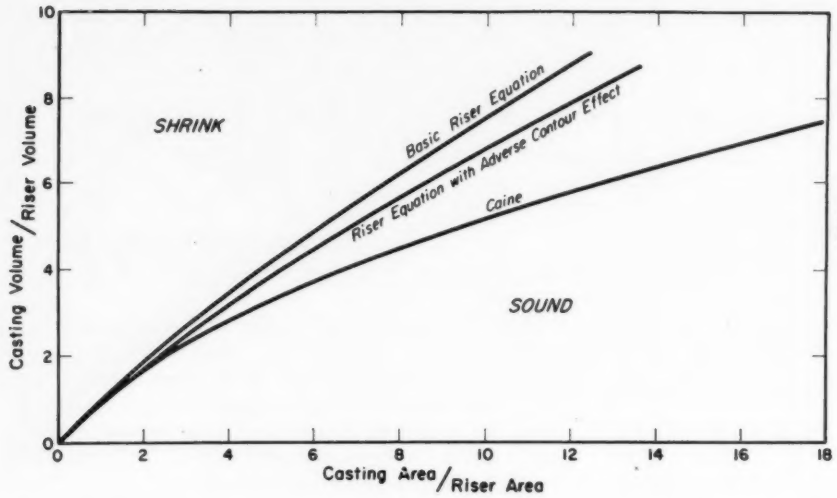


Fig. 3—Steel castings fed with top blind risers; 3 pct solidification shrinkage.

Caine's, particularly for plate-like castings (right-hand portion of Fig. 3). The principal reason for the apparent disparity between theory and practice in feeding requirements of plate-like castings stems from the gating system. Study of the original report by Caine indicates that the gates were sometimes larger in cross section than the castings, and that they were always large enough to be considered significant parts of the casting. Gates, too, require feed metal during freezing, and thereby reduce riser efficiency.

Effects of Metal Exposed to Materials Other Than Sand (Insulators, Chills, and Radiation)

Frequently, certain areas of the casting or of the riser are exposed to chills or insulators, or are open to the surroundings. The general technique for handling such a condition is to make use of a correction factor, "*f*", which may be described best by means of an example:

Steel or cast iron chills are used extensively on steel castings. One square inch of chill can remove 2.2 times as much heat from the casting as a square inch of sand, in a given period of time. Thus, in using the riser equation, the chilled area is multiplied by 2.2, and added to the sand area of the casting to give the total equivalent casting area, "A_c", which may be used in base equation. In this case, $f = 2.2$.

1. **Insulators**—The correction factor, "*f*", for an insulated area is less than 1.0. The correction factor for gypsum used in conjunction with sand-cast bronze is about 0.6, for example. These correction factors are best determined experimentally, but if thermal data are available, the following expression may be used:

$$f = \sqrt{\frac{K_i \rho_i Cp_i}{K_s \rho_s Cp_s}},$$

where K_i = thermal conductivity of insulator,
K_s = thermal conductivity of sand,
ρ_i = density of insulator,
ρ_s = density of sand,
Cp_i = specific heat of insulator, and
Cp_s = specific heat of sand.

To determine "*f*" experimentally, a simple test casting may be made in a mold consisting entirely of the unknown, and the freezing time determined by means of a thermocouple located in the casting. This test may then be duplicated in sand. Using Chvorinov's Rule,

$$f = \sqrt{\frac{\theta_s}{\theta_i}},$$

where θ_s = freezing time in sand, and
θ_i = freezing time in insulator.

2. **Chills**—The correction factor for a chilled area is greater than 1.0. The correction factor for a chill may not be easily determined from the thermal properties, principally because little is known of the chill-metal contact resistance; also, the mathematics is somewhat involved.

The experimental determination of chill-correction factors is the same as for insulators. Bishop, Brandt, and Pellini have presented data carefully obtained which indicates that freezing times of steel in chill (gray iron) molds, and in sand, are in the ratio of 1 to 4.8, respectively.[4] Thus the correction factor,

$$f = \sqrt{4.8} = 2.2.$$

3. **Radiation**—The correction factor method may be equally well applied to the exposed surface area if an open riser. There is a slight additional compli-



Fig. 4—Effect of superheat on top losses.

cation in that the correction factor is not a constant for a given system, e.g., steel and sand. A previous publication has demonstrated that there is a size effect on radiation losses; large, exposed risers lose relatively more heat through the top than do small risers.[6] Thus the correction factor, "*f*", increases with increasing riser size. This may be conveniently handled (APPENDIX, Part IV) by relating "*f*" to the size of the casting. The result is:

$$f = B \frac{V_c}{A_c},$$

where "B" is a true constant for a given system. For the system: steel and sand,

$$B = 1.25 \text{ in.}^{-1} \text{ (Approx.)}.$$

Experimental determinations of "B" are best made from measurements of pipe depths in a few simple castings. Figure 5 illustrates the results of this type determination on sand castings of tin.

Use of the Factor "f"

To summarize, in the case of an area chilled, insulated or exposed to air, the actual contact area in question is multiplied by a correction factor "*f*", which reduces the area to an equivalent sand-contact-area. Complicated cases may thus be reduced to simple applications of the base riser equation.

Deviation from Chvorinov's Rule

The middle curve in Fig. 3 illustrates one modification of the basic equation. An adverse contour effect has been included in the calculation. A typical adverse contour effect exists, for example, when a cylindrical riser is used to feed a plate-like casting. Heat flow is less restricted in the concave riser wall (Fig. 1a) than in the flat mold wall (Fig. 1b); this exemplifies a true deviation from Chvorinov's Rule. Plates fed by cylinders are described by the third curve in Fig. 3. The curve is slightly less optimistic than the base equation, but the change is not great. On the coordinates of Fig. 2, the third curve would nearly coincide with the curve for the base equation, suggesting that contour effects are not important on "chunky" castings. The analytical method for handling the effect of contour is outlined in Part II of the Appendix.

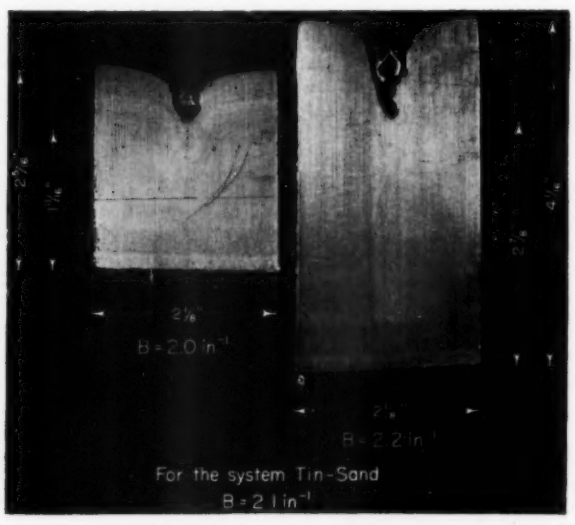


Fig. 5—Measurement of top loss "B" values from casting and pipe dimensions.

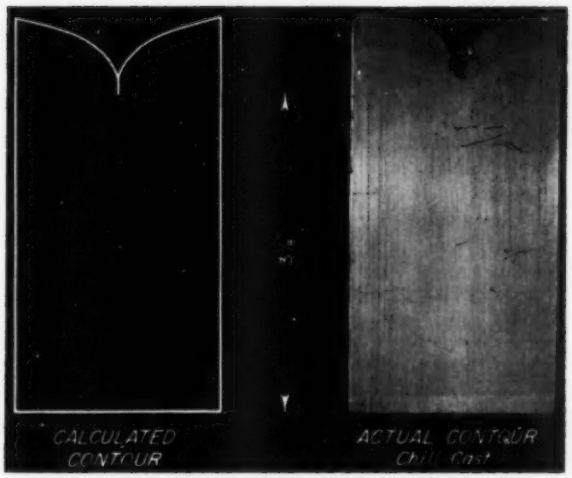


Fig. 6—Pipe formation in tin cylinder.

The Effect of Superheat

Foundrymen have always disputed the influence of superheat in feeding castings. Is superheat good or bad? Calculation indicates there is room for dispute. Superheat may be good *or* bad, depending upon other conditions.

(a) If there is no contour effect, and the other limitations involved in the base equation are met, superheat has a *very slight* beneficial effect.

(b) With the addition of a contour effect to (a), the problem becomes complex, but superheat still has only a slight effect, either beneficial or deleterious.

(c) Last, and most important, if an *unprotected open riser* is employed, superheat is profoundly harmful. Superheat has much the same effect as preheating the mold, and so increases the freezing time. Radiation or convection losses from the top of the riser are seriously increased. Figure 4 illustrates this behavior for simple castings of tin.

Analysis of the effect of superheat is outlined in Part III of the Appendix.

Applicability of the Equation

The equation seems well verified by the work of Caine[2] for the case of casting steel. Limited investigations using aluminum and tin show promising results. One hoped-for objective of this presentation is the extension of the Caine analysis to metals other than steel.

In using the equation, second-order effects such as superheat and contour may be neglected for most engineering purposes. (The combination of radiation from an unprotected riser and excessive superheat is very difficult to analyze mathematically, but the combination has such an adverse effect on efficient risering it should be avoided—thus the calculation should rarely be necessary.) Making proper use of the applicable correction factors, and including the gating

system as part of the casting, the base riser equation may be used directly.

Pipe Contours

Although of little more than academic interest, the pipe contour obtained when a risered casting solidifies is calculable, subject to the conditions imposed in deriving the base riser equation, with the further limitation than an open-top, non-radiating riser be studied. The pipe contour analysis is presented in the Appendix, Part IV. A result of this type calculation is shown in Fig. 6, where a theoretical pipe contour is compared with a tin chill-casting. The general similarity lends further support to the validity of the riser analysis.

(A chill casting was used in this experiment to speed freezing, and thus minimize top losses. In a chill casting, solid contraction during freezing may actually compensate for some solidification shrinkage. Careful comparison of the real and theoretical contours in Fig. 6 indicates this was indeed the case.)

Risering of Alloys

The only complication, which arises in the case of alloy solidification, is the existence of a "mushy" stage during freezing. This causes feed metal to bog down in the riser, and be wasted. Probably this problem can be approached by assuming that the riser can no longer feed after it has become some fraction (e.g. 70 per cent) solid. When this quantity is written into the riser analysis, the base equation becomes (Appendix, Part V):

$$\frac{V_r}{V_c}(1-\beta) = \frac{1}{P}\frac{A_r}{A_c} + \beta$$

When some fraction, "P", of the riser is solid, feeding ceases.

An example of the difficulties resulting from mushy freezing is exemplified by the blind-risered, ductile iron casting shown in Fig 7. The gross pipe is well above the casting, there is no restriction at the throat of the riser, and yet the casting exhibits dispersed shrinkage below the riser.

Conclusions

A quantitative analysis of riser behavior, based upon a combined thermal and material balance, has yielded the following results:

1. A base risering equation, involving all possible simplifications, has been derived:

$$(1-\beta)\frac{V_r}{V_c} = \frac{A_r}{A_c} + \beta,$$

where 100β = per cent shrinkage, and

V_r, V_c, A_r, and A_c = *volumes* and *sand-contact-areas* of riser and casting, respectively.

In the absence of chills, insulators, exothermic materials, or radiation effects, the base equation may be used directly with good engineering accuracy.

2. To use the base equation, it is frequently necessary to apply correction factors to those areas of the casting or riser *not* in contact with sand, reducing these areas to "equivalent" sand-contact-areas. Chilled, insulated, or radiating areas may be treated in this fashion, and then introduced into the base equation.

3. The effects of mold contour and superheat are small enough to be neglected for most engineering purposes, provided risers are protected from radiation losses.

4. Comparison with the work of Caine[2] indicates close agreement between theory and practice for the case of steel castings. The analysis should extend equally well to other metals.

5. Superheat and radiation losses can combine to reduce feeding efficiency drastically. Since the amount of superheat will always vary in practice, *close control* may best be realized by protecting risers from radiation. The analysis of this situation has been carried out, but is not included in this paper because of its complexity.

6. A means for modifying the base equation to handle "mushy" freezing alloys has been hypothesized. The treatment assumes that the riser ceases to feed after it has become a fraction, "P", solidified.

$$(1-\beta)\frac{V_r}{V_c} = \frac{1}{P}\frac{A_r}{A_c} + \beta$$

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APPENDIX

Part I: Derivation of Base Riser Equation

(1) Material Balance

V_c = volume of casting

V_r = liquid volume of riser

V_{rf} = final solid volume of riser

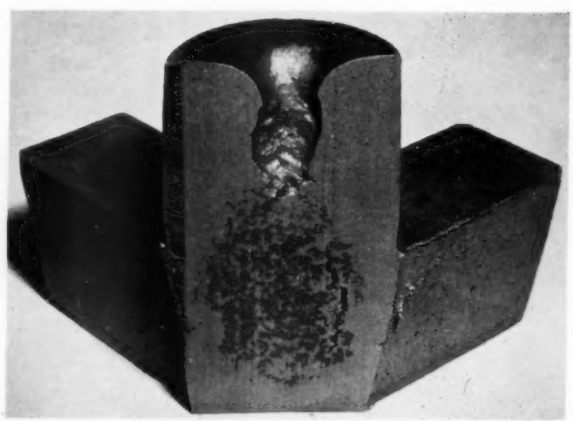


Fig. 7—Ductile iron casting showing dispersed shrinkage under the riser.

β = fraction shrinkage = $\frac{\text{per cent shrinkage}}{100}$

Since the entire shrinkage void is in the riser:

(final solid riser volume) = (liquid riser volume) — (total solidification shrinkage of riser and casting)

$$V_{rf} = V_r - \beta (V_r + V_c) \quad \ldots\ldots (1)$$

(2) Chvorinov's Rule, (freezing time) = (constant)

$$\times \left(\frac{V}{A}\right)^2$$

Assume perfect efficiency. The last drop of riser liquid disappears at the moment the casting becomes completely solid. In other words, solidification ceases simultaneously in the riser and in the casting.

Since the freezing times are equal for the riser and the casting,

$$\frac{V_{rf}}{A_r} = \frac{V_c}{A_c} \quad \ldots\ldots (2)$$

where:

A_r = sand-contact area of riser
A_c = sand-contact area of casting

(3) The Base Riser Equation

From Equation (1): $\frac{V_{rf}}{V_c} = \frac{V_r}{V_c} - \beta\left(\frac{V_r}{V_c} + 1\right)$

From Equation (2): $\frac{V_{rf}}{V_c} = \frac{A_r}{A_c}$

Then:

$$(1 - \beta)\frac{V_r}{V_c} = \frac{A_r}{A_c} + \beta \quad \ldots\ldots (3)$$

Equation (3) was used in preparing Figs. 2 and 3.

Part II: **Effect of Contour on Base Riser Equation**

Consider an example which may be regarded as typical: a cylindrical riser is designed to feed a flat plate. The top of the riser is protected from radiation losses. Heat flows into the flat mold wall according to the following relationship (5):

$$\frac{q_c}{A_c} = KT_s\left(\frac{1}{\sqrt{\pi a \theta}}\right) \quad \ldots\ldots (1)$$

where:

$\frac{q_c}{A_c}$ = heat flow rate per unit area of casting surface
K = thermal conductivity of mold
T_s = surface temperature of mold minus room temperature
a = thermal diffusivity of mold
θ = time

Heat flows somewhat more rapidly into the cylindrical riser wall (6):

$$\frac{q_r}{A_r} = KT_s\left(\frac{0.47}{R} + \frac{1}{\sqrt{\pi a \theta}}\right) \quad \ldots\ldots (2)$$

where:

$\frac{q_r}{A_r}$ = heat flow rate per unit area of riser surface
R = radius of the riser

And since:

$$\frac{V_r}{A_r} = \frac{R}{2}, \text{ for a cylinder,} \quad \ldots\ldots (3)$$

$$\frac{q_r}{A_r} = KT_s\left(\frac{0.47}{2}\frac{A_r}{V_r} + \frac{1}{\sqrt{\pi a \theta}}\right) \quad \ldots\ldots (4)$$

When solidication is complete, the total amount of heat which has left the casting may be found by integrating Equation (1):

$$Q_c = A_c K T_s\left(\frac{2\sqrt{\theta_f}}{\sqrt{\pi a}}\right) \quad \ldots\ldots (5)$$

θ_f = time required for casting to freeze
Q_c = heat of solidification of the casting

Similarly:

$$Q_r = A_r K T_s\left(\frac{0.47}{2}\frac{A_r}{V_r}\theta_f + \frac{2\sqrt{\theta_f}}{\sqrt{\pi a}}\right) \quad \ldots\ldots (6)$$

where:

Q_r = heat of solidification of riser.

Since Q_c and Q_r are heats of solidification,

$$Q_c = V_c \rho_s H \quad \ldots\ldots (7)$$

and $$Q_r = V_{rf} \rho_s H \quad \ldots\ldots (8)$$

where:

ρ_s = density of solid metal
H = heat of solidification per unit weight

Finally, Equations (5), (6), (7), and (8) are combined:

$$\frac{Q_r}{Q_c} = \frac{V_{rf}}{V_c} = \frac{A_r K T_s\left(\frac{0.47}{2}\frac{A_r}{V_r}\theta_f + \frac{2\sqrt{\theta_f}}{\sqrt{\pi a}}\right)}{A_c K T_s\left(\frac{2\sqrt{\theta_f}}{\sqrt{\pi a}}\right)} \quad \ldots (9)$$

Equations (5) and (7) may be combined to give:

$$\sqrt{\theta_f} = \frac{V_c \rho_s H \sqrt{\pi a}}{2 A_c K T_s} \quad \ldots\ldots (10)$$

Material Balance:

$$\frac{V_{rf}}{V_c} = \frac{V_r}{V_c} - \beta\left(\frac{V_r}{V_c} + 1\right) \quad \ldots\ldots (11)$$

Substituting Equations (10) and (11) into Equation (9), and rearranging:

$$(1 - \beta)\frac{V_r}{V_c} = \left(1 + D\frac{V_c/A_c}{V_r/A_r}\right)\frac{A_r}{A_c} + \beta \quad \ldots\ldots (12)$$

where:

$$D = \left(\frac{0.47\,\pi}{8}\right)\frac{a \rho_s H}{K T_s} \quad \ldots\ldots (13)$$

If the riser has a *spherical shape,* and is designed to feed a flat slab,

$$D = \left(\frac{\pi}{8}\right)\frac{a\rho_s H}{KT_s} \quad \ldots\ldots\ldots\ldots (14)$$

Equations (12) and (13) were used in preparing Fig. 3.

Part III: **Effect of Superheat on Base Riser Equation**

In this case:

Q_c = heat of solidification of the casting and superheat in the casting.

Q_r = heat of solidification of the riser and superheat in the riser.

$$Q_c = A_c K T_s \left(\frac{2\sqrt{\theta_f}}{\sqrt{\pi a}}\right) = V_c\rho_s H + V_c\rho_s C_p \triangle T \quad \ldots\ldots\ldots\ldots (1)$$

where:

P_s = density of solid metal

P_a = density of liquid metal

C_p = specific heat of liquid metal

$\triangle T$ = superheat

Since there is no contour effect,

$$Q_r = A_r K T_s \left(\frac{2\sqrt{\theta_f}}{\sqrt{\pi a}}\right) = V_{rf}\rho_s H + V_r\rho_a C_p \triangle T \quad \ldots\ldots\ldots\ldots (2)$$

Dividing Equation (2) by Equation (1),

$$\frac{Q_r}{Q_c} = \frac{A_r}{A_c}\,\frac{V_{rf}\rho_s H + V_r\rho_a C_p\triangle T}{V_c\rho_s H + V_c\rho_a C_p\triangle T} \quad \ldots\ldots\ldots\ldots (3)$$

Material Balance:

$$\frac{V_{rf}}{V_c} = \frac{V_r}{V_c} - \beta\left(\frac{V_r}{V_c} + 1\right) \quad \ldots\ldots\ldots\ldots (4)$$

Shrinkage expressed in terms of densities:

$$\beta = \frac{\text{volume of liquid-volume of solid}}{\text{volume of liquid}}$$

$$\beta = \frac{\dfrac{1}{\rho_a} - \dfrac{1}{\rho_s}}{\dfrac{1}{\rho_a}} = 1 - \frac{\rho_a}{\rho_s} \quad \ldots\ldots\ldots (5)$$

$$\frac{\rho_a}{\rho_s} = 1 - \beta \quad \ldots\ldots\ldots\ldots (6)$$

Substituting Equations (6) and (4) into (3), and rearranging:

$$\frac{A_r}{A_c} = \frac{V_r}{V_c} - \frac{\beta\left(\dfrac{V_r}{V_c} + 1\right)}{\dfrac{C_p\triangle T}{H}(1-\beta) + 1} \quad \ldots\ldots\ldots\ldots (7)$$

Part IV: **Effect of Radiation on Base Riser Equation**

In this case, heat leaves the riser by conduction into the mold, and radiation into the air, so that:

$$Q_r = v_{rf}\rho_s H = \frac{2A_{sr}KT_s\sqrt{\theta_f}}{\sqrt{\pi a}} + A_{tr}F_r\theta_f \quad \ldots\ldots (1)$$

A_{sr} = sand-contact area of riser

A_{tr} = radiating (top) area of riser

F_r = rate of heat loss per unit area of radiating (top) surface.

As in the Appendix, Parts II and III

$$Q_c = V_c\rho_s H = \frac{2A_cKT_s\sqrt{\theta_f}}{\sqrt{\pi a}} \quad \ldots\ldots\ldots\ldots (2)$$

Dividing (1) by (2):

$$\frac{Q_r}{Q_c} = \frac{v_{rf}}{V_c} = \frac{\dfrac{2A_cKT_s\sqrt{\theta_f}}{\sqrt{\pi a}} + A_{tr}F_r\theta_f}{\dfrac{2A_cKT_s\sqrt{\theta_f}}{\sqrt{\pi a}}} \quad \ldots\ldots (3)$$

From Equation (2)

$$\sqrt{\theta_f} = \frac{\sqrt{\pi a}\,V_c\rho_s H}{2A_cKT_s} \quad \ldots\ldots\ldots\ldots (4)$$

Material Balance, Appendix, Parts I, II, III:

$$\frac{v_{rf}}{V_c} = \frac{V_r}{V_c} - \beta\left(\frac{V_r}{V_c} + 1\right) \quad \ldots\ldots\ldots (5)$$

Substituting Equations (4) and (5) into (3):

$$(1-\beta)\frac{V_r}{V_c} = \frac{A_{sr} + f\,A_{tr}}{A_c} + \beta \quad \ldots\ldots\ldots\ldots (6)$$

where:

$$f = \left(\frac{F_r\pi a\rho_s H}{4K^2T_s^2}\right)\frac{V_c}{A_c} = B\,\frac{V_c}{A_c}$$

B = constant, a characteristic of the metal-molding material combination.

Part V: **Risering a "Mushy-Freezing" Alloy**

The riser can deliver no feed metal to the casting after it has become a fraction, "P", solid.

$Q_c = V_c\rho_s H$ ..... (1)

$Q_r = v_{rf}\rho_s PH$ ..... (2)

As in Appendix, Part III:

$$\frac{Q_r}{Q_c} = \frac{A_r}{A_c} \quad \ldots\ldots\ldots\ldots (3)$$

Material Balance:

$$\frac{v_{rf}}{V_c} = \frac{V_r}{V_c} - \beta\left(\frac{V_r}{V_c} + 1\right) \quad \ldots\ldots\ldots (4)$$

Equations (1), (2) and 3:

$$\frac{v_{rf}}{V_c} = \frac{1}{P}\,\frac{A_r}{A_c} \quad \ldots\ldots\ldots\ldots (5)$$

Combining Equations (4) and (5):

$$(1 - \beta)\frac{V_r}{V_c} = \frac{1}{P}\frac{A_r}{A_c} + \beta \quad \ldots (6)$$

Part VI: **Theoretical Pipe Contours**

A casting is fed by an open cylindrical riser having radius, "R", and height, "H", with no radiation losses from the top of the riser. When solidification is partly complete, the liquid zone in the riser is a cylinder having radius, "r", and height, "h". The riser is just big enough to feed the casting.

At any instant during solidification, the rates of freezing in the casting and in the riser are proportional to their areas (Chvorinov relationship) so that:

$$\frac{dv_r}{dv_c} = \frac{A_r}{A_c} \quad \ldots (1)$$

where

$\frac{dv_r}{dv_c}$ = change in volume of solid in riser per unit change in volume of solid in the casting.

Also, the rate of change in liquid riser height is related to rate of shrinkage of casting and riser:

$$-\pi r^2 dh = (dv_r + dv_c)\left(\frac{\rho_s}{\rho_a} - 1\right) \quad \ldots (2)$$

$$dv_r = -2\pi rh dr \quad \ldots (3)$$

Density-shrinkage relationship

$$\frac{\rho_s}{\rho_a} - 1 = \frac{\beta}{1-\beta} \quad \ldots (4)$$

Combining (1), (2), (3), and (4)

$$\frac{dh}{h} = \left(\frac{2\beta}{1-\beta}\right)\left(1 + \frac{A_c}{A_r}\right)\frac{dr}{r} \quad \ldots (5)$$

And, since $h = H$ when $r = R$,

$$\ln \frac{h}{H} = \left(\frac{2\beta}{1-\beta}\right)\left(1 + \frac{A_c}{A_r}\right)\ln \frac{r}{R}$$

Relation (6) was used in preparation of the theoretical pipe contour shown in Fig. 6.

FOUNDRY NOISE AND ITS CONTROL

By

Herbert T. Walworth*

The growing interest of management in industrial noise problems is brought about by the prospect that loss of hearing among workers exposed to excessive noise may be considered compensable in certain states. It is not the purpose of this paper to discuss the legal aspects of this problem, but rather to review some of the technical factors of industrial noise exposures and their control in the foundry and other industries, and also to discuss briefly the background leading to our present problems, the plans which are being formulated for their solution and certain practical approaches to noise control.

It is important, therefore, that all of the unknown factors relating to this problem should be classified.

Need for Industrial Noise Research

It has been estimated that between 15 and 20 million adults in this country have defective hearing. Exposure to excessive industrial noise is only one of the causes of defective hearing, and thus industry is faced with the prospect of assuming responsibility for impaired health which was not caused by conditions of employment. Some of the more important items needing further research and study at this time are:

1) The development of permissible safe noise levels.

2) Practical procedures for determining or evaluating hearing loss. While pure tone audiometers are in common use, the procedure for operation in actual use may be subject to criticism because test room background noise will usually exceed the decibels of hearing loss being tested.

Medical departments and audiometric testing rooms in areas with noise levels below 50 decibels (commonly recommended) do not seem practical for most industrial facilities.

3) Procedures for evaluating relative or percentage hearing loss.

4) Specifications for construction of acceptable rooms for audiometric testing.

5) Practical medical control programs for the noisy trades.

6) Evaluation of permanent hearing loss in relation to time of last exposure.

7) Measurement of impact noises.

8) Noise control. The scope of noise control is so broad that years may be required before significant accomplishments are made.

It is apparent that the scope of these problems suggests the need for early study and research.

Research in Industrial Noise. A complete summary of the various aspects of noise and its effects on behavior, the human ear, and communications was published by Kryter[1] in 1950. This report is a review, evaluation and interpretation of the experimental literature on noise up to that time, and should be required reading for those more than casually interested in this subject.

In the middle 1940's, the Committee on Conservation of Hearing of the American Academy of Ophthalmology and Otolaryngology became interested in industrial noise and its effect on workers, and promoted discussions among professional groups to stimulate greater interest in the subject. The Sub-committee on Noise in Industry of this committee instituted a research program designed to evaluate some of the factors concerned with loss of hearing and industrial noise. Other groups showed increased interest and many new research projects were instituted in various phases of the problem. However, it soon became apparent that the compensation aspects of industrial loss of hearing were developing more rapidly than the results of scientific research. This, then, emphasized the need for expanding these research projects and broadening their scope.

In the spring of 1952, two widely separated industry groups approached the Industrial Hygiene Foundation with the proposal to undertake the job of promoting and coordinating industrial noise research. The Foundation was selected because of its long association with industry in connection with industrial hygiene and health problems. Also, it has facilities and a staff to form the nucleous around which a practical program can be developed.

The Foundation agreed to study this problem and the project is now under way. When sufficient funds are available, the Foundation will promote specific research among those organizations already working in this field. Much of its effort will be directed to-

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ward coordination of existing research, rather than to encourage the development of new facilities. As the program develops, it also will act as a clearing house for information on the broad field of industrial noise.

In addition to offering facilities for doing this work, the Industrial Hygiene Foundation will have access to large groups of industrial workers for hearing field studies. It is through the examination of workers exposed to noise of different intensities and frequencies for different exposure periods which eventually will provide the data for establishing minimum permissible noise levels.

Noise Effects on Work and Efficiency. It is quite generally believed that noise causes fatigue and thus reduces production and efficiency. With further reference to Kryter's[1] work, "..... field studies do not convincingly show that the noise encountered in certain industries and offices has any detrimental or beneficial effect upon non-auditory tasks performed by man." It was Kryter's general conclusion that nearly all field and laboratory studies which reported that noise adversely affects work output were open to criticism because of poor study techniques and failure to control related variable factors, such as lighting, temperature and humidity. In fact, most of the well controlled studies revealed that steady noises do not adversely affect the ability of persons to do mental, and ordinary motor or mechanical tasks.

Most of the research studies on the effect of noise on work output and efficiency did not consider noise effect on communication. It can be assumed that noise can interrupt, interfere with, or reduce the accuracy of communication. Therefore, the only way in which noise is definitely known to affect work and efficiency is through its effect upon the speed and accuracy of communication. Where communication is not a factor, the effects of noise on work efficiency are believed to be negligible.

Permissible Noise Levels. The greatest value of well founded permissible or safe noise levels is as bench marks in the design of environmental control. Unless permissible levels are based on scientific facts, however, an injustice may result to the exposed worker or an unusual burden will be placed upon industry.

Many workers in the industrial health, medical and other fields have suggested permissible levels ranging from 75 to 115 decibels.[4] In practically all of these cases, the suggested levels are in over-all intensity in decibels without consideration of frequency. Most of these suggested values can be considered little more than guesses since they have limited or no scientific verification.

A few workers have proposed tolerance levels in which frequency is considered. However, except for the lowest octave band, there is poor agreement throughout the remainder of the frequency spectrum. More recently, Hardy[5] suggested permissible levels which are shown in Fig. 1.

According to Hardy the 50-sone and 100-sone curves suggest the lower and upper limits respectively of damage risk for prolonged daily exposures. In other words, it is suggested that noise levels in frequency bands above the 100-sone line are definitely damaging to the hearing of exposed workers, while noises falling below the 50-sone line are considered safe. The area between these two curves includes all border-line noises which may or may not eventually be shown to cause hearing loss.

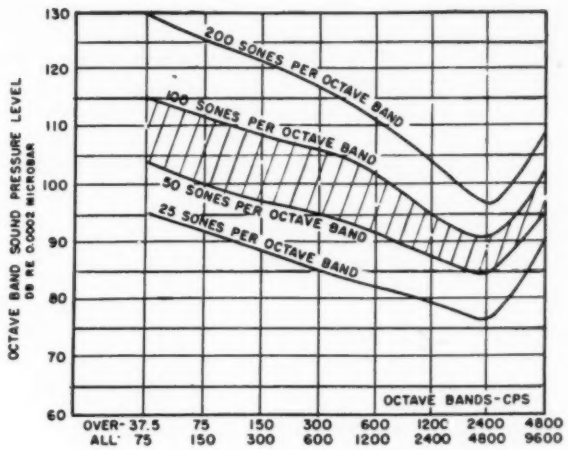


Fig. 1—Hardy's[5] tentative hearing risk criterion. The 50-sone and 100-sone curves bracket the tentative lower and upper damage risk for prolonged daily exposure. These curves are tentative and subject to modification with the publication of new data.

Hardy presents these curves as tentative conclusions and admits that they are based on limited data. However, even the limited substantiation presented appears to justify the use of the curves as at least tentative bench marks for the present.

Foundry Noise. The evaluation of foundry noises presents many problems because of the great variety of operations and the influence one operation has on noise exposure at another. Open windows tend to decrease noise exposures (by reducing reflections), and the construction of walls or the installations of metal shields may increase exposure by increasing reflections. Plant layout and arrangement of equipment also affects the noise level. So does an increase or decrease in the number of operating machines.

Because of the many variables in noise exposure, it is possible that noise levels inside a foundry may change one, two, or more decibels in any octave band from day to day. Variations of over 10 db can be expected when the same operation is being performed on different pieces of work. Likewise, the noise exposures for similar operations may vary from one foundry to another. The noise produced by a machine depends on its size, weight, speed, mounting, location with respect to other machines and reflecting surfaces, and state of repair. Thus, while the noise spectra reported in Figs. 2, 3 and 4 are typical of the foundry operations reported, they may not be the same for all foundries.

The measurements reported here were made using the standard sound level meter in conjunction with an octave band analyzer. The importance of measuring noise in octave bands cannot be over-emphasized since it not only shows the contribution of different

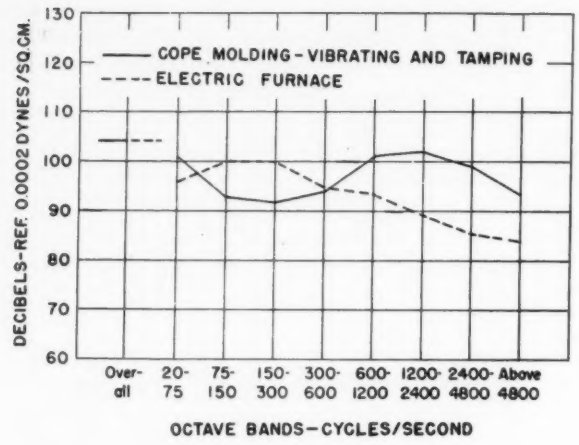


Fig. 2—Foundry noise spectra at a molding operation and an electric furnace.

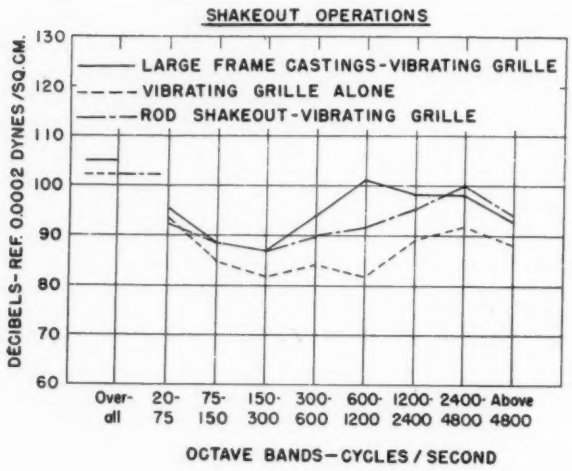


Fig. 3—Foundry noise spectra at shakeout operations.

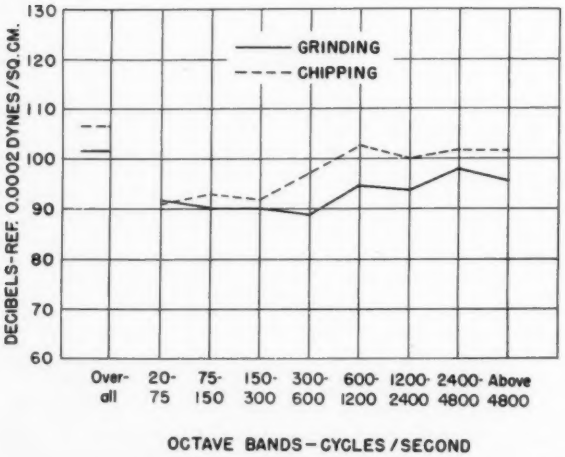


Fig. 4—Foundry noise spectra at grinding and chipping operations.

frequencies to the total noise, but it also gives information as to where to start when considering control.

Figure 2 shows the noise spectra for a molding operation and an electric furnace. It is interesting to note that the over-all level was 104 db in each case, but the distribution of noise by octave bands varied considerably. The greater part of the energy of the electric furnace noise was in the lower frequencies, while the energy of this particular molding operation was in the higher frequencies.

Figure 3 presents three noise spectra of shakeout operations. The solid line shows the noise picture of a vibrating grill type shakeout handling large castings. This curve has a general upward trend, with the highest noise levels in the four upper octave bands. The dotted curve represents the noise from the vibrating grill alone. The two curves are somewhat similar in shape, but the noise levels are lower with the vibrating grill alone.

The spectrum for rod shakeout was similar to large casting shakeout, but noise levels were slightly lower. The higher noise level in the 600-1200 octave band for the large castings shakeout may have been caused by the shape of the castings which produced and amplified the noise in this frequency band.

Figure 4 shows noise from chipping and grinding operations. Most chipping operations will result in considerably higher noise levels in all octave bands than those shown. In this case, chipping was being done on small castings with a small tool. Grinding noise also may be higher in many situations since intensities will be influenced by the number of machines, type of metal and castings, and the size and shape of casting. The general trend of these curves is upward in the higher frequencies.

In general, these spectra illustrate the concentration of noise in the higher frequencies, an area which is potentially damaging to the ears of exposed workers. They further illustrate some of the complicated control problems facing the foundry since shakeout and chipping operations in particular do not lend themselves to the simpler control methods. In any case, the first step in noise control is evaluation of the potential hazard, followed by a program of re-evaluation as exposures are controlled or improvements made.

Noise Control

Noise control for worker protection has not been generally practiced in most modern industries. Many of the advances in this field have been made in quieting offices, hospitals and schools for the purpose of improved comfort. However, since useful knowledge in scientific fields grows with practical experience, an understanding of the basic principles of industrial noise control and their application should form the basis for rapid progress in this field. These basic principles are:

Quiet the Source

Just as industrial dusts, fumes, vapors and gases are best controlled at the point of origin, so are the greatest benefits derived from noise control at the source. This can be accomplished through emphasis on noise control in machine design, redesign after installation, or machine maintenance. The ideal approach, of

course, is noise reduction through improvements in design.

Many manufacturers have done outstanding jobs in this respect, although much of the noise reduction has been accomplished in connection with items sold to the public, such as automobiles, refrigerators, home appliances, etc. As time goes on, industrial purchasers will demand quieter machines, and designers and suppliers will be required to give more consideration to these factors.

As the foundry industry recognizes and evaluates its noise problems, it will be in a better position to make changes in operating machines that will be favorable to noise reduction. Gear noises, loose bearings, noisy conveyors, escaping compressed air and steam and similar noise sources can be quieted through engineering and maintenance department changes. The noise caused by escaping waste compressed air is easily controlled by use of a simple muffling device packed with brass or other metal turnings (Fig. 5).

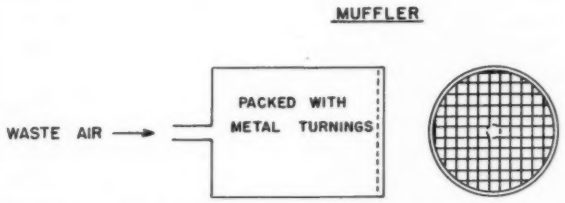


Fig. 5—Noise control from waste compressed air by use of muffler.

Other considerations to bear in mind when controlling noise at its source are:

1) Determine the vibrating surfaces
2) Reduce area and number of vibrating surfaces
3) Avoid vibrating or impact forces
4) Avoid high frequency vibrations
   a) Control by increasing weight
   b) Decreasing speed
5) Use rubber or other dampening materials wherever possible
6) Provide regular maintenance

Substitute Less Noisy Operations or Machines

Certain operations may lend themselves to a form of substitution which will provide for the elimination of noisy operations. Examples of this method of control include substitution of "squeeze" type of equipment and welding for riveting operations, and chemical cleaning of metal for high speed polishing wheels. The engineering departments of many industries should be in an advantageous position to consider such methods of control in its development activities.

Increase Distance Between Source and Worker

The application of distance to noise control is simple in theory, although it may not always be practical in practice.For example, the necessity for operating a shakeout machine at a particular location may make it impractical to move that machine. However, much can be accomplished in noise reduction if this principle is applied in plant layout, or when relocating machinery.

Noise reduction by increasing distance follows the inverse square law. Thus, by doubling the distance from the source, the sound intensity is reduced to one fourth of the original intensity. Trippling the distance reduces the intensity to one ninth. It may be possible, therefore, to effectively reduce noise exposure by minor adjustment in plant layout. The effect of distance is roughly illustrated in Fig. 6.

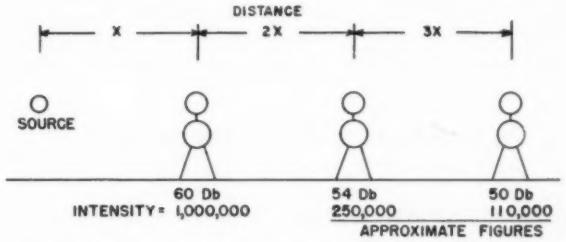


Fig. 6—Noise control by increasing distance between noise source and exposed worker.

Construct Barrier Between Noise Source and Worker

The isolation of noise sources should have many applications in the foundry industry. This involves the construction of a wall or other barrier between the source and the exposed workers, or isolating the operation in a separate room. If the isolated machine requires an operator, protection may be provided by the installation of sound absorbing material or by supplying workers with adequate personal protective equipment. Thus, the efficient use of barriers, or isolation of especially noisy operations, can greatly reduce the number of workers exposed to damaging noise.

Reduce Noise Reflections by Absorption

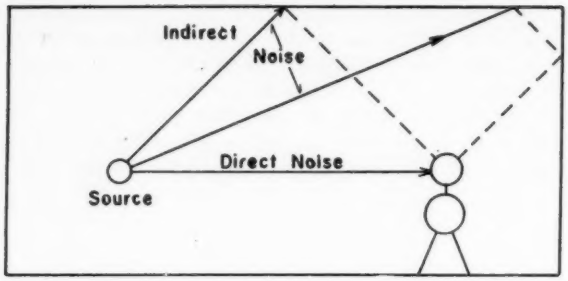
When noise is emitting from a sound source, sound waves travel outward in all directions. When the sound wave encounters other machinery or walls, it is reflected in the same manner as light. As other surfaces are encountered, the wave is again reflected. Thus, every part of the room almost immediately is filled with sound waves traveling in every possible direction.

These multiple sound reflections have the effect of increasing the over-all sound intensity. The total sound intensity will equal the sum of the intensities of the direct and indirect sound. In any foundry or other industrial operation, reflected sound can contribute materially to the noise exposure. In some cases, reflected sound can cause an increase of as much as 25 db over the intensity level of the direct noise alone. The control of noise by the absorption of sound is illustrated in Fig. 7.

As mentioned above, noises in the higher frequencies are more damaging to the human ear. Fortunately, it is the noises of higher frequency which are more easily controlled.

Reduce Exposure Time

In atomic energy installations exposure to external radiations is limited to 300 milliroentgens per week. Various methods are used to measure total exposure to radiation, and when the exposure exceeds 300 milliroentgens in any one week, the worker is removed from all radiation exposure and he is not per-



NOISE CONTROL BY ABSORPTION

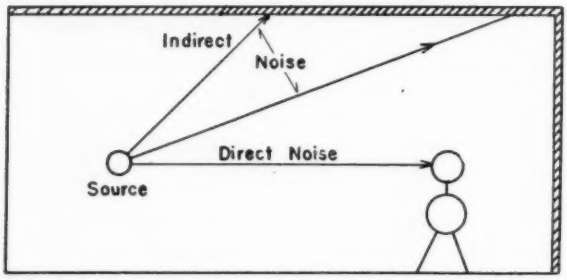


Fig. 7—Noise control by absorption of reflected sound.

mitted to work until the average weekly exposure is reduced to below permissible levels. In the case of serious accidental exposure, the time away from the job might conceivably extend to several weeks.

In a similar manner, the total exposure of workers to noise can be reduced by removing the worker from the exposure a certain period of time each day, or transferring the worker to a "quiet" area after spending a certain period of time in excessive noise. Usually the human ear can resist high noise levels for short periods and the provision of rest periods offers a method of averaging the high with low exposure levels. However, since permissible or safe noise levels have not been determined, it would be difficult to immediately measure the beneficial effects of 2-hr or 4-hr relief periods from excessive daily exposures. Thus, the application of this method of worker protection offers certain problems and the complete answers will be forthcoming only after further study. However, the practice of a well organized audiometric program makes it possible to regulate exposure time with sufficient accuracy to insure worker protection.

Admittedly, this method of control offers many administrative problems, but unless industry can reduce harmful noise levels by engineering or other means, such measures may be necessary.

Provide Personal Protection

The use of respirators for protection against dusts, fumes, vapors and gases usually is considered only when conditions are temporary, or when ventilation or other engineering control is not practical. The application of this basic principle to the wearing of ear protectors, ear muffs or helmets for reducing noise exposures would be considered desirable.

There are certain operations and machines in the foundry industry which do not lend themselves to the application of engineering quieting procedures. For example, there is little prospect that grinding and chipping operations can be quieted at the source, and methods have not yet been developed which would allow the substitution of a quiet process. While it is possible that these operations might be isolated and the reflected sound absorbed by acoustical materials, the operator would still be exposed to high noise levels. Thus, the only known protection for the operation is suitable ear protection.

Ear protectors are of three types[2]: 1) substances inserted into the canal (insert types); 2) objects covering the orifice of the canal (muff types); and 3) fitted coverings (helmets) which include the major area of the skull. The protection (reduction in decibels of sound reaching the inner ear) afforded by the various devices on the market will vary considerably. Perhaps 25 or more decibels attenuation in all frequencies might be expected if the protectors are properly fitted. There have been no reliable scientific reports concerning the protection provided by various ear defenders and our principal source of information is from the manufacturers.

The success of an ear protector program will hinge directly on program planning, the backing of managment, and the effort made to "sell" and supervise the employees participating. Unlike respirators, insert ear protectors cannot be seen readily from a distance and close supervision will be necessary.

In high noise exposures, many employees plug their ears with dry cotton for protection. This practice has been proven to be of no value in excluding noise, but the supervisor may encounter difficulty in convincing the employee that he should wear an "acceptable" protector which may be uncomfortable to wear.

Following are hints which may be helpful in instituting an ear protector program:

1) A reasonable supply of ear protectors should be kept on hand, as they are small and easily misplaced.

2) Ear protectors should be cleaned daily.

3) A suitable lubricating jelly should be readily available where permanent type protectors are used.

4) Workers should be encouraged to replace defective protectors promptly.

5) Workers should be instructed to select the largest size which will fit into the ear canal. Employees have a tendency to select the smaller sizes of protectors because they are more comfortable.

6) When purchasing ear protectors in quantity, it has been suggested that the total order include sizes in the following proportions[2]: small, 15 per cent; medium, 70 per cent; large, 15 per cent.

Discussion

In the minds of many people the complete answer to noise control is the use of acoustical materials. While these products have many valuable applications, they are not the answer to all noise problems. Occasionally, acoustical materials are sold with the promise that the noise in a given situation will be reduced by a high percentage. Usually, such claims

refer to a reduction of energy or loudness by these percentages, although the customer may believe that the anticipated reduction is in decibels. The following table gives a comparison of noise reduction in decibels and the corresponding percentage reduction of energy and loudness.

NOISE REDUCTION

Decibels	Energy, %	Loudness, %
1	20	5
3	50	20
5	70	33
10	90	55
20	99	80

Thus, a one decibel reduction will result in 20 per cent decrease in energy and a 5 per cent decrease in loudness. Likewise, a reduction of 3 decibels corresponds to 50 per cent decrease in energy and a 20 per cent decrease in loudness.

Many acoustical materials have low resistance to wear and abrasion and in certain industrial operations their value may be quickly destroyed. Exposure to abrasive materials, steam, acids, alkalis and mechanical shock may reduce the noise absorption properties and rapidly reduce their effectiveness. Therefore, the use of these materials must be properly engineered in order to realize their greatest value.

Medical Control. It is not the purpose of this paper to outline a medical control program for noisy occupations. However, many of the phases of suggested medical control programs for the noisy trades need clarification. The establishment of audiometric programs should be made with the full realization of the possible pitfalls and the probable results which may be realized. Among other things, the application and use of speech versus pure tone audiometry should be clarified.

Further information regarding medical control in the noisy trades may be secured by contacting the Sub-committee on Noise in Industry of the Committee on Conservation of Hearing of the Academy of Ophthalmology and Otolaryngology.

Conclusions

Industrial noise exposures are by-products of the growth and mechanization of industry.

The prospect that industrial loss of hearing may become a compensation problem emphasizes the need for increased activity in the field of noise control. Scientific advancements in this field have been slow and we are in need of much additional medical and engineering information. However, present knowledge, if used and properly applied, will go a long way toward the protection of workers exposed to excessive noise. Industrial noises can be measured with equipment now available and by techniques which have proved satisfactory. While permissible noise exposure levels have not been definitely established, sufficient data are available which at least classify noisy operations into "excessive," "border-line" and "safe" catagories.

After having classified its noise hazards, the foundry industry can establish control programs based upon the discussion presented above. For the noise hazard which cannot be controlled by use of the plant's existing facilities, there are consulting organizations which may be employed to give these problems detailed consideration. For those situations which still do not lend themselves to engineering control, individual worker ear protection is still available. Of course, any such program should include medical control using approved audiometric procedures and techniques.

In the final analysis, the success of industrial noise control programs will depend upon industry's desire to study and control its own hazards.

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SOUND AND ITS PHYSICAL PROPERTIES

SOUND: Sound is a series of alternate waves of compression and rarefraction, set up in a transmitting medium such as the atmosphere by a mechanically vibrating body. In the atmosphere, sound waves are radiated spherically. When these waves are received by the human ear, they are translated into sound through the hearing mechanism.

CYCLE: One complete forward and backward movement of the vibrating sound source which produces one complete sound wave.

FREQUENCY: The number of cycles per second of time, or the number of waves reaching the ear per second.

HEARING: Hearing is the mechanism by which sound waves are transmitted into sensations of sound. Pitch is the sensitivity of the ear to sound frequency. The ear can distinguish frequencies ranging from 20 to 15,000 cycles per second. Thunder, for example, is composed of low pitched sounds, while the chirp of insects represents high pitched sounds. Middle C on the piano is 256 cycles per second, and each octave above is twice the frequency preceding.

The human ear is a sensitive and versatile mechanism, for it measures many of the physical properties of sound over wide ranges. In addition, it measures loudness of sound, a property which cannot be directly measured by scientific instruments.

INTENSITY—DECIBELS: The ear is capable of receiving a large range of sound intensities. The loudest sound the ear can hear without pain (120 decibels) is one trillion times the intensity of the faintest audible sound. Because of this wide range of intensities, the decibel has been adopted as a more convenient unit to use in calculations. The decibel is a unit for measuring the ratio of two amounts of acoustical power (sound intensity or pressure).

The decibel is a logarithmic unit and thus cannot be added or subtracted as we do length, weight and time. If the intensity of any sound is doubled, there will be a corresponding increase of approximately 3 decibels. For example, two horns each of 60 db intensity or sound pressure will yield a combined intensity of 63 db and not 120 db as is sometimes believed.

Also, one decibel increase corresponds to an increase in intensity of approximately 26 per cent.

LOUDNESS: Two sounds of the same intensity level, but of different frequencies do not necessarily sound equally loud. Neither does one sound having double the intensity of another sound appear twice as loud. The reasons for this are

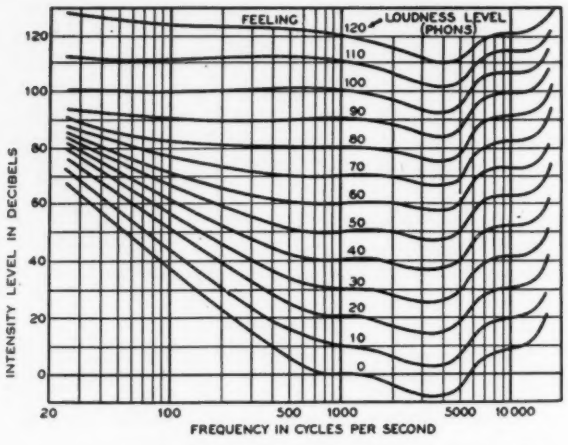


Fig. 8—Fletcher-Munson equal loudness contours.[6]

at least partially explained in Fig. 18, which shows the Fletcher-Munson curves of equal loudness. The curves were constructed on the basis of loudness judgments of large numbers of persons with normal hearing. Each point on one of these lines or contours represents a sound which appears to the average normal ear to be just as loud as the sound represented by every other point on the contour. For example, if a tone of 1000 cycles per second (cps) is set at 50 decibels intensity level, the loudness would be the same as a 100 cps tone of 67 decibels (follow 50 phon line to intersect 100 cycle frequency and read 67 decibels).

This chart leads to the following conclusions:

a) High frequency sounds at low intensity levels seem louder than low frequency sounds of the same intensity.
b) At high intensity levels, all tones sound almost equally loud at all frequencies.

NOISE MEASUREMENT: Intensity levels of noise are measured in decibels by means of the sound level meter. When used with the octave band analyzer, it is possible to measure noise intensity levels in octave bands ranging from 20-75 cps to 4800-10,000 cps. When evaluating noise exposures, it is important to use the octave band analyzer since there are indications that noise exposures in the middle and high frequencies are more damaging to the hearing mechanism than those in the low frequencies. In addition, the collection of frequency data usually gives valuable information for noise control.

ENGINEERING ASPECTS OF CENTRIFUGAL CASTING*

By

John F. Wallace**

ABSTRACT

Centrifugal casting is conducted by three methods: true centrifugal, semi-centrifugal, and centrifuged castings. In the true centrifugal method, the mold is spun about its own axis and at least part of the useful interior surface is formed by centrifugal force. The true centrifugal casting process in horizontal molds is widely used for the manufacture of cast iron pipe, gun tubes and cylinders of steel, heat resistant metals, and copper-base alloys. The vertical axis, true centrifugal process is employed to manufacture cylinder barrels, short tubes, gears, and liners. Semi-centrifugal casting is utilized for the casting of disc-shaped and annular castings spun about their axes with cores used for internal surfaces. Centrifuged castings are small similarly shaped parts located in symmetrical stacks about a central sprue. Both semi-centrifugal and centrifuged castings are produced in vertical axis machines. Metal and sand molds are employed for true centrifugal castings but the semi-centrifugal and centrifuged processes usually use sand molds.

True centrifugal castings will produce a high quality product by the elimination of gaseous cavities, the removal of nonmetallics to the inside surface, and the assurance of a sound casting. Both true, semi-centrifugal, and centrifuged castings can be produced with a high yield and excellent metal flow and detail. Centrifugal castings are subject to defects, such as hot tears, laps, undesirable segregation, and refractory failure but these defects can usually be easily detected by inspection. Metal molds will increase metallurgical quality, reduce segregation, and increase the production rate, but sand molds are more adaptable and reduce the occurrence of laps.

Centrifugal casting is in competition with the forging and static sand casting processes. The dimensions, quality, and volume requirements of the casting dictate the most economic method of production. The centrifugal casting process selected on this basis has a definite field in which its use is advantageous. The process is no panacea for casting ills, however, and should be confined within these limitations.

Introduction

This paper is intended to review and describe the various types of centrifugal casting and to evaluate the engineering aspects of each type. Centrifugal casting has been utilized for various types of parts and been periodically investigated since the inauguration of this method by the patents of Anthony Eckhard, an Englishman, in 1809.[1] While this process has now been improved so that it has several advantages that dictate its use for certain products, this condition has only been a development in the last 30 years. Centrifugal casting absolutely required the introduction and development of the electric motor to solve the problem of a simple, dependable, and inexpensive driving mechanism for rapidly rotating the molds.[2]

The engineering aspects of centrifugal casting can be more simply described as a study of when it is economically and technically advantageous to utilize each type of this process in comparison with other methods. There are two separate methods with which centrifugal casting is in direct competition: static casting and forging.[3] The engineering decision to employ any given process depends upon how the relative advantages and disadvantages of each method are affected by the characteristics of the particular item being manufactured.

There is no previously recorded study on the engineering aspects of centrifugal casting, but several articles have been published discussing the economics and technical limitations of the various methods of centrifugal casting and the use of the process for specific applications. The American Foundrymen's Society published a complete *Bibliography of Centrifugal Casting* up to 1949 by Taylor and Register.[1] In addition to a bibliography, this work contains a history of the development of centrifugal casting and a discussion of fundamentals that briefly cover some of the salient economic and technical points. The American Foundrymen's Society also published a *Symposium on Centrifugal Casting*[4] that contains a group of several important technical articles on the subject. Papers by Donoho,[3] Weber,[5] Zuehlke,[6] and Janco[7] in this symposium all discuss some aspects of engineering in centrifugal casting.

Donoho's[2] paper compares the advantages of centrifugal casting with static casting and forging in considerable detail. Weber[5] describes the difficulties and limitations of centrifugal casting machines. Zuehlke[6]

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discusses the various types of equipment, some of the metallurgical advantages, and economic considerations compared to sand castings. Janco,[7] in his article in this Symposium, is concerned with the practicability of centrifugal casting and some of the economic advantages.

Briggs[2] describes some of the more successful centrifugal casting processes and products, the limitations of the process, and some cost considerations. Dickson[8] in an article written on centrifugal casting of gun tubes at Watertown Arsenal has listed several metallurgical and economic advantages of horizontal, thick-walled castings. Some of these advantages have to be amended somewhat in the light of additional gun casting experience, but many of these factors have been substantiated by a production program. Donoho[9] describes the adaptability of several advantages of centrifugal castings to the manufacture of specific tubular products. Unfortunately, some enthusiasts for centrifugal casting have made claims for the process that have been far beyond the capabilities. These writers have rendered centrifugal casting a disservice. Fortunately, articles by Taylor,[1] Briggs,[2] Zuehlke,[6] and Cammen[10] have pointed out the limitations of the process with respect to competition with other casting methods, increasing density, occurrence of special defects, and elimination of impurities.

A surprisingly small amount of technical literature has appeared on the subject of centrifugal casting. The overwhelming amount of this literature has been on one specific phase of only one type of centrifugal casting. A review of the literature is considerably expedited by the use of the bibliography of Taylor and Register.[1] A review of the articles contained in this bibliography and more recent work reveals only a few[4,9,11,12] that could be considered to be general in scope. Proper coverage of this subject requires consideration of the individual articles on each specific phase. On the basis of this study, together with unpublished Watertown data on centrifugal casting, an engineering evaluation of the applications of this process can be obtained.

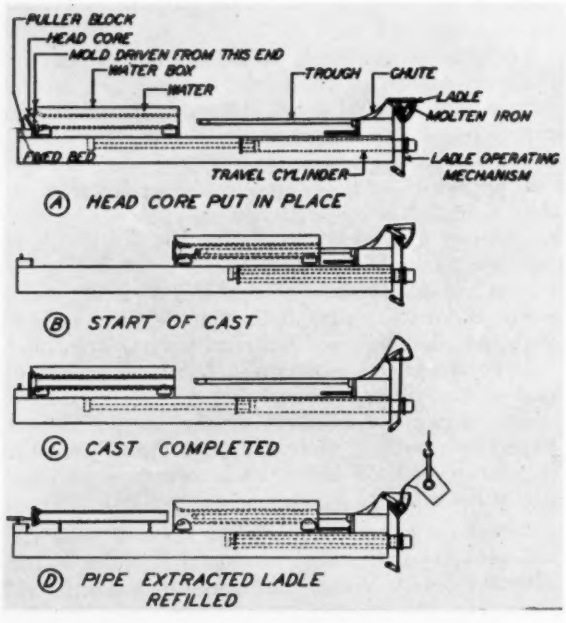


Fig. 1—Diagram of operations of DeLavaud Process for cast iron pipe manufacture (Stuart).

Definitions

Centrifugal casting is divided into several interlocking categories. The field can be separated by the type of metal that is being cast, the orientation of the axis of rotation of the centrifugal casting machine, or the type of mold utilized. The most important difference in the methods of casting, however, is concerned with the shape of the casting and the effect centrifugal force has in producing this shape. Accordingly, the field of centrifugal casting is usually divided into three types: true centrifugal casting, semi-centrifugal casting, and centrifuging. In true centrifugal casting, the mold is spun about its own axis and at least part of the interior surface is shaped by centrifugal force without the aid of a core. In semi- centrifugal casting the mold is spun about its own axis and the centrifugal force generates pressure towards the outer section to assist in feeding. This method is usually used for disc-shaped pieces. Centrifuging is exclusively a vertical axis type in which the molds are spaced symmetrically around a central down sprue that is also the axis of rotation. The metal enters each mold cavity through radial ingates from the central sprue.

True Centrifugal Casting

Pipe Manufacture—Horizontal Axis—Metal Molds

The most widely used of centrifugal casting methods is true centrifugal casting. This process has been particularly successful in the manufacture of cast iron pipe. The pipe are produced in a machine rotating about a horizontal axis in both metal and sand-lined molds. The product of both methods compares favorably with soil pipe cast by the normal static sand casting procedure.

The production of cast iron pipe[13] in metal molds is known commercially as the De-Lavaud process. The casting machine consists of a cylindrical steel mold, surrounded by a water jacket and mounted on rollers so that it can be rotated within the water jacket. The jacket-mold assembly can be moved longitudinally to the axis of the mold on a fixed bed inclined slightly to the horizontal. The molten iron is fed into the mold through a refractory lined trough that is similarly inclined to the horizontal. The trough has a pivoted ladle at one end and a small curved spout at the other. The ladle contains sufficient metal for one pipe. The spout directs the molten metal in the trough towards the side wall of the mold. The ladle is tilted at a uniform rate by an electric hoist to pour a constant stream of molten iron into the trough. The steel mold is coated with a silica base refractory wash utilizing a special spraying nozzle before each cast. A schematic diagram of the equipment and the four operations mentioned above is shown in Fig. 1.[13]

The pipe manufactured varies from 3 to 24 in. in diameter and 12 to 18 ft in length. The speed of rotation of the centrifugal casting machines during production of pipe in metal molds is determined by the inside dimensions of the pipe. In practice, a centrif-

ugal force of approximately 60 times the acceleration of gravity or 60g is obtained at the inside diameter of the casting. The centrifugal force at any point in a true centrifugal casting can be calculated as:

$$F = \frac{W}{g} 4\pi^2 N_1^2 R$$

where F = Centrifugal force in pounds
W = Weight in pounds
N_1 = Number of revolutions per second
R = Radius or distance of that point from axis of rotation in feet
g = Acceleration of gravity in feet per second squared

The pounds of force per pound of metal or centrifugal force x gravity (g's) is:

$$F = \frac{4\pi^2 N^2 R}{3600 \times 32.2}$$

where N = Number of Revolutions per minute
The calculation of centrifugal force in further detail is described by Carrington[14] and some excellent nomograms for practical use are published by Donoho.[8]

The manufacture of pipe in this manner is limited in several important respects. The thickness of the tube must be restricted so that all solidification occurs from the outside towards the center. A bell and bell core can only be placed on one end to permit the tube to contract freely longitudinally. The process is limited to a type of metal that flows well, such as the gray cast iron used, and will not erode the refractory trough severely.

In this process, the metal leaves the spout at the end of the trough travelling in the direction the mold is rotating and is picked up immediately by the revolving metal mold. The mold-jacket assembly is retracted at such a rate that an even helix of molten cast iron is laid upon the rotating mold with a slight overlap to assure good fusion. The rotation and pour is rapid enough so that the previous spiral has not solidified before another spiral is placed next to it. Centrifugal force maintains a smooth, even cylindrical shape on the inside surface of the pipe. The speed of rotation must be rapid enough to prevent any slipping between metal and mold.

Properties of Horizontal Centrifugally Cast Pipe

The mechanical properties of the cast iron pipe produced by true horizontal centrifugal casting compare favorably with cast iron produced by static methods. The rapid unidirectional solidification assures a fine grained, uniform, well fed structure. The centrifugal force removes all gas holes and assists in forcing lighter constituents such as non-metallic inclusions towards the center, but solidification is so rapid that complete elimination is not possible. The rapid solidification permits little segregation of the cast iron. The pipe is resistant to impact and makes a strong durable soil pipe.

The principal defects in the process are the occurrence of laps, cracks, and non-metallic bodies in the pipe. Laps result from the improper fusion of successive layers of cast iron, cracks from non-uniform or restricted contraction of the pipe, and the non-metallics are broken away from either the ladle or trough. The process has been developed to an extent, however, where the occurrence of defects is infrequent.

The manufacture of cast iron pipe by the true, horizontal centrifugal casting method in metal molds has two competitors: centrifugal casting in sand molds and static casting in sand molds. The metal mold centrifugal process has the advantages of very high quality, high production rate, and low rejection percentage. It is necessary to replace the chill molds after considerable use and to anneal the pipe after casting. The centrifugal casting machines are expensive and are more complicated than those required for sand molds. The process cannot manufacture pipe requiring bells on both ends and a considerable order of pipe of one size is necessary in order to effect the high mold cost.

Where the process is applicable within number and size limitations, it is in competition with the sand mold centrifugal process. The quality and production rate potentials are usually too great for static sand casting to overcome in the field. The quality and high production rate also have somewhat of an advantage over the sand-lined mold method, but this process does not require such a large expenditure for the machine or mold and the pipe does not have to be annealed after casting. Both centrifugal processes have the advantage of nearly 100 per cent casting yield, i.e. no metal required for gates or risers.

Manufacture of Gun Tubes

Because of the long, thin cylindrical shape of gun tubes, this product is suitable for manufacture by the true, horizontal, centrifugal casting process. The thick wall of the tubes compared to cast iron pipe not only interposes many problems but requires the exclusive use of metal molds. About 1928 at Watertown Arsenal and reported to be about that time in Germany,[15] experiments were started that led to the independent development of two satisfactory techniques for gun tube and thick-wall steel cylinder manufacture.

The Watertown Process consists of pouring molten steel at a slow rate into the larger or breech end of a horizontal, centrifugal casting machine. Molten steel of the desired analysis and temperature is melted, alloyed, and brought to pouring temperature in a high-frequency electric induction furnace. It is poured

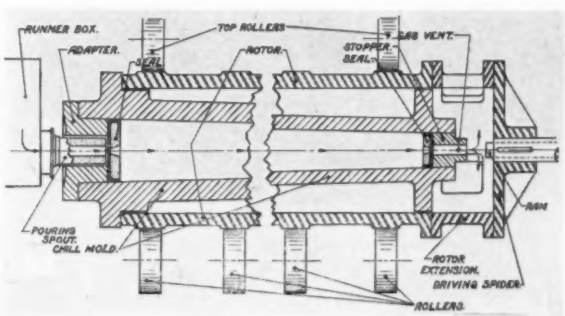


Fig. 2—Diagrammatic sketch of centrifugal casting machine for gun tube production.

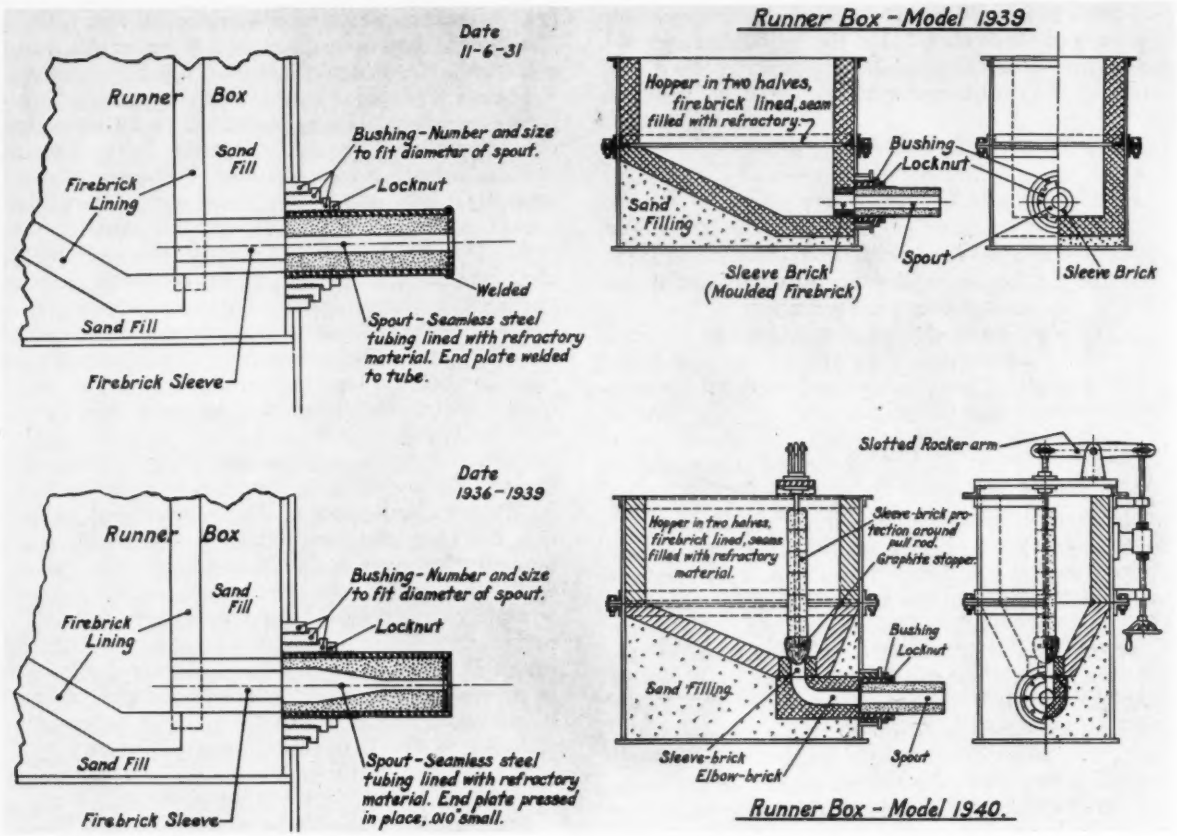


Fig. 3—Section view of pouring boxes for gun tube production (Martin).

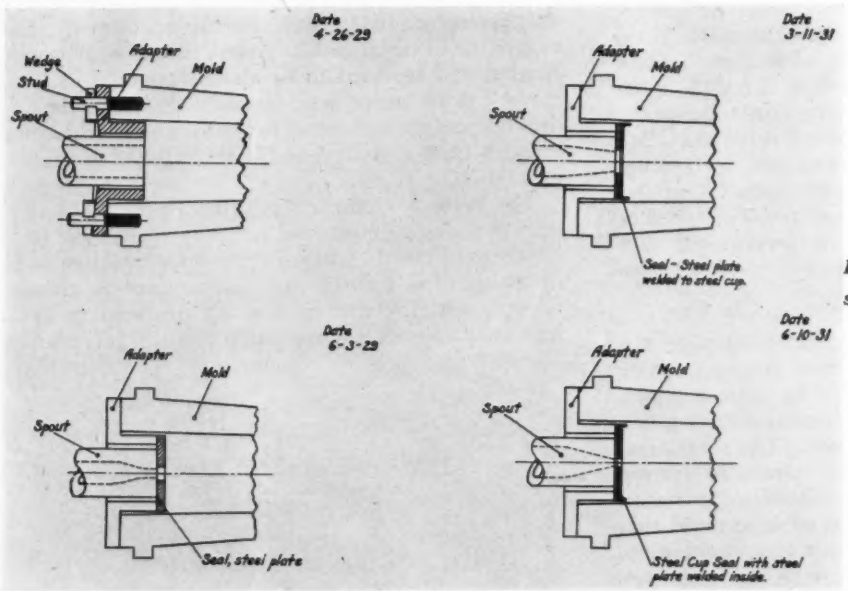


Fig. 4—Pouring spout and breech seal assembly view for gun tube production (Martin).

into the rotating mold through a refractory-lined pouring box with a refractory spout on the bottom front section. The spout is small in diameter (¾ in. or smaller) and is placed so that it is almost in contact with the breech refractory seal and located at the rotational axis of the mold. The mold is a heavy cast iron type that extracts the heat from the casting while spinning and is cooled between casts by air on the outer surfaces. The inner surface of the mold is sprayed lightly with a refractory wash before each pour. The mold is placed in a heavy steel rotor that rotates on several rollers under the force imparted by

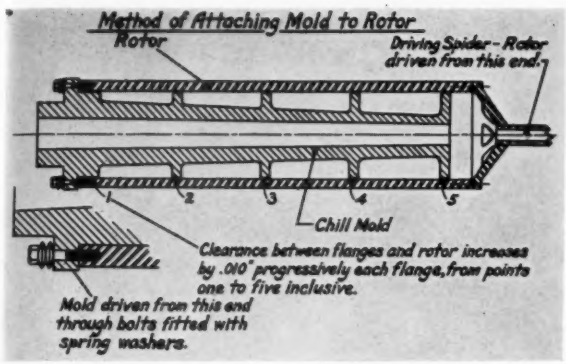


Fig. 5—Details of gun tube centrifugal casting machine construction (Martin).

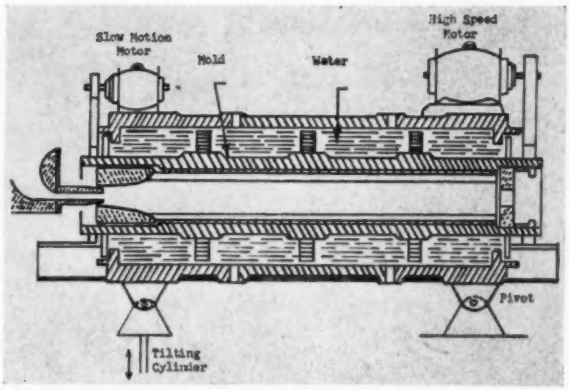


Fig. 7—Sectional view of water-cooled German centrifugal casting machine.

an electric motor drive. Two rollers are also positioned at the top of the rotor to prevent bouncing during the spin. The mold is usually rotated at a constant speed of from 1000 to 1400 rpm until all the metal has been poured to impart a force of approximately 30 g's to the inner surface of the bore.

A diagrammatic drawing of the machine construction is shown in Fig. 2, indicating the essential features of the machine. It is to be noted that the castings are removed by means of a pneumatic-hydraulic ram located at the driving end of the machine. Casting machines of this type are utilized for the production of gun tubes of from 4 to 20 in. in outside diameter and 7 to 25 ft in length. Figure 3[16] demonstrates the types of refractory pouring boxes utilized; Figure 4[16] shows various methods of sealing the pouring end of the mold and Fig. 5[16] indicates the mechanism used to attach the steel rotor to the drive shaft and cast iron mold to the rotor. A photograph indicating the overall arrangement in pouring position of all the equipment is shown in Fig. 6.

The usual German process in horizontal axis machines differs from the Watertown method in several important respects. The mold is of a thin-walled, low alloy steel type and rotates in a water jacket throughout the cast. The tubes are poured at the smaller or muzzle end and tilted slightly downward at the nonpouring end (about 5 degrees) during the pour. The mold is not sprayed to prevent fusion, but a thin layer of dry, fine, white silica sand is spread on to the mold surface by means of a special, movable trough and held at this location by centrifugal force. The molten metal does not impinge directly on the mold but enters by means of a refractory funnel positioned at the pouring end. A sketch of the construction of this machine is contained in Fig. 7.

The gun tubes manufactured by both the Watertown and German methods require machining of metal from both the inside and outside of the tubes to remove segregated or defective material and heat treatment to develop the optimum properties. The alloy steel tubes are extracted from the casting machines at about 1600 F and must be slowly cooled to prevent flaking. Following this, the tubes are normalized and tempered, turned and bored to remove defects and to bring to desired dimensions, and then heat treated by liquid quenching and tempering. The metal at the external surface of the casting contains laps, scale, and adhering sand with the German process. The metal at the inside diameter contains the lighter segregated elements, large non-metallic inclusions, and slag forced to the inner wall. Consequently, this metal must be removed to obtain desired results.

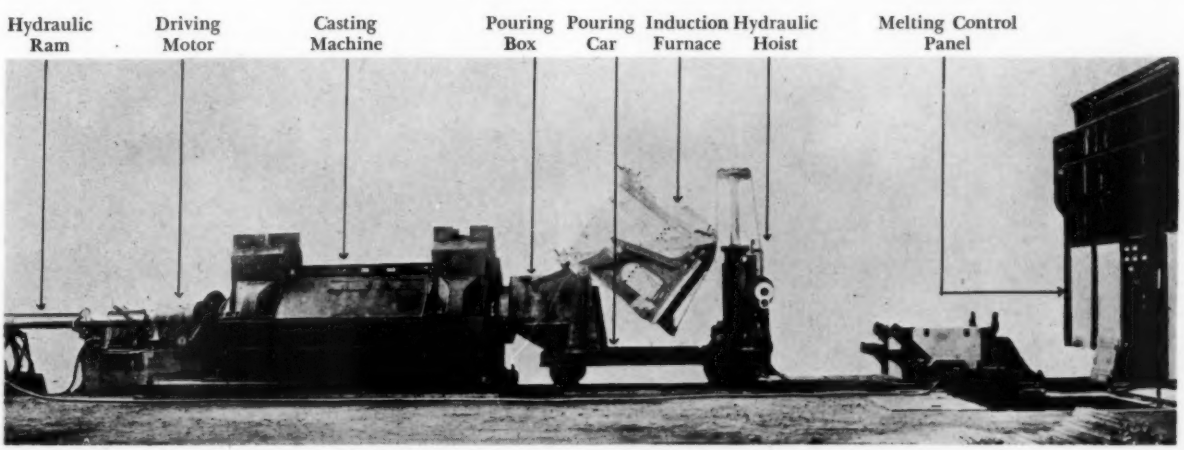


Fig. 6—Centrifugal casting machine equipment in operating position.

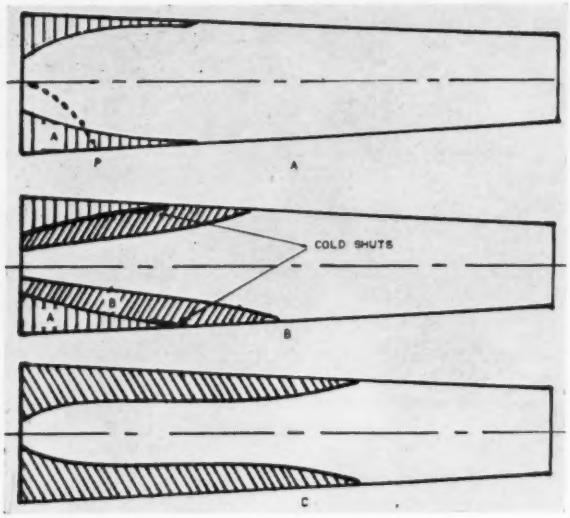


Fig. 8—Diagram of layer formation in production of cold shuts of laps.

The manufacture of tubes by either process is limited to the size and number of castings that can be economically produced. Tubes with a large outside diameter (over 20 in.) are difficult to produce without longitudinal hot tears.[17] The process cannot economically compete with forging and rolling methods under 4 in. in diameter of tube. Since the metal is only poured from one end of the tube during this process, it is not usually desirable to attempt to manufacture castings much in excess of 25 ft in length. Castings greater than this length have such deep laps that must be removed by machining that the tubes have to be cast with too much additional stock for economic production, and the mechanical problem of controlling the machine becomes serious.

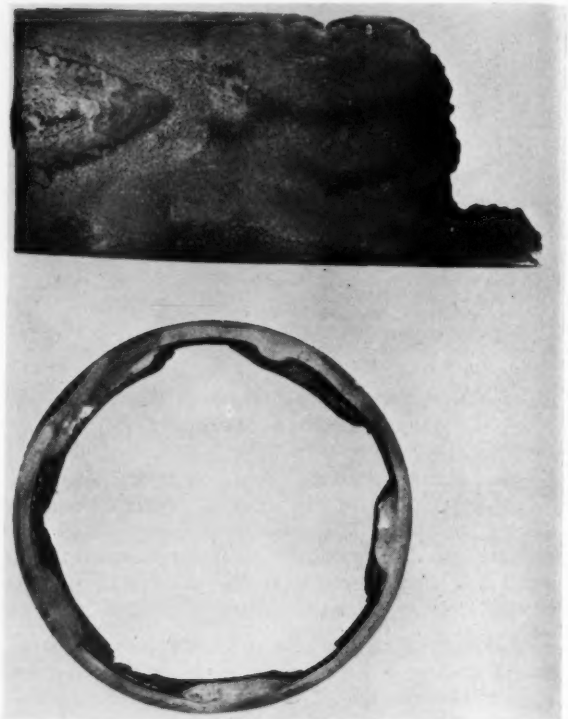


Fig. 10—Macro-etched tube sections indicating layer and lap formation in centrifugal castings.

The quality of centrifugally cast gun tubes is such that they are the equal of properly manufactured gun forgings in every respect. The tensile and impact properties of centrifugal castings is as high as forgings in the radial and transverse directions of principal stress. It is necessary to use high quality steel, however, to assure these excellent results. The action of

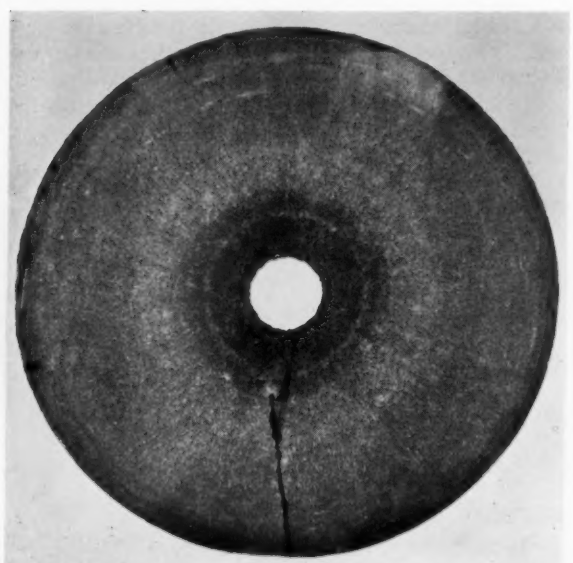


Fig. 9—Transverse macro-etched disc of centrifugal casting showing longitudinal hot tear.



Fig. 11—Cold shut contained in transverse macro-etched disc of centrifugal casting.

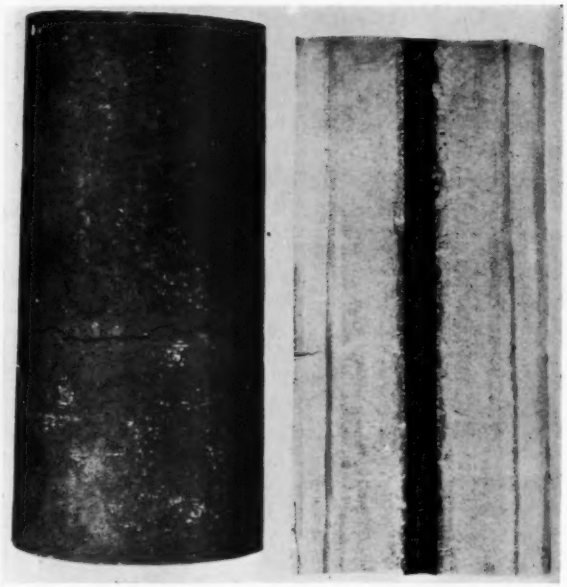


Fig. 12—Transverse hot tear shown in longitudinal centrifugal cast tube section.

the centrifugal force on the metal assures a complete absence of gas pockets, and the centrifuging of the larger non-metallics to the inner wall. The chill mold utilized produces rapid solidification with a minimum of macro and micro segregation and the assurance of excellent unidirectional solidification to eliminate shrinkage or micro porosity.

The occurrence and prevention of defects in large steel, thick-walled, centrifugal castings is a considerable problem. Considerable space, therefore, will be devoted to describing these defects, their causes, and prevention. The same types of defects are found in

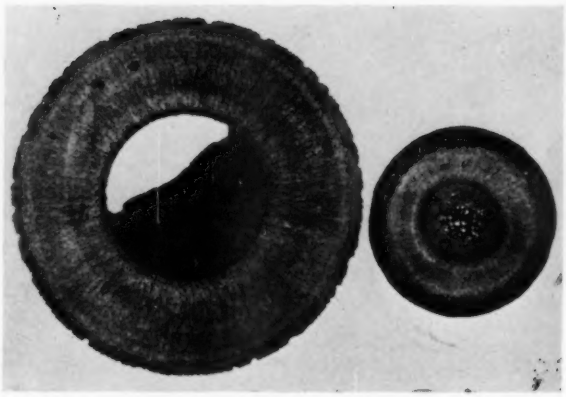


Fig. 14—Transverse macro-etched disc of centrifugal casting with spin stopped abruptly.

tubes produced by both the Watertown and German methods.[18] Since these tubes are poured from only one end, a method of spreading the metal over the mold surface evenly and rapidly has to be obtained to prevent the formation of deep laps or cold shuts between successive layers or waves of molten metal proceeding from the pouring to the non-pouring end.

Figure 8 contains a diagram of the layer or wave formation that produces the laps or cold shuts. The spreading of metal over the length of the mold is improved by faster rates of pour and higher speeds of rotation. However, increasing these variables beyond a certain critical amount will cause longitudinal and spiral hot tears in the casting. These hot tears are caused by the internal pressure of the molten steel, under the action of centrifugal force, acting against the solidified outer shell of steel. This solidified shell shrinks away from the supporting mold wall due to solidification contraction and has low strength and

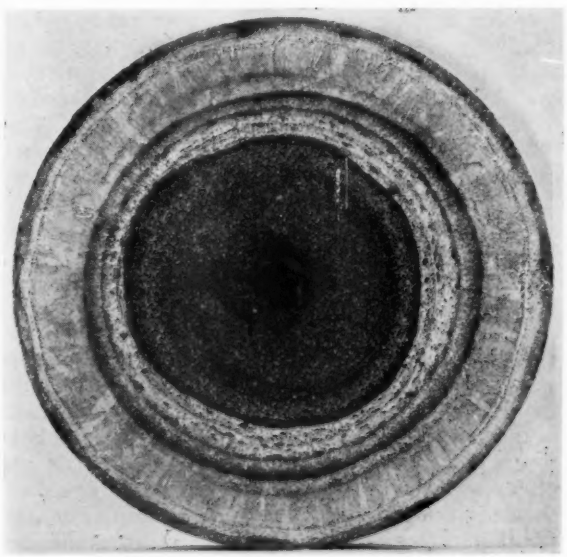


Fig. 13—Transverse macro-etched disc showing center porosity from non-unidirectional solidification or machine failure.



Fig. 15—Transverse macro-etched disc from centrifugal casting showing pitted structure (Smith).

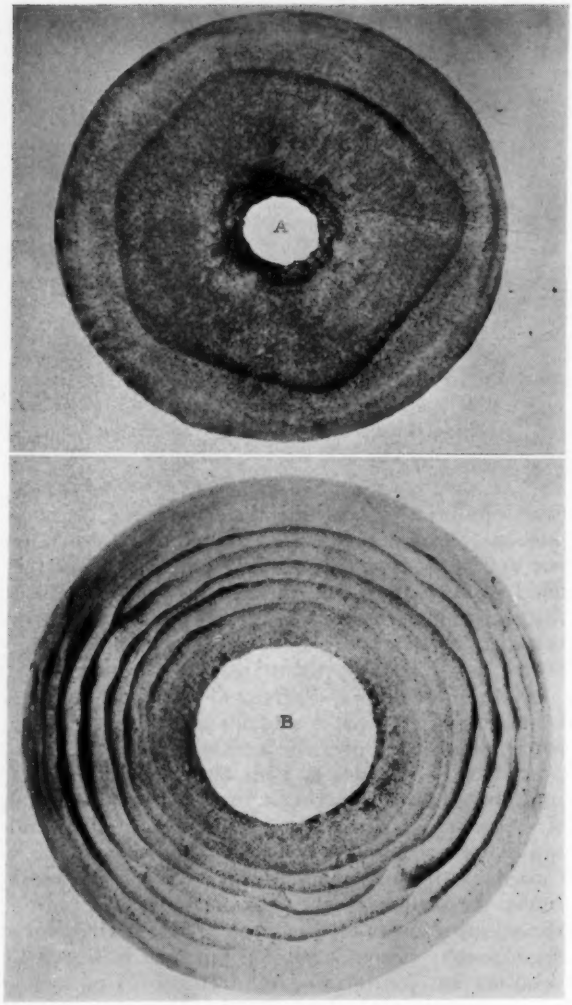


Fig. 16—As-cast macro-etched disc from centrifugally cast gun tubes showing (A) odd-shaped segregation ring and (B) multiple segregation rings (Smith and Bender).

insufficient ductility to permit yielding at the higher steel temperatures.[17] High internal pressures must be avoided, then, by keeping the rate of pour and speed of rotation as low as is conducive with the elimination of serious laps. The manufacture of acceptable centrifugally cast, thick-walled tubes, then, requires an accurate, technically controlled balance between the rate of pour and speed of rotation.

Photographic evidence of the types of defects which occur are shown in Figs. 9, 10 and 11. Figure 9 demonstrates a deep longitudinal hot tear as it appears in a transverse macro-etched disc sectioned from the casting. Further indication of the layer formation causing cold shuts and the lack of fusion at these layers is shown in Fig. 10. The top view of this figure contains a longitudinal section from this tube and the bottom of this photograph is a view of a transverse section from the tube. In Fig. 10, the pouring of the casting was interrupted and these photographs of the end of the tube farthest from the pouring end were taken to demonstrate the cause and effect of cold shuts. The appearance of a cold shut or lap in a transverse section of a complete tube is shown in Fig. 11.

Some other defects also occur in centrifugally cast gun tubes that are of sufficient importance to warrant discussion. These include the transverse hot tear and the undesirable segregation of elements. Transverse hot tears are produced at a similar period during the casting as longitudinal hot tears but have a different cause. These defects result from the inability of the casting to freely contract during the cooling period immediately following solidification. If the casting becomes fastened at both ends or at one end and along the length, it will tear itself apart during this period. This problem was so severe in the Watertown method that equipment was designed to apply approximately 1000 lb to the smaller or muzzle end as soon as the solidified shell can support this pressure. This force is maintained by the ram throughout the solidification and cooling period to assist contraction and reduce transverse hot tears. A photograph of a longitudinal section of a centrifugal casting containing a transverse crack is shown in Fig. 12.

Segregation of Non-metallics

Some segregation of the lighter elements and non-metallics to the inner surface of centrifugal castings is to be anticipated and desired. This normal type of segregation can be observed at the center of the macro-etched disc in Fig. 9. The segregated center of these tubes contains approximately 1½ times more carbon and a considerably higher phosphorus and sulphur content than the remainder of the tube, in addition to a large amount of non-metallics. If this segregation becomes marked outside this inner wall, however, the quality of the tube is reduced. This marked segregation is the result of either improper machine operation, large machine vibrations, or too high a pouring temperature. Photographic evidence of the influence of these factors on transverse macro-etched discs cut from the tubes are shown in Figs. 13, 14, 15 and 16. The result of a machine breakdown that caused a reduction in the speed of rotation before complete solidification is depicted in Fig. 13. The center of this casting is porous because of improper directional solidification under centrifugal pressure. The influence of an abrupt casting machine stoppage after pouring had been completed and solidification initiated is indicated in Fig. 14.

Machine vibration, the establishment of resonant frequencies, and lack of proper dynamic balance result in two types of castings that do not represent the optimum in casting quality. The first of these types always demonstrates a larger "as cast" central bore in the casting at the muzzle or non-pouring end than at the breech or pouring end. This difference in hole size is accompanied by segregated dark spots in the center of the tube as shown in Fig. 15.[19] The spots shown in this disc contain segregations of carbon, sulphur, phosphorus, and non-metallic inclusions. Severe machine vibrations also produce circumferential carbon, phosphorus, and sulphur segregation rings during the solidification of a casting. These rings become more frequent and pronounced as the vibration

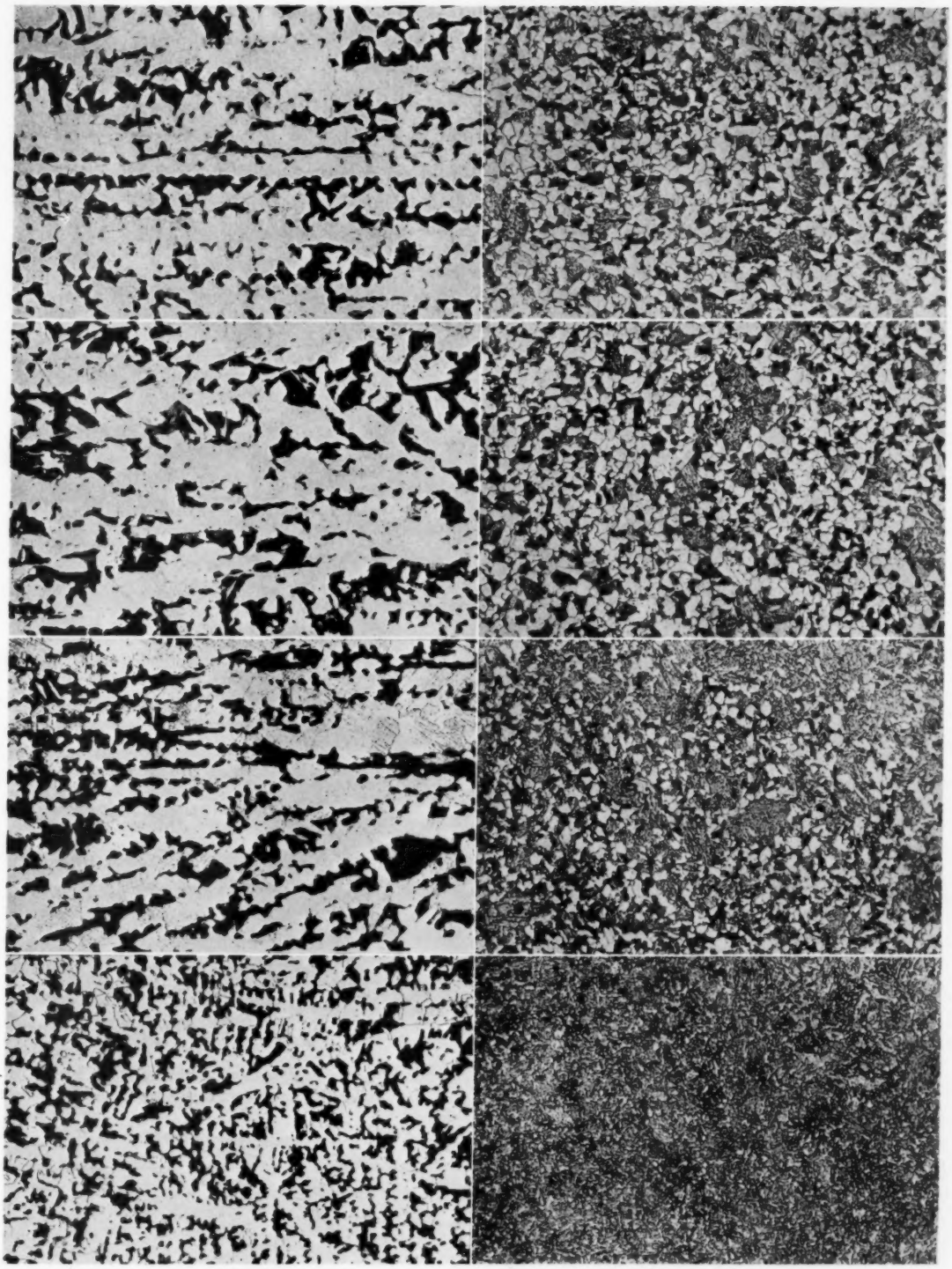


Fig. 17 (Left) and Fig. 18 (Right)—Microstructure of centrifugally cast tube at various diameters. Nital etch. Mag. 25x.

becomes more severe and even show irregular patterns due to faulty machine operation as indicated by the photographs of the transverse macro-etched discs in Fig. 16.[20]

The macrostructure of centrifugal castings clearly indicates that they contain a dendritic structure from the outside to center. The microstructure is similar to high quality, rapidly solidified, statically cast steel. The grains are equiaxed; the inclusions follow the usual rules for casting; and the reaction to heat treatment is similar to other steels. Photomicrographs are included to demonstrate the "as cast" and quenched

and drawn microstructures obtained at various transverse diameters on a large centrifugal casting of low carbon-chromium-molybdenum-vanadium steel.

Figure 17 demonstrates the large dendrites in the "as cast" microstructure of these tubes shown at twenty-five times magnification. The dendrites are normal for a heavy chill cast section; the smaller dendrites near the outside show the influence of the rapid solidification. The influence of a water quench from 1600 F and subsequent temper is shown in Fig. 18. The grains are refined very appreciably by this process and the influence of the quench in producing lower temperature transformation products near the outer surface is indicated. All of these structures are normal cast structures and indicate the usual reaction to heat treatment for this steel.

As mentioned previously, the centrifugal casting of gun tubes is in direct competition with the steel forging process. When tubes are made by the usual open die method, centrifugal casting has the advantage of lower initial investment, less basic and auxiliary equipment, reduced man power per tube, a smaller initial melt of steel and the smaller plant space needed. The process has the disadvantages compared to conventional forgings of less adaptability to various sizes, higher cost of raw material, higher melting charges, and the necessity for closer technical control.

If a high yield of acceptable tubes can be obtained from the centrifugal casting process, the cost factors are such that for tubes of 1000 to 5000 lb in weight, the centrifugal casting process has a large economic advantage. The problem is primarily one of obtaining and maintaining a satisfactory technique so that this high percentage of acceptable product is attained. Claims have been made of up to 95 per cent acceptable gun tubes cast by the German process with the thin sand coating on a metal mold. This figure is somewhat in excess of the present United States ratio of acceptable tubes, but the process is definitely competitive with forgings.

The yield of the metal melted that appears in the final product is also a serious consideration in assessing the economics of any process. Because the centrifugal casting equipment is designed for and is producing only a single type of product, the percentage of the metal melted that appears in the final product is relatively high. Forgings, on the other hand, must melt additional weight for the same product weight. The casting yield of final versus melted weight is about 56 per cent, whereas for forging it is approximately 40 per cent for medium-sized tubes. It should be pointed out, however, that each process requires this additional metal to assure the high quality of the product. The metal in the bore is the last to solidify and therefore acts as a riser to feed remainder of the tube. The metal on the outer surface contains laps, sand, etc. About 10 to 15 per cent of the total weight of the tube is usually bored from the center and the outside is machined down to clear metal.

Manufacture of Other Tubular Products

While cast iron pipe and gun tubes require special attention because of the large tonnages of centrifugal castings produced, several other castings are also made by centrifugal means in horizontal spindle, metal mold casting machines. These items include long tubes similar to pipe and many short tubular products. The types of short castings that are produced are: radial engine cylinder barrels, cylinder sleeves, bearing blocks, and other short tubes. The long castings in production include: cylindrical parts for aircraft landing gear, heat and corrosion resistant steel tubes, cylindrical liners with thin walls, and non-ferrous metal cylinders. The manufacture of these products is described in the technical literature.[2,3,15,21,22]

The centrifugal casting machines used for the manufacture of these tubular products are similar in most respects to those employed for the production of gun tubes and cast iron pipe. Since these products are not as heavy or as specialized, the machines tend to be more simplified in nature. The short cylinders and barrels manufactured can be produced with relatively simple equipment that usually consists of a machine in which a small horsepower motor drives rolls on which the metal mold is located.

One interesting innovation is the removable mold used in cylinder liner production in which the metal mold is replaced in the machine by other similar molds as soon as the casting is solidified. The casting practice utilized for this product varies from one type of casting to another and from plant to plant. Some of the larger molds are water cooled; the majority are air cooled for these lighter tubes. The speeds of rotation do not follow any fixed formula, but in order to assure removal of the gas and non-metallics and a smooth inside surface, a speed of about 60 g's at the inside casting diameter is employed. The metal is poured into the mold by means of a spout that usually is directed a short distance inside of the mold. The mold is customarily spinning at top speed during the pour in order to "pick up" the molten metal without slipping. The casting quickly solidifies in these metal molds and is extracted hot. The mold is sprayed or washed and the next pouring resumed. The mold spray or wash is used to prevent fusion of the casting to the mold, increase mold life, and prevent lap formation.

Manufacture of Composite Rings

One recent development in the centrifugal casting of tubes is the manufacture of composite castings. These castings contain one type of metal on the outside and another metal on the inside. The metals utilized consist of cast iron, stainless steel, carbon steel, and copper-base alloys. The technique of manufacture consists of pouring the external metal from one end of the mold, adding a heated, liquid brazing flux into the inside bore of the first metal as soon as it solidifies, and pouring the core metal from the second end into the inside of the cylinder.

Tubes up to 9 in. in diameter and 8 ft in length have been cast in this manner. This work has been conducted both in the United States and Germany.[15,22] The bond obtained is usually excellent and the mixture of the two metals very slight. These composite tubes can be employed to conserve critical materials by using the expensive alloys only in the required locations. They also can furnish a combination of strength and heat- or corrosion-resistant alloys

required for specific applications. Centrifugally-cast composite tubes can be rolled into tubing by successive stages.

The manufacture of these tubular products is subject to the same type of defects experienced in gun tube manufacture. Transverse and longitudinal hot tears, laps, segregation, and porosity of the same type as described for gun tubes occur for the same causes and can be prevented by similar methods. One type of defect does occur, however, that deserves special mention. In order to assure a completely sound, uniform density of metal, solidification must be progressive from the outer surface to inside core. In this manner the solidification contraction of each layer is compensated by the still liquid inner layers and shrinkage is avoided.

If, however, as is sometimes the case in the thick, short tubes and liners produced in the small horizontal spindle machines the metal is poured rapidly and considerable cooling occurs at the inner surface, shrinkage in the casting does result. An example of how this defect influences the structure was shown in Fig. 13. The inner porosity is produced because solidification occurs from both the inner and outer surfaces simultaneously and the last metal to feed inside the casting exhibits this shrinkage. This type of defect is prevented by either pouring so slowly during rotation that solidification is unidirectional or placing an exothermic or insulating material on the inside surface of the metal immediately after pouring has been completed.

Manufacture of Thin Metal Tubes

Thin metal tubes produced in metal molds with a horizontal spindle can be manufactured free from shrinkage without the necessity of insulating or exothermic internal coatings. These tubes are cast without the centerline shrinkage so prevalent in thin sections of statically cast metal. Consequently, much thinner, sound tubes can be made centrifugally than by the static method and the yield of castings in relation to weight of metal melted is much higher for the centrifugal process. It is pointed out, however, that the thin tubes must be cast rapidly to avoid laps or cold shuts and that lighter non-metallic particles do not have an opportunity to migrate as completely to the center of the casting as in the case of heavier tubes. The occurrence of blow-holes is rare except for castings utilizing a gas-producing mold wash since this gas is usually driven off by the centrifugal force in even thin tubes. Additional treatment of these thin castings is usually confined to inspection, heat treatment to the desired mechanical properties, and surface blasting.

The true centrifugal casting of non-ferrous metals in metal molds with horizontal axis machines is also very widely employed. The metals include the brasses, bronzes, aluminum bronzes, leaded bronzes, and bearing metals. Light metals are not usually produced by this type of centrifugal casting. The centrifugal casting of non-ferrous metals is, generally speaking, a less critical procedure than ferrous metal casting. These metals are not as subject to hot tears, metalloid segregation, or refractory problems as ferrous metals. Segregation is a problem in leaded bronzes, however, and it is necessary to use metal chill molds to prevent the serious segregation of the lead.[23] It is also frequently necessary to employ a special gaseous atmosphere to eliminate drossing.

Centrifugal Casting Non-ferrous Metals

Because of the reduction of hot tearing and refractory problems, much larger size tubes can be produced without difficulty with laps and hot tears by more rapid pouring. The literature contains references[22] to the manufacture of brass and bronze cylinders up to 8 ft in diameter and 13 ft in length and tubes of 3 ft in diameter and 40 ft in length. The limitation appears to be the size of the furnace and centrifugal casting equipment rather than any serious problems in casting technique. The literature records castings of as heavy as 22 tons produced when required for a given application. It is necessary in the case of the drossing alloys to obtain a smooth even flow in the mold. The three fundamental rules for the successful centrifugal casting applicable to non-ferrous metals have been listed by Zuehlke[6] as:

1) A uniform and quick distribution of molten metal over the entire surface of the mold.

2) Progressive solidification of the metal from the mold surface towards the center.

3) Final accumulation of all dirt and impurities in the bore where they can be removed by machining. Compliance with these three rules and the assurance of a satisfactory product is not a particularly difficult requirement in non-ferrous metals for a shop with adequate controls and satisfactory equipment.

In order to determine the economic feasibility of centrifugal casting, it is necessary to directly compare the process with forging and static casting. The quality of steel centrifugal castings is similar to that of forged material and superior to the statically cast product; non-ferrous centrifugal castings are of higher quality than static castings, and approach, but are not quite as high as the properties of forged or extruded material. The initial investment for the equipment required is highest for forgings and lowest for static castings. Centrifugal castings require somewhat more of an investment than static castings and, therefore, centrifugal casting requires a reasonably large quantity for manufacture or compliance with a rigid quality specification. Centrifugal casting can also produce flanges and irregular contours on the tubes that are not always feasible in conventional forging operations. In addition, the casting yield is considerably higher by centrifugal than by static methods resulting in an appreciable saving per casting. The economic advantage of any one process must necessarily depend in the factors such as quantity, quality, dimensions, and initial investment requirements of a particular operation.

True Centrifugal Casting—Horizontal Axis—Sand Molds

Manufacture of Cast Iron Pipe and Steel Tubes

Cast iron pipe is produced by the so-called "sand spun"[24] process in sand-lined molds as well as in the metal molds. The same sand-lined mold equipment utilized for cast iron pipe production is also employed for the manufacture of centrifugally cast steel tubing.

The sand-lined mold method differs from the metal mold in a number of important respects. Each process has its advantages and disadvantages and the utilization of each depends on the requirements of the pipe or tubing being manufactured.

A metal mold can be used more economically when a large number of identical castings are to be produced; a sand-lined mold permits considerable and frequent variation in the dimensions of the tube or pipe. Tubes produced in a metal mold must be of a design that can be readily removed from the mold without interference, whereas castings made in a sand-lined mold can be more complex in shape. Metal molds impart a fine grain size and high quality from the rapid solidification, but sand-lined molds give a more uniform, slower cooling rate with a more similar "as cast" structure in all sections of the casting. Sand molds have the advantage of cooling the metal less than metal molds and accordingly, metal can be run much further distances without the formation of laps or cold shuts. The solidification times in the sand mold are considerably longer than metal molds, however, and the spinning and cooling times per cast are much longer. This longer liquid period increases segregation, but also assists in the removal of the lighter, non-metallic materials into the inside of the tube where they can be subsequently removed.

The pipe or tubing is cast by pouring into one end of a metal flask that has been previously rammed to form a mold. The thickness of the sand is maintained between 1½ and 2 in.[9] The metal flask is rammed with a sand lining or the sand forced into place in a vertical position on a metal stool. The diameter of the metal pattern forms the outside of the pipe or tube plus allowances for shrinkage and finishing. The mold is usually rammed with tempered synthetic sand, faced with a silica flour wash, and dried with gas before use. The mold is placed in the machine, the end cores positioned, and the molten metal poured. The flask usually runs on and is driven by two sets of friction rollers, one at either end of the machine as shown in Fig. 19.

The molten metal is introduced very rapidly into the flask while the mold is being operated at full spinning speed. The metal spreads rapidly over the mold without slipping or "raining" since the speed has been calculated to preclude this possibility. Speeds of rotation sufficient to obtain approximately 75g's at the inside surface of the casting are used for sand molds. This speed is somewhat higher than for metal mold because of the greater tendency of the molten metal to "rain" in a sand mold. Castings of from 4 to 36 in. in diameter, ½ to 4 in. in thickness, and 16 ft in length have been produced by this method.

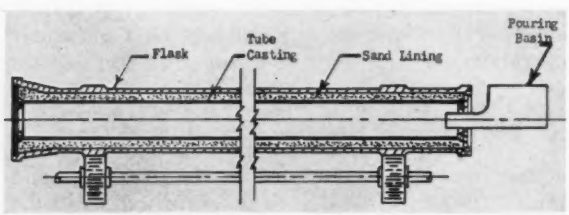


Fig. 19—Horizontal axis machine for production of true centrifugal castings in sand-lined molds.

The rate of production of the sand-lined machine is relatively slow compared to the metal mold process because of the slower solidification rate. The time the casting must spin varies with the diameter and thickness but it is usually about 10 times longer than in metal molds. In addition, the castings are usually left in the flask for a considerable period after the spinning has stopped in order to permit it to cool to lower temperatures. In this way, an effective anneal is obtained on the cast iron pipe or steel tubing. The longer solidification and cooling time necessitates a larger number of machines and a considerably larger number of flasks for each machine than for comparable production with metal molds.

The additional treatment and machining required for centrifugal castings produced in sand-lined molds is generally less than for castings produced in a metal mold because of the effective annealing by slow cooling and the reduction of laps and surface defects in the steel tubes.

The mechanical and metallurgical properties of castings produced in sand-lined molds compares favorably with static sand castings. The centrifugal force drives many of the impurities to the center of the cylinder or pipe, eliminates blow holes or gaseous defects, and insures sound, well-fed metal if unidirectional shrinkage is maintained. It should be stated, however, that any claims that centrifugal casting "works" the metal or increases the density are false. The centrifugal casting process eliminates some defects and facilitates the detection of those which remain by locating the remaining defects at the outer surfaces. However, the slower rate of solidification in sand molds does result in increased segregation and reduces metal quality to somewhat less than that attained by centrifugal casting in metal molds or by forging. The product is, like metal mold casting, non-directional, and this advantageous over forgings for some purposes.

The defects encountered are directly similar to those experienced in metal mold centrifugal casting except for the added segregation. Transverse and longitudinal hot tears, irregularities because of machine vibration, refractory breakdowns, and porosity due to the lack of uni-directional solidification are all experienced. The casting yield is very high by this process.

True Centrifugal Casting—Vertical or Inclined Axis

While the majority of true centrifugal castings are produced in machines rotating about a horizontal axis, true centrifugal castings are in some cases spun about vertical or inclined axis.[25] True centrifugal castings are again defined as castings spun about their own axes where at least part of the useful interior surface is shaped by centrifugal force without a central core. Small gear blanks, cluster gears, bushings, and liners such as produced at the Ford Motor Co. in metal molds[2,25,26] are manufactured by this method. Although there are some special cases,[11] true centrifugal casting about vertical or inclined axes is rarely conducted in sand molds. The high speeds of rotation required to maintain the sides of the central hole

nearly parallel increases the possibility of sand breakdown and the usual requirement for a short length eliminates the necessity of a sand lining to permit the flow of molten metal for long distances without chilling.

The central cavity formed by spinning about any other axis than the horizontal is paraboloidal. The shape of the cavity may be calculated by the following, which is the equation of the parabola at a section through the line of the axis:[3]

$$H = \left(\frac{.0000142\ N^2}{\sin\ B}\right) R^2$$

where H = height above vertex in inches
N = spinning speed in revolutions per minute
B = angle of inclination of axis in degrees
R = radius of cavity in inches

Experimental work has shown that the calculation described above results in an accurate representation of the actual cavity obtained during spinning except for contraction and piping.[3] It is to be noted that when B becomes 90 degrees (for castings rotated about a horizontal axis), sin B is equal to unity, H becomes equal to R, and the internal surface of the casting becomes a cylinder.

The type of vertical or inclined axis machine utilized for true centrifugal casting is shown in Fig. 20.[6] This figure indicates schematically the various parts required. The mold, A, of either metal or sand is rotated rapidly within the metal flask or rotor, B. The motor or flask is spun by a shaft held by bearings and driven by a belt or gear arrangement. A removable cover, C, is placed on top of the mold with a smaller hole than that to which the casting is poured. The metal enters the mold through this small hole and is kept within the mold cavity by this cover. The cover is removed easily when the casting has solidified in order to facilitate stripping. Fly ball weights, D, or bolts are used to hold the cover in position during the spin. A pouring spout, E, is necessary to carefully direct the molten metal through the hole in the cover and into the mold. All molten metal is directed so that it impinges entirely on the bottom of the mold. A guard, F, is always used to contain any metal or parts which fly off under centrifugal force.[6]

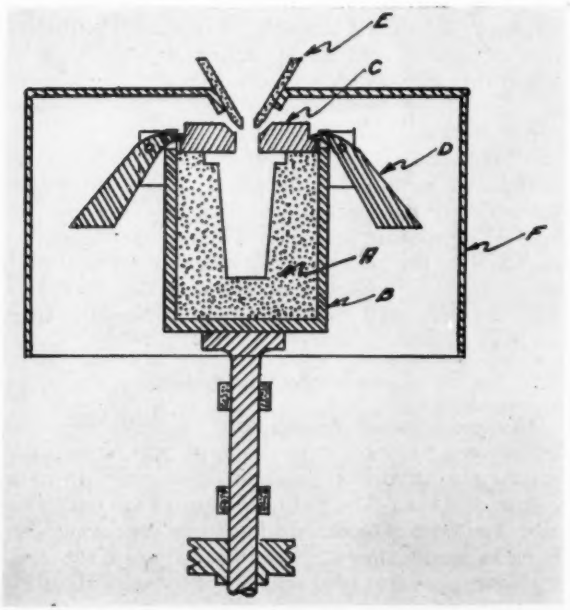


Fig. 20—Sketch of vertical centrifugal casting machine (Zuehlke).

Molten metal is poured when the casting machine is rotating at either reduced or at maximum speed. Under the action of centrifugal force, the molten metal moves rapidly up the sides of the mold. The inner surface of the casting is a paraboloid determined by the equation previously discussed; the outer surface is determined by the inside face of the mold. The speed of rotation employed is dictated by the dimensions of the casting. The amount of taper of the paraboloid on the inner surface depends upon the speed of rotation and since most work requires a nearly cylindrical wall, the speeds of rotation are necessarily high. Zuehlke[6] states that the spinning speed should produce a centrifugal force on the inner surface of the casting of 100 g's to insure a small taper and a high quality product. This applies to thin-walled castings and if castings are produced with very thick walls, the centrifugal force at the outside can become dangerously high. Under these circumstances, it is frequently necessary to reduce the speed, even at the expense of too great a taper of the internal surface.

The pouring operation is very important in vertical or inclined axis casting. The metal is directed at the center of the bottom and introduced very rapidly so that it will remain one liquid mass for as long a time as possible. It is desirable to maintain the flow of metal with a minimum disturbance. When the metal is directed at the center of the bottom, this condition is obtained. The metal will remain at the bottom of the mold until it is accelerated and, then will rise up the side walls of the mold in an even rush. The slag and dross is carried to the top or inner top surface of the casting from where it can be easily removed.

Cold shuts or laps are a problem for the production

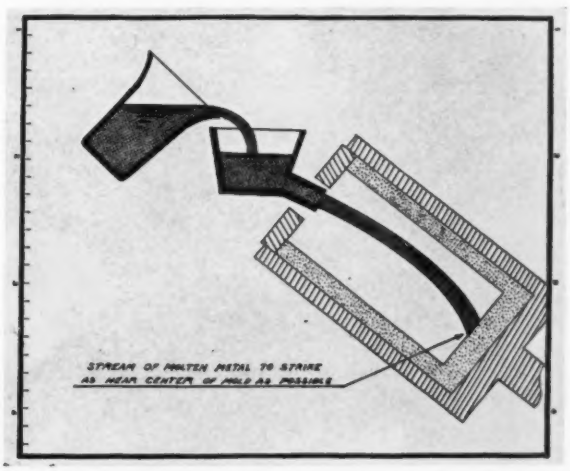


Fig. 21—Diagram of pouring method in inclined axis mold (Zuehlke).

of long or large castings by this method. Since this size of casting cannot be poured entirely within a very short period, it is frequently necessary to pour a considerable amount of metal when the machine is rotating slowly. After a considerable portion of the charge has been poured, the machine is accelerated to maximum speed and the pouring is continued. The practice provides sufficient metal to cover the mold surface in the first rush up the walls and the additional metal is introduced rapidly enough to prevent laps.

The cylindrical internal surface obtained with the horizontal axis machine is preferred usually for most products, but a vertical machine has advantages of faster metal introduction and improved metal distribution that make the vertical method desirable in many cases for smaller castings. A compromise between the two methods can be obtained by utilizing an axis inclined from the vertical. The taper on the internal surface becomes less as the horizontal is approached, but the difficulty in rapid pouring and good metal distribution increases. The diagrammatic sketch contained in Fig. 21[6] indicates the method that has to be used with centrifugal casting in a mold at an inclined axis to insure the proper method of pouring. It is at once evident that the centrifugal force must be high at the start of the pour in order to insure uniformity in metal distribution. This eliminates the possibility of the accelerated pour previously discussed.

Gun tubes were produced in vertical centrifugal casting machines of the heavy walled, cast iron mold type in wartime Germany.[15,18,22] Castings with a length of from 6½ to 13 ft. and a diameter of 10 in. and smaller were in production. Molten steel was poured into the top at approximately 200 rpm and after the mold had been filled to about one-third of its length, the mold was accelerated to 1400 rpm for the remainder of the pour and spin. The metal was carefully directed towards the center of the bottom to avoid splashing on the side walls. The paraboloidal cavity at the top only extended down from 3 to 4 ft; the remainder of the tube was solid except for piping. The castings were bored out and finished in the same manner as the tubes cast in a horizontal mold and all piping was removed. The mold was coated with a wash consisting of fine quartzite, aluminum oxide, and bitum. This wash prevents fusion to the mold walls but causes a fine porosity on the outside surface of the castings as shown in Fig. 22. The removal of this porosity and the other surface defects requires the machining of approximately 0.7 in. from the outside radius[18]—a similiar amount to that required in horizontally cast gun tubes.

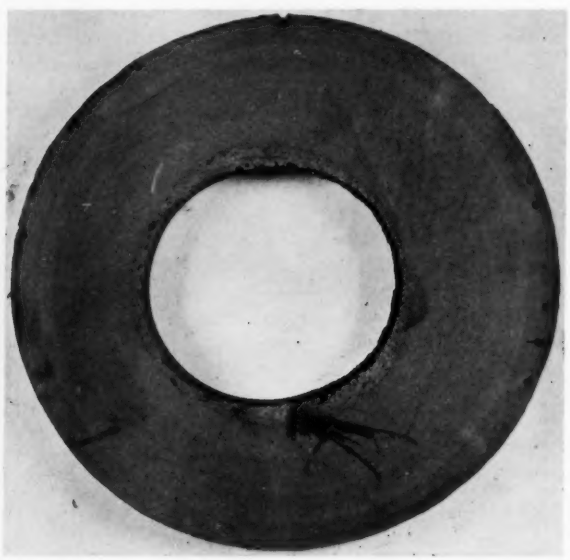


Fig. 22—Transverse macro-etched disc from German tube showing porosity near surface.

The mechanical and metallurgical properties of the castings produced in vertical or inclined axis molds by the true centrifugal casting process are similiar to those manufactured in the horizontal axis molds. The vertically cast gun tubes have been found to be the equal in quality with horizontally cast tubes.[15,18,22] The macro- and micro-structure and mechanical properties of the centrifugally cast gear blanks[26] are equal to forged gears except for the absence of flow lines and directionality. These flow lines are parallel to the lines of force in the gear so that the centrifugal cast product is superior for this application.

Centrifugal castings of non-ferrous metal produced in metal mold vertical axis machines exhibit the same relative properties compared to statically cast and forged materials as similiar metals cast in metal mold horizontal axis machines. The densities of the product of the various processes are similar, provided the sand casting is properly fed. The chemical analyses are uniform except for the tendency of lead to segregate to the outside of the heavier sections. The grain size of the centrifugally cast material is finer than the sand cast product because of the rapid solidification and does not exhibit the directional properties of forgings. The tensile strength, elongation, and reduction of area of centrifugal castings exhibit about a 10 per cent increase over sand castings[27] for a number of copper-base centrifugal castings. This difference varies somewhat from alloy to alloy but indicates this improvement on the average. The properties of centrifugal castings produced in sand-lined molds more nearly approach those of static sand casting. Except in specific instances where improvement is obtained by the better feeding or elimination of non-metallics, the properties of the centrifugal castings made in sand-lined molds and the static sand castings are very similar.

Economic Factors Involved

Numerous factors determine the relative economic advantages of vertical or inclined axis centrifugal casting compared to the competing forging and static casting processes. The facilities required are not elaborate for vertical centrifugal castings but somewhat more expensive than sand castings, although the smaller floor space area requirements of centrifugal casting may offset the greater expense. The initial investment for forgings is considerably higher than for sand castings. To make these investments economically feasible,

centrifugal castings require considerable volume of production and forgings need a very large volume. The casting yield of the centrifugal process is higher than sand casting and at least the equal of the forging method.[26] Cleaning room costs are lower for centrifugal compared to sand castings but higher than for forgings. Final machining costs of the centrifugal product compare favorably with sand castings but their relative position with respect to forging depends on the type of forging.

Semi-Centrifugal Casting

Semi-centrifugal casting is employed for the manufacture of castings of a circular, annular or disc-shaped type such as gears, wheels, tires, or plates. The mold is always rotated about the axis of the part and the centrifugal force exerts pressure from the center towards the rim that facilitates the filling of the mold and assists to some extent in directional solidification. The process differs from true centrifugal casting in that the entire shape of the casting including the internal surface is formed by the mold. The molds are almost always rotated about the vertical axis. The process is usually conducted in sand molds, although some special die molds with sand cores are employed by the Ford Motor Co.[2,28] With either sand or metal molds, the molds are frequently stacked several high around a centrally located sprue.

While semi-centrifugal casting has advantages over static sand casting, it does not benefit from many of the advantageous features of true centrifugal casting. Semi-centrifugal casting will cause metal to flow in thin sections of castings that will not fill properly in stationary molds. Suitable disc-shaped castings can be produced by single and particularly by stack-molding with a large increase in yield over conventional static means. The pressure of centrifugal force also assists directional solidification and the production of sound castings to some extent, but there are definite limitations of the influence of this pressure. The elimination of non-metallics and gas cavities obtained in true centrifugal castings is not experienced in semi-centrifugal casting. In addition, the unidirectional solidification present in true centrifugal castings cannot be obtained with the mold virtually surrounding the casting from all sides in the semi-centrifugal process.

The centrifugal casting machines used in this process consist of a flat, round table supported and rotated by a vertical shaft, that is driven by an electric motor. The size of table, motor, and bearings vary with the size and weight of the castings being produced.The sand molds and the flasks containing the molds or metal dies are fastened to the table with the axis of the molds coinciding with the axis of the table and shaft. The equipment is similar to that shown previously in Fig. 20 except for the mold construction. The molds are poured either singly or, where possible, several molds are stacked on top of each other about a central sprue. Figure 23[29] demonstrates how a stack of track wheels were positioned about the central sprue for the semi-centrifugal process. The sprue serves both as a gate and riser for these castings.

The molds utilized in the semi-centrifugal process are usually high strength, core sands or unusually hard, dried sand molds. Green sand does not have the required strength, although it has found some use[29] in special cases. A special application with large volume requirements and high quality are necessary to justify the cost of metal molds. In order to prevent serious erosion of the sections of the sand molds on which the metal impinges as it enters the mold cavity under centrifugal force, high strength cores, chamotte inserts, or nailing must be employed. Cement-bonded molds are utilized also for added strength.[11,15,29]

The speed of rotation is considerably slower in semi-centrifugal than true centrifugal casting because of the lower strength of the molds. The parting lines between the cope and drag and between each mold in a stack provide openings through which molten metal could run in a "break through" if the centrifugal pressure were too high. In addition, the mold itself would vibrate or fly apart under this force if the pressure were permitted to become excessive. For this reason, the speeds of rotation are usually calculated in terms of the speed of rotation of the periphery of the casting. This speed is maintained at between 450 and 600 fpm on sand molds and up to 1000 fpm on metal molds. Speed is calculated from revolutions per minute as follows:

$$V = 2\pi RN$$

where V = peripheral velocity of outside of mold in feet per minute
R = radius of mold in feet
N = number of revolutions per minute

Fundamentally, this method of calculation should be based on centrifugal force or pressure rather than peripheral speed. However, for the range of sizes and speeds concerned, it has been proven by experience that the use of peripheral speeds is a satisfactory criterion. It is evident that even for small castings, the speed of rotation does not exceed a few hundred revolutions per minute in semi-centrifugal casting.

The method of pouring and feeding the castings is of considerable importance. The metal is poured into the top of the sprue through a funnel that properly directs the metal. Pouring, as in the case of true

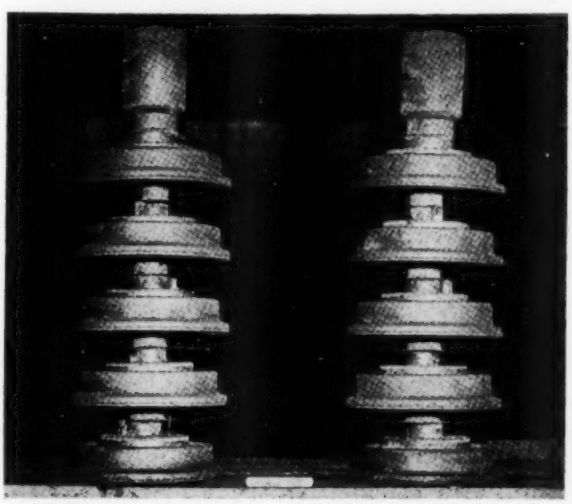


Fig. 23—Stack of track wheels cast by semi-centrifugal method (Blackwood and Perkins).

vertical casting, occurs at either maximum rotational speed or is started at a slow speed and accelerated during the pour.[30] In order to obtain directional solidification, a greater variety of gates, additional heads, and large reservoirs are used at various locations in mold and the spokes of a wheel are tapered from the hub and central sprue down to the rim so that the sprue can feed the entire casting. Since one of the main advantages in semi-centrifugal casting is the increase in casting yield, it is frequently the practice to increase this yield on annular shape castings by pouring just sufficient metal so that the central sprue and ingates or radial spokes are hollow after solidification has occurred.[30]

Semi-centrifugal casting is principally conducted on steel castings, although the method has been successfully utilized on cast irons, copper-base alloys, and light alloys.[29] A recent article shows that improved casting yield and good quality can be obtained from casting discs of an aluminum casting alloy[31] even though the density of the non-metallics does not differ greatly from the base metal. The mechanical and metallurgical quality of the castings is similar to those made in stationary molds except for the slight improvement in feeding that results in some cases.

The economic position of semi-centrifugal casting depends upon whether the cost advantages of increased yield, slightly improved feeding, and better filling overcome the cost disadvantages of more expensive molds and pouring arrangements plus the initial investment required for the casting machine. Although the initial costs are not large for the casting machine, the special type of molds required are a considerable expense. It is necessary, therefore, that each type of casting be required in considerable quantity to justify the special set-up. In addition, the design of the casting must be such that the slight assistance in feeding is required, the increase in yield is large, or the additional force in filling the mold necessary. These qualifications confine the process to the manufacture of special types of discs or annular shape parts within a definite size range where the design is suitable and the orders are large enough to warrant the investment and special type molds.

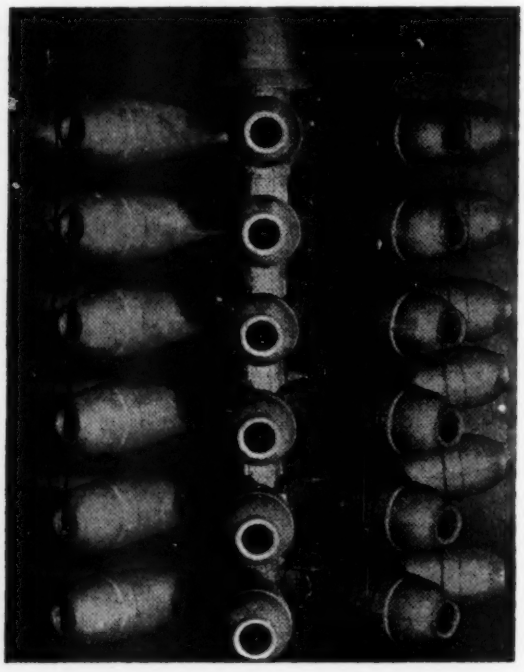


Fig. 24—Centrifuged stack of mortar shell castings (Blackwood and Perkins).

Centrifuged Castings

Centrifuged castings are produced in molds spaced symmetrically about a central down gate. The equipment, spinning speeds, pouring rate, properties, and problems are similiar to semi-centrifugal casting. The molds, however, are not spun about their own axes but arranged as symmetrically as possible about the central sprue to maintain a good dynamic balance. It is the usual practice to stack the castings in this grouping as high as the machine location and power capabilities permit so that the greatest possible number of castings can be poured in one assembly.

A great variety of pieces can be manufactured by centrifuging. It is desirable, however, to manufacture identical or very similar castings in any one group to avoid excessive vibration. The types of castings produced are usually small and simple in design. An example of a typical type is shown "as cast" in Fig. 24.[29] Large castings present more difficulties in molding, pouring, and spinning without compensating advantages in definition or yield[11]; intricate castings are more subject to mold breakdown or erosion. Since the centrifugal pressures are small at the speeds of rotation employed, only a small assistance in feeding results. It is necessary to use down-gates large enough to provide a source of molten steel while the entire casting is solidifying. In addition, the casting sections should be progressively heavier from the outside periphery of the mold cavity to the central sprue if soundness is required. In some cases, however, where shrinkage can be tolerated at certain locations, these progressively thicker sections need not be employed.

The machines, equipment, molds, and techniques utilized in centrifuging are so similar to semi-centrifugal casting that only a few observations need be noted. The molds are almost always made of sand, usually slabs of oil-bonded core sand, although, in a few cases, green sand molds with core inserts are used. The gates are radial but extend upward and then outward (so-called "umbrella" gates) when the castings are poured in a stack to insure that the lower castings of the stack are filled before metal enters the higher stacks.[29] The machines are usually rotated to provide a peripheral speed of 450 to 600 fpm and the molds are filled with molten metal as rapidly as possible. The mold is usually spinning at the maximum rotational speed during pouring, although in special cases, pouring is initiated when the mold is stationary or moving slowly and accelerated during the pour.

Centrifuging is used to a great extent in precision investment casting to obtain the detail and dimensions required.[32] In this case, the relatively dense investment used with wax or plastic disposable patterns requires an exterior force to drive the metal into all

portions of the mold cavity. This is particularly true of parts with considerable fine detail. The flasks containing the mold cavities are usually located at a considerable distance from the central sprue to increase the centrifugal pressure. These precision castings are always spun at maximum speed before pouring and the pressure is maintained until solidification. A sketch of the centrifuging of investment castings is contained in Fig. 25.[32]

The problems encountered in centrifuging such as lack of balance and resulting vibration, runouts at horizontal joints, and bursting of the mold are similar to semi-centrifugal casting. The mechanical and metallurgical properties of ferrous and non-ferrous metals are, as in the case of semi-centrifugal castings, similar to static sand castings except for the improved flow and slightly better feeding. The number of rejections obtained depends, of course, on the complexity of the casting and the stage of development of the technique. Generally, the loss of castings due to failure to fill all sections of the mold will be considerably reduced from static casting although the "breakouts" are apt to occur more frequently. By either process, the number of rejects should be very small with a satisfactory technique. Defects which do occur in centrifuged castings can generally be more easily detected than in stationary castings.

In assessing the economic factors influencing the utilization of this process compared to static sand castings, the improved casting yield, slightly enhanced feeding, and better detail obtained in centrifuging must be compared to the additional cost of the machine, molds, and set-up. A comparison of these figures usually shows that static casting is cheaper except for large production of small castings of certain designs that permit greatly increased yield or where the centrifugal pressure is a requirement to obtain the required detail.

Summary

Centrifugal casting provides the foundryman with great assistance in the manufacture of castings within certain design, quality, and quantity limitations. The centrifugal casting process is used to its greatest advantage in producing tubular products by the horizontal or vertical true centrifugal method. In true centrifugal casting, gas cavities are removed, nonmetallics forced to the inner surface, and a sound casting usually produced throughout by unidirectional solidification from the outer surface. This results in the production of a high-quality casting with a high casting yield. Centrifugal castings of a disc or circular shape are made with some yield, detail, and limited feeding advantages in vertical axis machines about a central sprue by semi-centrifugal casting. Small castings of simple design are centrifuged in vertical axis machines about a central sprue to obtain improved detail and casting yield.

Centrifugal casting is performed in both metal dies and sand molds. Metal molds require the manufacture of a large quantity of identical castings of a design that will permit easy removal from the mold. This type of mold has the advantages of more rapid production and improved quality with the faster solidification. Sand molds permit frequent changes in casting design, the production of more irregularly shaped castings, the flow of molten metal over long distances without marked chilling, and a more uniform, though slower cooling rate throughout the casting.

The ease of production of both ferrous and nonferrous centrifugal castings depends on the composition of the metal, dimensions of the casting, and techniques developed. Heavy-walled steel tubes are one of the most difficult types, since they are subject to hot tears, irregular segregation, laps, and refractory breakdowns. In thin-walled steel or cast iron tubes or pipe, laps and cracks are serious problems. The copper-base alloys are not usually subject to cracks and tears and can be produced with fewer defects. Inspection procedures are simplified by the fact that the defects that do occur can usually be readily observed on the surface.

Centrifugal castings compete with the forging and static sand casting processes. The economic and quality considerations for each part are the determining factors. True centrifugal castings produced in metal molds exhibit mechanical and metallurgical properties that are the equivalent of forgings without directional properties. True centrifugal castings made in sand molds are coarser in structure than metal-mold castings because of the slower rate of solidification but are generally of a higher quality than static sand castings. The lower centrifugal pressure extended in semi-centrifugal and centrifugal castings improved detail and mold filling, increases casting yield, but the quality of these castings is similar to those produced by static methods.

Centrifugal casting is not a cure-all for the ills of a foundry. It is very useful and economical for the manufacture of tubular, high quality castings, cast iron pipe manufacture, and in other special instances but this field is limited. The use of centrifugal casting outside of this limited field will increase costs and cause more problems than are solved.

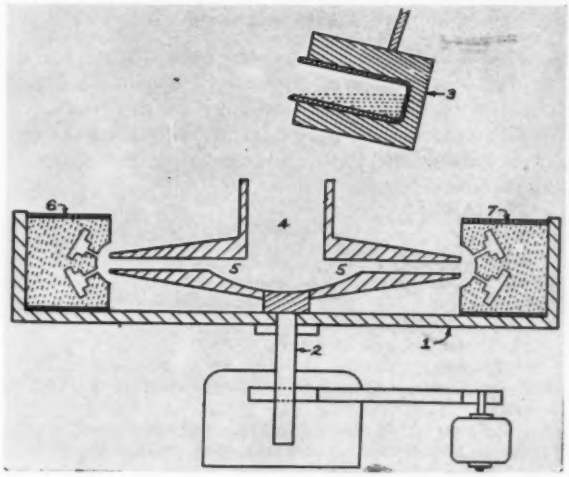


Fig. 25—Diagram of equipment for centrifuging precision castings (Nieman).

Acknowledgments

The author wishes to express his gratitude for the guidance of Professor Howard F. Taylor of Massachusetts Institute of Technology in this work.

The assistance of personnel at Watertown Arsenal in obtaining the data and preparing the figures is also acknowledged.

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CHARACTERISTICS OF HIGH STRENGTH MAGNESIUM CASTING ALLOY ZK61

By

J. W. Meier*

Introduction

The rapid progress in design of larger and faster aircraft based on the increasing power of jet-propelled engines created the necessity of development of alloys with the highest possible strength-to-weight ratio. High-alloy steel, as well as titanium, aluminum and magnesium alloy products are close competitors in this race for weight reductions.

In the casting alloy field, magnesium-base alloy ZK61 still has the highest strength-to-weight ratio found in any commercial casting alloy, ferrous or non-ferrous. The development of alloy ZK61 and its heat treatment was reported earlier[1-4] and it is the purpose of the present paper to discuss some characteristics of this alloy as compared with other magnesium casting alloys in the light of additional laboratory work and of its performance in actual service.

Earlier work on magnesium-zinc-zirconium casting alloys was reviewed in a previous paper,[1] and was recently brought up to date by Saunders and Strieter[5] and by Hildebrand and Strieter.[6]

Materials and Experimental Procedures—Materials used for the preparation of the experimental alloys as well as the standard ZK61 alloy for prototype castings of commercial size were: high-purity magnesium (99.98 pct) prepared by Dominion Magnesium Ltd. using the Pidgeon ferrosilicon process, electrolytic zinc (99.99 pct), and various zirconium compounds or master alloys. Rare earth additions to some experimental alloys were made using standard Mischmetal (approximately 50 pct Ce, 50 pct other rare earth metals, less than 1 pct Fe).

Zirconium was introduced, in most cases, as a fused salt mixture containing approximately 50 pct zirconium tetrachloride and 25 pct each of sodium and potassium chlorides. In some of the experimental alloys zirconium was added in the form of other zirconium chloride or various zirconium fluoride mixtures, as well as in binary or ternary master alloys.

The melts were prepared in welded iron pots of 30- to 150-lb capacity, using Domal crucible flux (Dow 310 type). Temperatures during melting and alloying operations were held, whenever possible, under 800 C (1470 F) and pouring temperatures between 750-780 C (1380-1435 F). Standard melting and alloying techniques were used as described previously.[1]

All castings were cast into green sand using standard sand composition and characteristics for magnesium alloys. Tensile test bars were cast-to-shape according to the Dow Chemical Company's test bar mold design[7] and pulled without machining, unless otherwise stated.

All heat treatments above 300 C (570 F) were carried out in electrically heated air-circulating furnaces with automatic control of a 1 pct sulphur dioxide containing protective atmosphere.

Alloys were designated throughout this paper according to the ASTM alloy designation code (ASTM B275-52T). No suffix or serial letter was used in designations of alloys not specified in ASTM B80-51T. Temper designations for various heat treatments were used in accordance with recent ASTM specifications for magnesium alloys, as follows:

F —As cast
T4—solution heat treated only
T5—artificially aged only
T51—12-24 hr at 180 C (355 F)
T52—24-48 hr at 150 C (300 F)
T6—solution heat treated and artificially aged
T61—2 hr at 500 C (930 F) and 12-24 hr at 150 C (300 F)
T62—2 hr at 500 C (930 F) and 24-48 hr at 130 C (265 F)

Effect of Zirconium Content—Earlier work by German investigators on the solubility of zirconium in magnesium was recently supplemented by investigations on the constitution diagram of magnesium-rich alloys of magnesium and zirconium published by Mellor[8] and by Schaum and Burnett.[9] No results of work on the ternary magnesium-zinc-zirconium alloy

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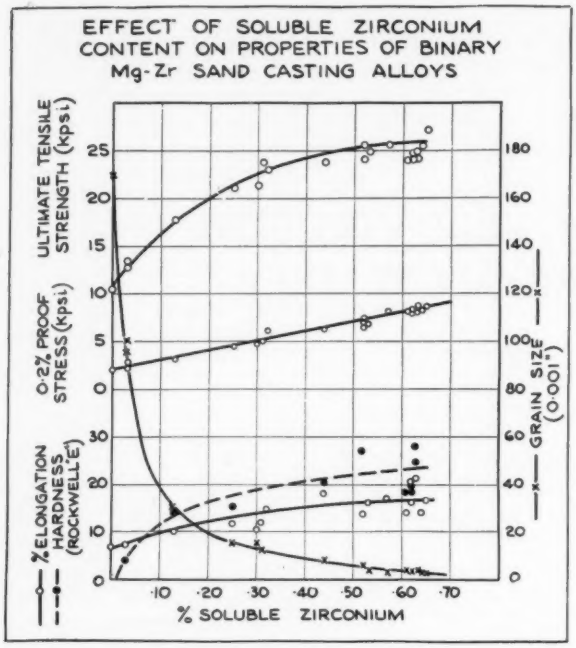


Fig. 1—Effect of soluble zirconium content on properties of binary Mg-Zr sand-casting alloys.

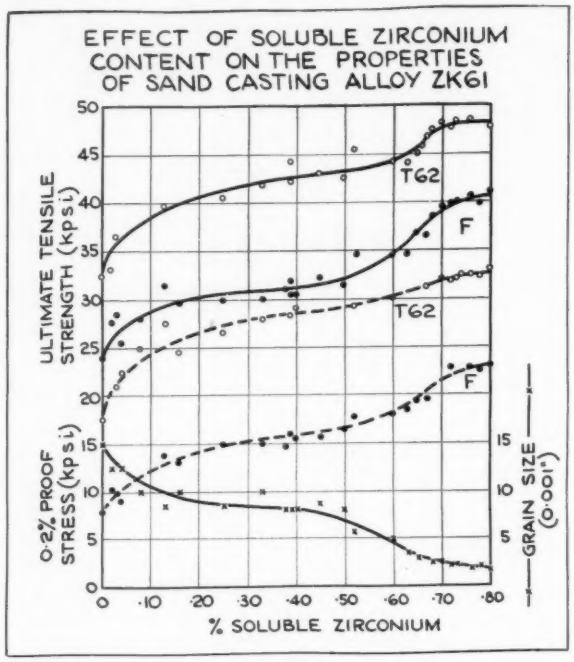


Fig. 2—Effect of soluble zirconium content on properties of sand casting alloy ZK61.

system have been published to date. As reported previously[1] in actual foundry practice it was established that the "effective" zirconium solubility in binary magnesium-zirconium alloys is approximately 0.65 pct, and in Mg- 6 pct Zn-Zr alloys it raises to approximately 0.85 pct.

It is known that the grain refining effect depends on the amount of zirconium which is actually in solution in magnesium. This "effective" zirconium content is directly related to the grain size and the mechanical properties in the as-cast condition as well as after heat treatment. Excessive, "free" or acid-insoluble zirconium remains in varying amounts finely dispersed throughout the alloy. As stated already,[10] experience based on many hundreds of magnesium-zinc-zirconium alloy melts shows that the "effective" zirconium content cannot always be identified with the analytically determined acid-soluble zirconium content. It was found that grain size and mechanical properties of magnesium-zinc-zirconium alloys are a much more reliable indication of the real "effective" zirconium content than its chemical determination.

Figures 1 and 2 show graphically the effect of soluble zirconium content on properties of binary magnesium-zirconium and ternary ZK61 casting alloys. The considerable grain refining effect of increasing zirconium additions is shown especially in the case of binary alloys. Figure 2 illustrates very convincingly that optimum properties both in the as-cast (F) as well as in the fully heat treated (T62) conditions are obtained only if the "effective" zirconium content is over 0.7 pct and the average grain size is in the range of 0.001-0.002 in., in any case, below 0.003 in.

Alloying Technique—A very good survey of alloying techniques used for the introduction of zirconium into the molten magnesium was recently published by Saunders and Strieter.[5] Most of the methods described by these authors were used also in the study of alloy ZK61 with similar results in alloying efficiency.[1] In both investigations it was found that the most consistent and most effective alloying mixtures are those based on or, at least, containing some zirconium chloride compounds. It was suggested[1,11] that the exceptionally high properties obtained using zirconium chloride compounds are due to the efficient refining and degassing action of decomposition products of these fluxes. Especially important seems to be the removal of hydrogen which affects greatly the solubility of zirconium in magnesium and magnesium alloys.

Attempts at using proprietary alloying methods based on various zirconium fluoride compounds for the production of alloy ZK61 were rather unsuccessful. Fully heat treated test bars cast from fluoride-treated melts show consistently lower tensile properties than similar test bars from chloride-treated melts and these differences are quite substantial, i.e., 5,000-6,000 psi UTS and 2,000-3,000 psi 0.2 pct PS.

The alloying technique used for most melts in the present investigation was described in an earlier paper.[1] Some of the melts were alloyed with satisfactory results using a magnesium-zirconium hardener (K30) described by Saunders and Strieter.[5]

A useful check of alloying efficiency and melt quality before the casting is poured was described by Van Ewijk.[12] A test bar is cast in a metal mold, taken out as soon as it is frozen, quenched in water, notched and broken. Extremely fine grain size of the fresh

fracture indicates that the zirconium content is satisfactory, otherwise more alloying of the melt is necessary.

Effect of Zinc Content—The diagram illustrating the effect of zinc contents on tensile properties of magnesium-zinc-zirconium casting alloys containing from 1-11 pct Zn and over 0.7 pct of soluble zirconium, shown in a previous paper,[1] was brought up to date and revised to include data on tensile properties of the various alloys after aging for 24 hr at 180 C (355 F) used commercially for alloy ZK51.

Figure 3 shows this new diagram. It should be emphasized that each of the curves shown was drawn on the basis of many hundreds of results obtained during the seven years of work on these alloys. However, in the interest of clarity only a few typical values for each condition were shown in the reproduction of the original diagram presented in Fig. 3.

In the former version of this diagram[1] the curve for the "fully heat treated" condition was continued for the alloys of higher zinc content. The heat treatment of these alloys (7-10 pct Zn) was, of course, different from that used for the lower zinc containing alloys. To avoid misunderstanding the present diagram contains data pertaining only to the heat treatments mentioned on the drawing.

The diagram shows clearly that highest strength values are restricted to alloys containing from 5-7 pct zinc, which show excellent amenability to solution heat treatment and very good, as compared with other magnesium casting alloys, elongation values (8-14 pct in 2 in.).

Alloys with lower zinc contents (1-3 pct Zn) show excellent elongation values (up to 25 pct), good casting characteristics and weldability, but due to their low strength cannot be used for highly stressed components.

Alloys with higher zinc contents (8-11 pct Zn) show a considerable drop both in strength as well as in elongation values (3-5 pct).

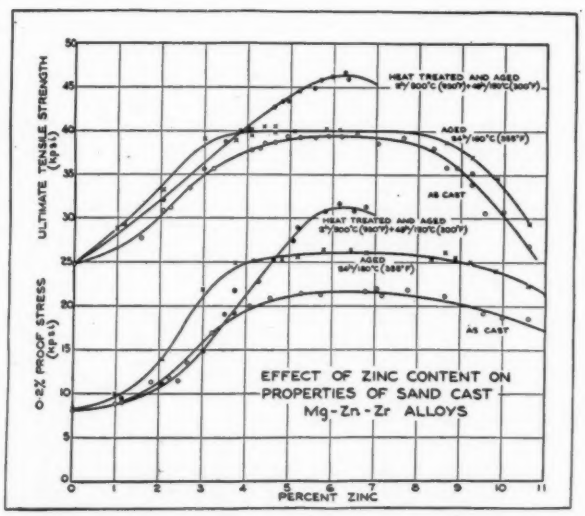


Fig. 3—Effect of zinc content on tensile properties of sand-cast Mg-Zn-Zr alloys (containing over 0.7% soluble zirconium).

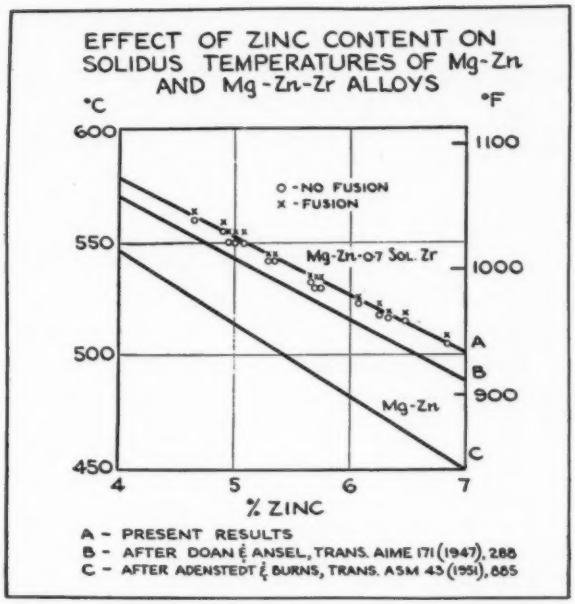


Fig. 4—Effect of zinc content on solidus temperatures of Mg-Zn and Mg-Zn-Zr alloys (containing over 0.7% soluble zirconium).

Figure 4 illustrates the effect of zinc content on the solidus temperatures of binary magnesium-zinc alloys and ternary magnesium-zinc-zirconium alloys. The difference in the solidus lines shown for the ternary alloys is believed to be due to higher soluble zirconium contents obtained in the present investigation. The results shown in the diagram were obtained by metallographic examination for incipient fusion of heat treated and rapidly quenched specimens cut out of sand cast test bars. Some of the points were checked by thermal analysis.

Figures 5 and 6 illustrate the effect of zinc content on the tensile properties of magnesium-zinc-zirconium casting alloys after different heat treatments. The curves for tempers T61 and T62 show clearly that the solution heat treatments developed for alloy ZK61 are applicable only to alloys containing over 5 pct Zn, because for lower zinc containing alloys the aging of as-cast products (T51 and T52) results in much higher tensile properties. It would seem from these experiments that the two-stage heat treatment for alloys having zinc contents below 5 pct offers no advantage over the one-stage treatment (aging).[10]

Heat Treatment—The development of the most effective heat treating methods for alloy ZK61 was described previously.[1] As for other alloys, there is a range of temperatures and times to be used depending on casting size and shape, as well as local heating facilities. This range for alloy ZK61 castings was established[2,4] as follows:

Solution heat treatment for 1-5 hr at 425-500 C (800-930 F), followed by air cooling and aging for 24-48 hr at 130-150 C (265-300 F).

The highest and most consistent results on cast-to-shape test bars and other small castings were ob-

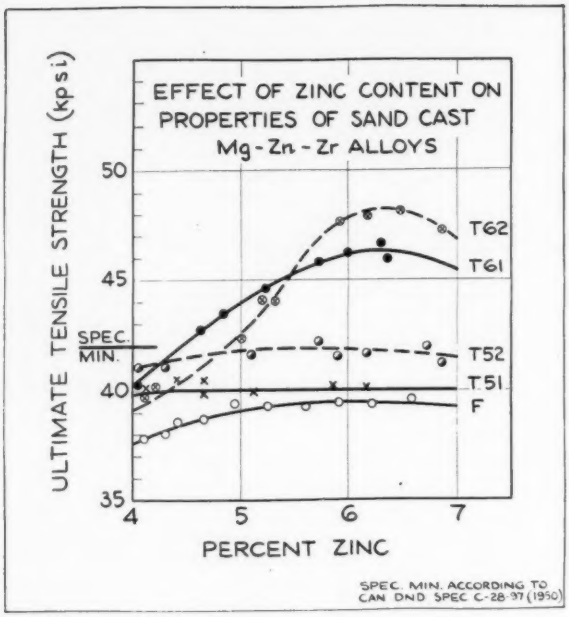


Fig. 5—Effect of various heat treatments on ultimate tensile strength of sand-cast Mg-Zn-Zr alloys (containing 4-7% and over 0.7% soluble zirconium).

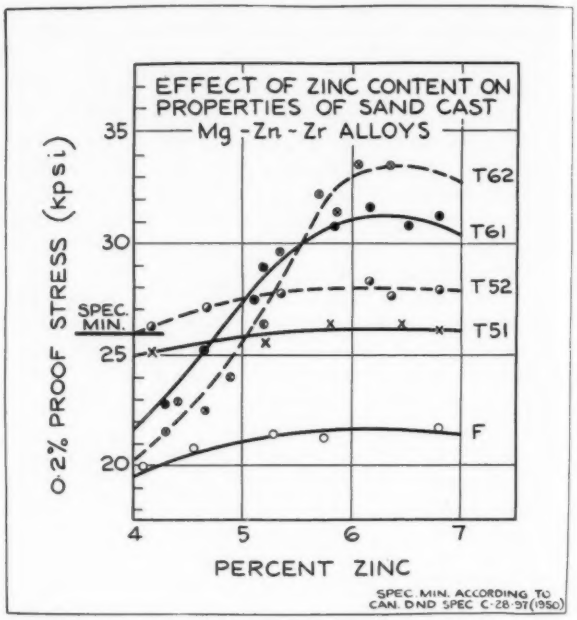


Fig. 6—Effect of various heat treatments on 0.2% proof stress of sand-cast Mg-Zn-Zr alloys (containing 4-7% Zn and over 0.7% soluble zirconium).

tained using a solution heat treatment of 2 hr at 500 C (930 F) and aging for 24-48 hr at 130 C (265 F) For larger castings a successful heat treatment was found to be: 5 hr at 485 C (905 F) and 48 hr at 130 C (265 F).

Successful heat treatment at these higher solution temperatures, as in all heat treating operations of whatever alloy, depends on proper heat treating equipment and especially on reliable temperature control and effective maintenance of a protective atmosphere. Sulphur dioxide atmospheres offer the best protection, although our experience in using carbon dioxide atmosphere was also satisfactory.

Another precaution necessary in all higher temperature heat treatments is the proper placement and supporting of castings of larger size or more intricate shape in the heat treating furnace to avoid warpage or sagging due to some loss of rigidity of the castings at temperatures close to the solidus temperature of the alloy. This occurrence is, of course, common to all alloys and is not a special feature of alloy ZK61 as might be inferred from some recently published remarks.[13,14] Well known methods for preventing any distortion in solution treatments of long established magnesium casting alloys AZ91 and AZ92, and especially at the much higher temperatures used for the newer magnesium-rare earth-zirconium alloys (16 hr at 565 C, 1050 F) show that the heat treating schedule established for alloy ZK61 is entirely practical.

It was also mentioned[14] that the response to heat treatment at 500 C (930 F) varies with the true soluble zirconium content of the alloy and the section thickness of the casting. This would mean, it was said, that to achieve a high level of properties, the duration and temperature of the solution treatment would require adjustment to suit the soluble zirconium content and section thickness of individual castings. Both of these statements while correct are of no practical significance. The heat treating schedule for alloy ZK61 was established for the lowest zirconium content allowable in the specification, and if the zirconium content in a particular casting would be lower, the casting would not be acceptable because of its improper chemical composition.

The other point is the dependence of heat treating cycles upon section thickness. This dependence exists for all metal or alloy products, cast or wrought, and it is quite clear that a bulky casting needs longer heat treatments than a thin sheet product. This is

TABLE 1—EFFECT OF VARIATIONS IN HEAT TREATING CYCLES ON THE TENSILE PROPERTIES OF ZK61 ALLOY CASTINGS

Alloy Composition: 5.5 to 6.5% Zn, over 0.7% soluble Zr
Average Grain Size: 0.001 to 0.003 in.
(All results are averages of at least 3 tests)

Solution H.T.			Aging			Tensile Properties		
Time hr	Temp. °C	°F	Time hr	Temp. °C	°F	UTS kpsi	0.2% PS kpsi	Elong. % in 2 in.
2	475	890	24	130	265	43.5	27.2	10.5
2	475	890	48	130	265	45.6	30.0	8.5
5	475	890	24	130	265	44.8	29.1	11.6
5	475	890	48	130	265	46.3	31.3	10.0
2	485	905	24	130	265	45.2	28.3	10.5
2	485	905	48	130	265	46.5	30.8	9.8
5	485	905	24	130	265	46.2	30.2	11.2
5	485	905	48	130	265	46.8	31.7	9.2
2	500	930	24	130	265	47.6	32.5	12.0
2	500	930	48	130	265	48.2	33.4	11.5

TABLE 2—TYPICAL TENSILE PROPERTIES FOR ALLOY ZK61 AS COMPARED WITH OTHER MAGNESIUM CASTING ALLOYS

Alloy Designation	Nominal Composition %	Ultimate Tensile Strength, kpsi		0.2% Proof Stress, kpsi		Elongation, % in 2 in.	
		Typical	Min.	Typical	Min.	Typical	Min.
ZK61-F	6 Zn, 0.7 Zr	38-42	35	20-22	18	9-14	8
ZK61-T52	6 Zn, 0.7 Zr	40-43	—	27-30	—	5-8	—
ZK61-T61	6 Zn, 0.7 Zr	44-48	42	28-32	26	8-12	5
ZK61-T62	6 Zn, 0.7 Zr	46-48	—	30-34	—	7-12	—
ZK51-T51	4.6 Zn, 0.6 Zr	39-41	34	25-27	20	6-10	5
AZ63A-T4	6 Al, 3 Zn	40	34	14	—	12	7
AZ63A-T6	6 Al, 3 Zn	40	34	19	16	5	3
AZ91C-T6	8.7 Al, 0.7 Zn	40	34	19	16	4	3
AZ92A-T6	9 Al, 2 Zn	40	34	23	18	2	1

Note: Values for AZ alloys according to ASM *Metals Handbook* (1948).
Minima for ZK61 alloys according to Canadian D.N.D. Spec. C-28-97.
Minima for ZK51 alloy as proposed for ASTM specification.

why heat treating schedules are normally given in a range of temperatures and times.

Table 1 shows the effect of some variations in solution heat treating temperature and time on the tensile properties of ZK61 alloy cast-to-shape test bars. As is usual, the properties increase with higher temperatures and longer duration of the treatments.

The graphs in Figs. 5 and 6 indicate also the considerable increase in properties obtained when using lower aging temperatures, both in the as-cast condition (T52) and especially after solution heat treatment (T62).

The most important feature of the two-stage heat treatment developed for alloy ZK61 castings is that it not only increases considerably the ultimate tensile strength and the yield strength but also retains or even improves the high elongation values.

Mechanical Properties—A comparison of typical tensile properties of ZK61 alloys with those of other magnesium casting alloys is given in Table 2. Minimum values of properties for ZK61 alloys are quoted as required in the Canadian aircraft specification for ZK61 alloy sand castings.[15] Results of compression, shear, hardness and impact tests are listed in Table 3. Alloy ZK61-T6 has considerably higher tensile, compression and shear strengths, combined with an elongation unusually high for a fully heat treated (T6) magnesium casting alloy. The impact strengths of ZK61 and ZK51 are similar and somewhat lower than that of AZ63A-T4.

TABLE 4—TYPICAL RESULTS OF FATIGUE TESTS AT ROOM TEMPERATURE

(R. R. Moore Rotating Beam Tests)

Alloy Designation	Plain Bar (stress in kpsi)			Notched Bar* (stress in kpsi)		
	10^8	10^7	10^8	10^8	10^7	10^8 cycles
ZK61-F	13	10.5	10			
ZK61-T6	18	15	14.5	12	11	10
ZK51A-T5**	11	—	9	11	—	9
AZ63A-T4**	18	—	14	12	—	8
AZ92A-T6**	18	—	15	11	—	9

*Stress Concentration Factor = 2
**according to Hildebrand and Strieter[8]

The fatigue properties of ZK61-T6, as shown in Table 4, are comparable with those of AZ63A-T4 and AZ92A-T6, and higher than the values given for alloy ZK51A-T5.

Properties at elevated temperatures are listed in Tables 5 and 6. Although alloy ZK61 was developed especially for room temperature service, it may be used at moderately elevated temperatures (up to 150 C, 300 F). Its elevated temperature tensile properties are higher than those of alloys ZK51A-T5, AZ63A-T6 and AZ92A-T6, and in creep strength it is equal to ZK51A-T5 and somewhat better than AZ63A-T6 and AZ92A-T6.

Results of tests on stress relief treatments show that similar to the results of Payne[16] for other magnesium casting alloys, for complete stress relief a 2 hr heat treatment at 330 C (625 F) is necessary. Alloy ZK61-T6 is being solution heat treated at a much higher temperature and the rather slow air cooling after this solution heat treatment limits the re-introduction of stresses. Ring tests, as described by Payne,[16] on ZK-61-T6 alloy confirmed the absence of residual stress and no problem has been encountered in practice.

Effect of Rare Earth Additions—The use of additions of rare earth metals to magnesium-zinc-zirconium alloys was first explored by German investigators[17] to overcome the tendency to microporosity encountered in alloys of higher zinc contents. They found that 1-2 pct rare earth metals should be added to alloys containing 4-6 pct Zn to obtain maximum pressure tightness. Recently, Payne, Michael and Eade[18] described a new magnesium casting alloy which has been developed to reduce the tendency to hot-cracking found in larger ZK51 alloy castings. The nominal composition of this new alloy was reported to contain 4.5 pct Zn, 1.25 pct R.E., and 0.6 pct Zr (ASTM alloy designation would be ZE51-T5) and it

TABLE 3—TYPICAL RESULTS OF COMPRESSION, SHEAR, HARDNESS AND IMPACT TESTS

Alloy Designation	Ultimate Compression Strength, kpsi	0.2% Compression Proof Stress, kpsi	0.1% Compression Proof Stress, kpsi	Shear Strength, kpsi	Brinell Hardness (500/10/30)	Rockwell "E" Hardness	Charpy Impact Strength Unnotched, ft-lb	Notched (Mesnager) ft-lb
ZK61-F	55.3	18.5	17.8	24.5	58-65	65-70	20	3.5
ZK61-T5	55.1	25.1	23.5	24.8	65-70	75-80	14	2.5
ZK61-T6	56.4	29.2	28.0	26.5	65-75	75-82	18	3.5
ZK51A-T5	51.8	23.7	22.6	21.7**	65-70	72-77	15	3.5
AZ63A-T4	46.0*	14.0*	—	18.0*	55*	66*	27**	—
AZ92A-T6	—	22.0*	—	22.0*	81*	88*	4**	—

* ASM *Metals Handbook* (1948).
** according to Hildebrand and Strieter.[8]

TABLE 5—TYPICAL TENSILE PROPERTIES AT ELEVATED TEMPERATURES

Alloy Designation	Testing Temperature °C	°F	UTS, kpsi	0.2% PS, kpsi	% Elong., in 2 in.
ZK61-T6	R.T.	R.T.	47.2	31.4	10.5
	100	212	36.8	26.5	20.0
	125	257	32.3	24.5	22.0
	150	302	28.0	22.0	25.0
	175	347	23.5	19.5	30.0
	200	392	20.5	17.6	32.0
ZK51A-T5	R.T.	R.T.	39.5	26.5	8.5
	100	212	32.4	22.4	15.0
	125	257	28.1	20.8	16.3
	150	302	24.3	18.5	15.5
	175	347	21.7	17.2	18.5
	200	392	19.1	15.8	18.0
AZ63A-T6*	R.T.	R.T.	40.0	19.0	5.0
	93	200	36.0	17.3	11.0
	121	250	32.4	16.5	11.0
	149	300	24.5	15.0	15.0
	204	400	17.5	12.0	17.0
AZ92A-T6**	R.T.	R.T.	40.0	23.0	2.0
	149	300	28.0	18.0	35.0
	204	400	16.9	10.9	36.0

*ASM Metals Handbook (1948).
**according to T. E. Leontis, Trans. AIME 180 (1949), p. 319.

TABLE 6—CREEP LIMIT IN 100 HR AT ELEVATED TEMPERATURES

Alloy Designation	Testing Temperature °C	°F	Creep Limit (stress in kpsi) 0.1% Creep Extension	0.2% Total Extension	0.5% Total Extension
ZK61-T6	100	212	9.5	8.0	13.8
	150	302	4.7	4.9	9.0
	200	392	1.1	1.9	3.2
ZK51A-T5*	93	200	10.0	8.1	14.0
	149	300	5.0	5.0	11.4
	204	400	1.6	2.2	4.2
AZ63A-T6*	149	300	3.5	4.0	8.5
	204	400	1.3	1.8	2.8
AZ92A-T6*	93	200	8.5	7.8	13.5
	149	300	3.2	3.9	7.0

*according to Hildebrand and Strieter[6]

was claimed that the alloy has good welding characteristics and excellent pressure tightness.

The effect of additions of small amounts of rare earth metals on the properties of ZK61 alloy castings was reviewed previously[1] and preliminary results showed that these additions considerably decrease mechanical properties, especially in the fully heat treated (T6) condition. Results of a more extensive investigation are shown in Figs. 7 and 8. The temper designations used in these graphs are:

F —As cast
T51—Aged for 24 hr at 180 C (355 F)
T52—Aged for 48 hr at 150 C (300 F)
T62—Solution heat treated for 2 hr at 485 C (905 F) and aged for 48 hr at 130 C (265 F).

The somewhat lower solution heat treating temperature was used because rare earth additions lower the solidus temperature of ZK61 alloy.

Figure 7 shows that rare earth additions above approximately 0.3 pct considerably decrease the tensile properties of alloy ZK61, especially in the fully heat treated (T6) condition. The graphs for alloy ZK51 (Fig. 8) show a similar downward trend, although on a lower level of properties.

It seems that the considerable decrease of mechanical properties caused by the addition of 1-1.5 pct rare earth metals will limit the use of these alloys to cases where requirements for extreme pressure tightness override the cost factors. In all other cases it would be hardly worthwhile to use these alloys in competition with the long established and much cheaper magnesium-aluminum casting alloys with low

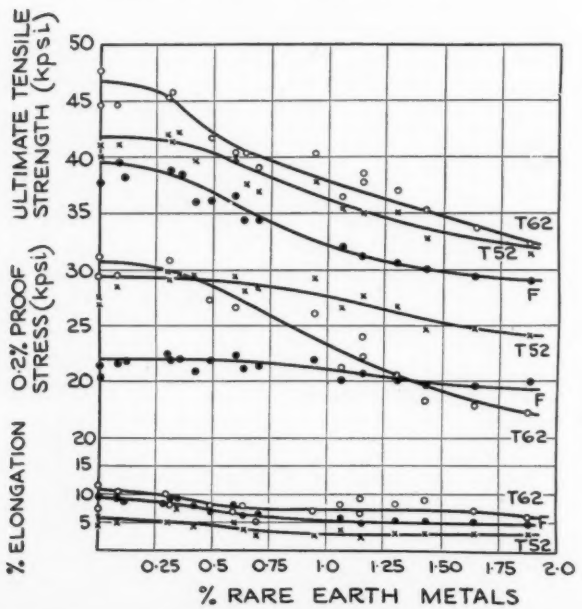


Fig. 7—Effect of rare earth additions on tensile properties of sand-casting alloy ZK61.

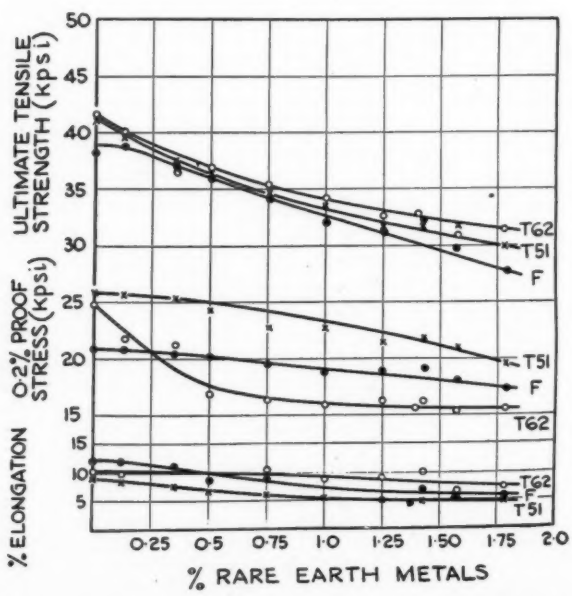


Fig. 8—Effect of rare earth additions on tensile properties of sand-casting alloy ZK51.

zinc contents, especially alloy AZ80 (7.5-8.5 pct Al, 0.2-0.6 pct Zn, 0.15-0.40 pct Mn). This most versatile general purpose foundry alloy, used very extensively in Canada, has good weldability and resistance to hot-cracking, and shows, if properly prepared, very little tendency to microporosity. Typical tensile properties are listed for comparison in Table 7.

TABLE 7—COMPARISON OF TYPICAL TENSILE PROPERTIES OF ALLOYS ZE51 AND ZE61 WITH ALLOYS AZ80 AND AZ91C

Alloy Designation	UTS, kpsi	0.2% PS, kpsi	Elongation, % in 2in.
ZE51-T5*	32	22	5
ZE61-T5*	35	27	3
AZ80-T4**	39	14	14
AZ80-T6**	39	16	8
AZ91C-T6**	40	20	6

*Alloys ZE51 and ZE61, respectively, containing 1.25% rare earth metals, as shown in Figs. 7 and 8.
**T4—solution heat treated only.
T6—solution heat treated and artificially aged.

Commercial Castings—In a recent discussion of an A.F.S. paper[19] it was stated that the "outstanding properties" of alloy ZK61 can be obtained only "with some types of test bar." As mentioned already, throughout the whole investigation on high strength magnesium casting alloys cast-to-shape Dow-type test bars were used, as recommended in U.S. Federal Specification QQ-M-56 and used in most U.S.A. and Canadian magnesium foundries. It was found by many workers in the U.S.A. and in Canada, including

TABLE 8—TENSILE PROPERTIES OF STANDARD SIZE TEST BARS MACHINED FROM ZK61-T6 ALLOY SAND CASTING "A" (FIG. 9)
(Melt No. 2049—6.02% Zn, 0.76% sol. Zr.)

Test Bar No.	Ultimate Tensile Strength, kpsi	0.2% Proof Stress, kpsi	Elongation, % in 2 in.
4	42.6	30.9	4.5
7	45.8	30.0	8.0
8	42.7	27.7	7.0
10	43.0	27.7	8.0
11	43.9	28.2	8.5
13	44.1	27.0	13.0
A1	38.8	26.4	3.5
A2	34.5	28.4	9.0
A3	46.0	30.0	10.0
B1	36.2	26.5	3.0
B2	41.9	27.6	7.0
B3	46.0	30.0	11.0
C1	39.2	26.4	5.0
C2	44.0	29.0	7.0
C3	39.9	26.3	6.0
D1	39.9	26.3	6.0
D3	39.6	27.6	3.5
Max. value	46.0	30.9	13.5
Min. value	34.5	26.3	3.0
Average (17)	41.6	27.9	7.0
Canadian D.N.D. Spec. C-28-97 (1950) for separately cast bars	42.0 min.	26.0 min.	5.0 min.
Minima for test bars cut from castings*	31.5 (75%)	19.5 (75%)	1.25 (25%)

* According to U. S. Federal Specification QQ-M-56 (1950) for Magnesium Alloy Sand Castings the average ultimate strength and average elongation of test specimens cut from castings shall not be less than 75% and 25%, respectively, of the values specified for separately cast test bars.

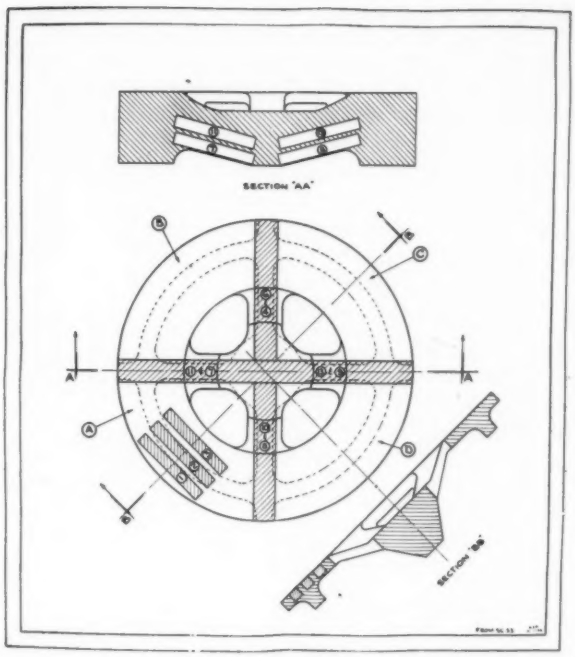


Fig. 9—Location of test coupons in ZK61 alloy prototype casting "A."

the present author, that test bars cast according to this mold design give consistent results and are sufficiently sensitive to changes in melt quality, which, of course, should be the main function of a cast test bar. British D.T.D-type machined test bars, it is generally agreed, represent to a large extent the skill and the experience of the foundry worker. This is very important in the production of good castings but should not affect a test of metal quality (not casting quality).

In any case, the real value of a casting alloy has to be proved by the quality of commercial castings and their service characteristics. A considerable number of ZK61 alloy prototype castings, up to 100 lb net weight, of various components of experimental military and other equipment was produced and tested in severe conditions with most satisfactory results. Aircraft castings have been produced regularly for some time now, and as reported recently by R. Smallman-Tew,[20] due to the higher strength-to-weight ratio of alloy ZK61-T6, a number of high-strength Al-10 pct Mg alloy castings in the AVRO jetliner could be successfully replaced with considerable weight

TABLE 9—TENSILE AND COMPRESSION PROPERTIES OF TEST BARS MACHINED FROM ZK61-T6 ALLOY SAND CASTINGS "B" (FIG. 10)
(Melt No. 2316—5.87% Zn, 0.74% sol. Zr.)

Casting No.	Ultimate Tensile Strength, kpsi	0.2% Tensile Proof Stress, kpsi	Elongation %	0.2% Compression Proof Stress, kpsi
1	42.8	26.4	7.8	24.9
2	44.5	26.3	9.0	24.5
3	43.1	26.5	6.8	24.7
4	42.9	26.7	7.5	24.8

Each value is an average of 4 results.

saving, an important aim of every aircraft designer.

The quality of each new design of prototype castings is, of course, tested on test bars cut from various sections of castings as required in the inspection of aircraft castings.[7] To illustrate values obtained in such tests Tables 8 and 9 present actual test results on two prototype castings, shown in Figs. 9 and 10.

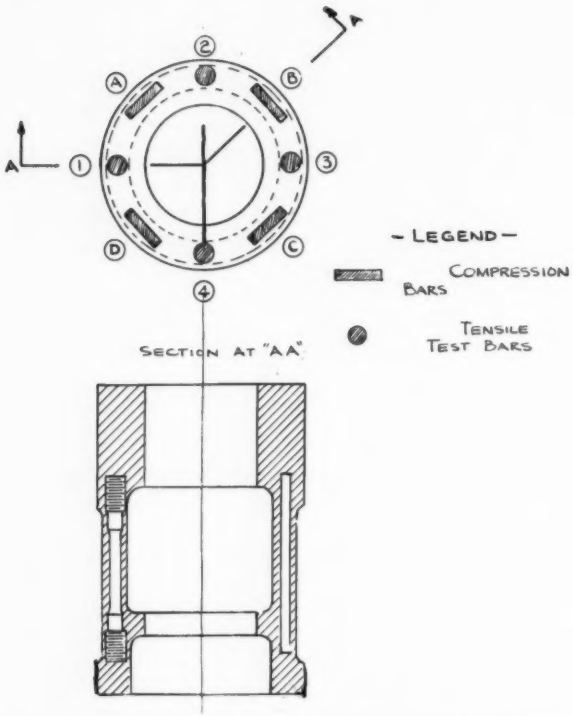


Fig. 10—Location of test coupons in ZK61 alloy prototype casting "B."

Casting "A" had a net weight of approximately 50 lb and standard size (0.505-in. diam) test bars were machined from various sections of the castings. The results from one of these castings, listed in Table 8, show high average values of tensile properties as compared with the requirements of the Canadian D.N.D. specification. Table 9 shows results of tensile tests on bars cut from a smaller casting (Figure 10) which reveal high averages and a surprising uniformity of properties.

As a further illustration of casting properties a summary is quoted of results of inspection tests carried out at an aircraft factory on castings supplied by a commercial foundry. Eight ZK61-T6 alloy castings of two different designs were cut into 67 test specimens of various sizes and shapes (round and flat) depending on the casting wall thickness, and the following tensile test results were obtained:

	UTS kpsi	0.2% PS kpsi	Elong. %
Maximum	44.8	32.9	(25)*
Minimum	32.3	22.9	(2)*
Average (67)	39.1	27.9	(9)*

(*Elongation figures should not be compared with values for standard size test bars.)

Considering the variety of cross-sections tested and the differences in test results due to the non-standard sizes of the test bars used, the results are satisfactory.

Foundry Characteristics—One of the most important factors in the consideration of any casting alloy is, of course, its foundry characteristics. There is no satisfactory definition of this complex group of properties which includes easily measurable properties such as pattern shrinkage, casting fluidity, solidification range and heat transfer properties, pressure tightness, resistance to hot cracking, weldability (for casting repairs), machinability, amenability to heat treatment and to surface treatments, etc. There are also other characteristics which are of prime importance to the practical foundryman and which can be generally called as the "ease of handling" in the foundry. This includes such consideration as the amount of care and time necessary for providing the proper conditions for melting, refining, alloying, pouring or handling of scrap, the sensitivity of the alloy to sand conditions and mold design, etc.

In spite of statements to the contrary[13,14] there is no doubt that alloys ZK51 and ZK61 are more difficult to handle and have somewhat poorer foundry characteristics than the older Mg-Al-Zn casting alloys, especially those with low zinc contents. Among the factors still to be overcome before the foundry operator would define these alloys as easy to handle are the following: the necessity of additional alloying after each remelting, the importance of a thorough alloying procedure to achieve a high zirconium content, more care in the design of gates and risers especially for larger castings,[13] some sensitivity to microporosity and hot cracking,[18] poor weldability, and great care in scrap handling to avoid contaminations from aluminum or rare earth containing alloy scrap.

There is no difference of any practical significance in the foundry characteristics of alloys ZK51 and ZK61. It was claimed that the higher zinc content increases the sensitivity to microporosity but this is generally only a problem of proper gating and feeding. A considerable number of castings was produced using the two alloys interchangeably with no difference in the handling of the castings. The only important difference is the amenability to two-stage heat treatment as was discussed already (Figs. 5 and 6).

In the field of general purpose alloys the choice of the material to be used for any particular application is in most cases based primarily on the production cost of the casting which depends to a large extent on the foundry characteristics of the alloy. In highly specialized fields, such as the aircraft industry, the cost of materials is small compared with the fabrication costs of the equipment and the choice of materials must be based on their superior service characteristics.

Research on high strength magnesium alloys is conducted mainly, if not exclusively, for the aircraft industry or for the production of airborne equipment, where the highest strength-to-weight ratio is one of the most important characteristics for any material to be used. As already stated magnesium casting alloy ZK61 was developed especially to obtain the highest strength-to-weight ratio "for use in airborne and transportation equipment where weight

is a major consideration."[4] This aim has been achieved, as shown by the comparison in Table 10 of strength-to-weight ratios of various magnesium-base and aluminum-base casting alloys. These ratios were calculated from the specification minima of the tensile properties because these are the values used by the designer.

TABLE 10—STRENGTH-TO-WEIGHT RATIOS BASED ON SPECIFICATION MINIMA FOR VARIOUS LIGHT METAL SAND CASTING ALLOYS

Alloy Designation	Specification Minima			Strength-Weight Ratio		
	UTS, kpsi	0.2% PS, kpsi	Elong.,% in 2-in.	Density	UTS, kpsi	0.2% PS, kpsi
Magnesium Alloys						
ZK61-F	35	18	8	1.83	19.1	9.8
ZK61-T6	42	26	5	1.83	23.0	14.2
ZK51A-T5	34	20	5	1.82	18.7	11.0
AZ63A-T6	34	16	3	1.84	18.5	8.7
AZ91C-T6	34	16	3	1.81	18.8	8.8
AZ92A-T6	34	18	1	1.82	18.7	9.9
Aluminum Alloys						
G10A-T4	42	(25)	12	2.58	16.3	(9.7)
C4A-T62	36	(31)	—	2.81	12.8	(11.0)
SC51A-T71	30	(29)	—	2.70	11.1	(10.7)

Note: Specification minima for alloy ZK61 according to Canadian D.N.D. Specification C-28-97, all other values according to ASTM specifications for sand castings. Values in brackets are for typical (not minimum) properties.

The considerable superiority of alloy ZK61 in mechanical properties as compared with the lower and medium strength magnesium alloys listed, is from the user's point of view much more important than the somewhat more difficult foundry characteristics. Alloy ZK61-T6 should be considered in the same category as other high-quality alloys which have to be handled in the foundry with special care but still are widely used because of their excellent performance in service. Good examples of such alloys are the aluminum casting alloy G10 (10 pct Mg), various aluminum bronzes, stainless steels, etc.

Summary

Based on a previous description of the development of magnesium casting alloy ZK61 further data have been presented on the effect of variations in zirconium and zinc contents on various properties of Mg-Zn-Zr casting alloys and their amenability to heat treatment. Mechanical properties of alloy ZK61 have been compared with those of other magnesium casting alloys and the effect of rare earth additions to Mg-Zn-Zr alloys has been described. Properties of commercial castings have been reviewed and various aspects of foundry characteristics have been discussed.

The modern trend in industrial product design and in materials engineering is to demand the development of "custom-made" materials for specific service conditions. Magnesium-base casting alloy ZK-61-T6 has been developed to obtain a maximum ratio of strength-to-weight for use in aircraft and airborne equipment. As shown in Table 10 the alloy possesses an exceptionally favorable combination of highest strength-to-weight ratio with good ductility. Although the handling of this alloy in the foundry requires somewhat more care as compared with the older Mg-Al-Zn casting alloys with low zinc content, the outstanding mechanical properties favor the use of ZK61-T6 in applications where high strength and low weight are of prime consideration.

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DISCUSSION

Chairman: A. T. PETERS, The Dow Chemical Co., Bay City, Mich.

Co-Chairman: W. D. DANKS, Howard Foundry Co., Chicago.

Recorder: M. V. CHAMBERLIN, The Dow Chemical Co., Bay City, Mich.

W. P. SAUNDERS and J. F. HILDEBRAND *(Written Discussion)*:[1] The author is to be congratulated for a comprehensive treatment of the characteristics of a very interesting alloy. His efforts with the Mg-Zn-Zr system are well-known and this paper would seem to complete the picture.

While most of our work at The Dow Chemical Company's Metallurgical Laboratories has been concerned with Mg+5Zn +Zr alloy (ZK51) we should like to concur with the author in the following:

1. We have found about the same maximum tensile properties with the T61 treatment, that is about 31,000 psi, 0.2 per

cent yield strength and about 46,000 psi tensile strengths in cast-to-size ZK61 test bars.

2. We would agree that the lower aging temperature T52 results in superior properties than those achieved with the T51 age.

3. We would agree with the realistic statement that the ZK51 and ZK61 alloys are more difficult to handle and have somewhat poorer foundry characteristics than the older Mg-Al-Zn casting alloys.

The tensile properties of bars cut from prototype castings "A" and "B" are indeed attractive. It is regretted that neither the casting size of casting "B" nor the gage length of bars cut from casting "B" were listed. Would the author care to comment on the large range of elongation values (2 to 25 per cent) shown for the non-standard size test bars cut from the inspection tests castings referred to in the paper?

While sufficient data about elongation for ZK51 and ZK61 alloys are shown in tables and mentioned in the text, the absence of elongation *vs* zinc content data (or other variable) leads one to ask the author if he has noted any regularity here, as he has for tensile yield and tensile strengths.

As a final note, we should like to discuss the statement that "effective" zirconium cannot always be identified with the analytically determined acid-soluble zirconium content. We should be in agreement if during dissolution of the sample the concentration of the solvent acid were permitted to be high (through improper preparation of the solution or through boiling the sample during dissolution). Tests in our laboratory indicate quite clearly that the concentration of the acid will affect the reported soluble zirconium level *only* if much "free," or "acid-insoluble," zirconium is present in the sample. Since it is not always possible to be certain that the "free" zirconium level is maintained at a low level in production-sized melts, our laboratories have recently recommended[•] using a solution of 1-4 HCl (20 per cent conc HCl solution) and have further recommended that boiling be prohibited. These techniques prevent some of the "free," or "acid-insoluble zirconium" (which is actually slightly soluble) from going into solution and thus erroneously reported as soluble zirconium.

MR. MEIER *(Author's Reply):* The discussion of Messrs. Saunders and Hildebrand is much appreciated, especially as it confirms some of the results presented in the paper.

The weight of prototype casting "B" (shown in Fig. 10) was approximately 5 lb and the gage length of the test bars cut from this casting was 1 in.

The large range of elongation values (2-25 per cent) shown for non-standard test bars cut from aircraft castings was quoted directly from an inspection test report and should not (as it was said) be compared with results on standard test bars. It is known that elongation values on very small tensile test bars (as used in the Hounsfield Tensometer) are usually much higher and show a greater scatter than standard-size bars.

A graphical correlation of elongation values vs zinc content was not shown, but the ranges of typical elongation values for various Mg-Zn-Zr alloys were given in the text of the paper as follows:

for alloys containing	1 — 3% Zn	— up to 25%
	4 — 7% Zn	— 8-14%
	8 — 11% Zn	— 3-5%

[1] The Dow Chemical Co., Bay City, Mich.

[•] Dow Chemical Co., *Analytical Method*, MLW-51.5R, March 27, 1953.

PATTERN EQUIPMENT FOR SHELL MOLDING

By

O. C. Bueg*

The shell molding process is a comparatively new method of producing castings with a smooth surface finish. It lends itself to close dimensional tolerances that are much lower than can be obtained by the conventional green sand method of casting. This new molding process eliminates machined finishes in many cases. It cannot be overemphasized that extreme accuracy is necessary in producing the shell molding pattern equipment, for which we have tried to lay-out the procedure by the following methods.

1. Some patterns do not adapt themselves to shell molding as readily as others. Therefore it is necessary to determine whether the particular item at hand has any saving possibilities of production by this method. When this question has been answered, the pattern arrangement on the plate should be determined to obtain sound castings and produce the highest yield.

2. The next step is to determine the proper alloy for producing the pattern equipment inasmuch as the alloy used often controls the cost of the pattern equipment. Many times the question arises, from the standpoint of annual requirement, whether this cost justifies the savings involved. Aluminum alloy is used to many advantages in making shell molding patterns as often times low production items are manually handled for making shells. Pressure-cast plates are suitable for shell molding patterns if proper precautions are taken in the construction of the master pattern to be used in making up the metal plate.

Where flat partings are used, it is recommended that machined mounted patterns be used as in many cases only one plate is necessary to produce the shell. This particular plate makes both halves, cope and drag. Mounted patterns have another advantage in that from time to time these patterns can be removed for possible alterations. Where higher production is required it is suggested that cast iron patterns be used because gray iron has greater wear and damage resistance. Cast iron also has a better heat transfer value. This alloy can be used at a higher temperature making it possible to produce more shells per hour. It is more adaptable to present silicone release sprays. All pattern castings should be normalized before machining to pattern dimensions.

3. In constructing patterns for shell molding, thermal expansion should be taken into consideration. It is important that the temperature to be used on the pattern be decided upon before production of the equipment is contemplated. Due to the accuracy required in making shell mold patterns, decimal equivalent measurements should be used. The surface of these patterns should be finished to a high polish as any irregularity on the pattern will appear in the shell and therefor on the casting surface.

In attaching the patterns to the pattern plate great care must be exercised not to mar the working face of the pattern. It is better to fasten the patterns from the back side of the plate not allowing screws, pins, etc. to go through to the working face of the pattern as it is difficult to prevent pattern imperfection if this rule is not followed. All pins, screws, bolts, etc., should be thoroughly locked in place for steel products have a greater expansion than the pattern alloy itself having a tendency to work loose eventually if this precaution is not taken.

Gating of Shell Mold Patterns

4. Experience will dictate the need for keeping the gates on shell mold patterns a separate unit of the equipment as many gating arrangements may have to be tried before the desired results are obtained from shell mold castings. One of the important features, where vertical pouring has been decided upon, is to keep the gate sizes at a minimum giving a maximum yield and sounder castings. Generally speaking, gates and risers are smaller than those needed in green sand casting. Considerable gating experience is needed to obtain the best results for shell molding castings.

5. Sand resin retaining strips are advisable on shell mold pattern plates. The thickness of the strips are determined by the thickness of the shell desired which usually depends upon the weight of the casting to be poured. These strips should be tapered for ease of shell release. The area at the largest part should be greater than the dump box upon which the pattern is being used. These strips prevent the uncured shell

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from falling away from the pattern plate when the pattern is returned to its inverted position. It is sometimes necessary to place retaining strips on the face of the pattern plate when insufficient pattern detail is absent.

6. It is necessary to use identical alloys for making of the pattern equipment and the plate, sand strips, ejector plate, etc. Any deviation from this practice will result in strains due to uneven expansion and contraction, causing difficulties after the plate is heated to the desired temperature for making shells.

7. In original pattern plate layout, careful consideration should be given to the push-out pin arrangement due to its importance in extracting the shells from the pattern plate. Various types of push-out pins can be used. The pattern plate should be constructed to receive the type of push-out pin selected. It is advisable to have flush pins wherever possible. However on some types of equipment it is necessary to have restraining pins. These pins need not be of the same material used on the pattern plate as their expansion is not in the same direction as the plate itself. All pins used should be of a uniform alloy. Where a good shell draw machine is in use for ejection of the shell it is most desirable to use a pin lift plate as this provides a more even shell lift and reduces the number of springs required to bring the pins into the original position. This plate has many advantages.

8. Matching buttons should be provided on all shell mold patterns. Extreme care must be taken to assure a perfect match whether a single plate is used or a cope and drag plate. These buttons should have enough draft for ease of matching and provide sufficient rigidity to hold shells in proper alignment to each other.

9. Electrically-heated patterns provide a greater problem than the conventional dump box type of pattern and require careful engineering to produce the heat desired as well as uniform heat transfer to the pattern plate. The shell cycle is also an important factor as well as the amount of sand invested. This point of the operation has a definite cooling effect upon the pattern equipment and necessary steps should be taken to maintain uniform heat over the entire pattern and plate area.

10. The most recent development in the shell molding process is the uniform thickness of shell which helps to control the shell cost. This discovery has many advantages over the shell made by the dump box method. The present expected rate is 480 molds per hour. Pattern equipment itself and also pattern heating arrangements require careful engineering. This method lends itself to closer dimensional casting control because the back-up is entirely different. The back-up frames are a part of the pattern shop's problem. These frames should have the same accuracy as the shell itself and should be constructed so as to give the proper support to the shell and casting. Due to the speed these shells are being produced and the minimum time to place them into the back up frames for pouring off this method has every possibility of surpassing other methods of back-up or green-sand molding.

TRENDS IN CUPOLA DUST COLLECTING SYSTEMS

By

Kenneth M. Smith*

In choosing a cupola dust collecting system today, the first decision which must be made is whether to install a medium cost, medium efficiency system which will remove enough of the stack gas solids to eliminate local neighborhood dust nuisances and avoid foundry roof cleaning costs, but which at the same time will not accomplish any striking improvement in stack discharge appearance, or whether to install a high cost, high efficiency system which will remove practically all of the stack gas solids in order to obtain an essentially clear stack gas discharge.

Medium Efficiency Dust Collectors

The simplest medium efficiency cupola dust collector consists of an enlarged circular shell approximately twice the cupola stack diameter which is mounted directly on top of the cupola stack. This shell is equipped with water sprays for knocking down the dust, baffle plates for catching the dust carrying water drops, and a drain section for discharging the water to a sluiceway which conveys the water and dust to settling tanks. The sluiceway, the settling tanks for separating the dust from the water, and the spray water pumps can be installed in the most convenient available space, thus requiring only minor building changes from the installation of this type system.

Dry dynamic dust collectors of either the rotary impellor or fixed vane types are also used in medium sprays will be required in the ducts to reduce the gas efficiency systems. Comparatively low volume water temperature in order to protect the dust collector. The installation of the collector itself may require building changes, but settling tanks will not be required. Consideration must be given to the problem of disposing of the collected dry dust.

Conventional wet type foundry dust collectors also can be used as medium efficiency cupola dust collectors by installing gas cooling water sprays in the ducts ahead of the dust collector. These wet collectors may removal conveyors, or they many discharge through have self contained sludge settling tanks and sludge removal conveyors, or they may discharge through a sluiceway to settling tanks. The installation of these collectors frequently require building changes because of the collector size and weight.

The electrostatic dust collector so widely used in power plants has as yet not been obviously successful in producing a clear stack gas discharge from foundry cupolas because a high per cent of cupola dust is very much finer than steam boiler dusts. The electrostatic collector to date should be classified as a medium efficiency cupola dust collector. Water sprays for cooling the cupola stack gases are necessary to protect the dust collector materials against temperatures above 500 F. They are also necessary because high humidity of the dust-laden gases is needed in order to accomplish a useful collector efficiency. In order to maintain a uniform collector efficiency a sectional collector design should be developed which will stop the gas flow in each section while the accumulated dust is being vibrated from the collector plates so the dust can fall into the collector hopper without being carried back into the collector.

There is some hope that lower voltage, lower cost, high efficiency electrostatic dust collectors suitable for cupola service will be developed.

High Efficiency Dust Collectors

Up to the present time only two similar dust collector designs have been successful in producing a clear stack discharge from the cupola. One collector design uses large diameter glass cloth filtering tubes arranged vertically. The second collector design uses smaller diameter orlon synthetic fiber tubes also arranged vertically. The glass cloth will withstand operating temperatures up to 500 F, and the orlon cloth will withstand temperatures up to 275 F.

Water sprays are used in the ducts to control the gas temperature reaching the collectors in order to protect the cloth tubes. A primary separating chamber is used ahead of these collectors to remove the large dust particles.

In cold weather visible steam may form as the highly humidified collector discharge gases mix with the cold outside air.

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Oil Fume Removal by Hot Blast Systems

The volume of oil smoke produced by oily sheet steel, briquetted steel turnings, or oily briquetted cast iron borings which may constitute an appreciable part of the metal charge should be considered in choosing a cupola dust collecting system. This oil smoke is a definite nuisance when winds carry it down into the surrounding neighborhood.

The most effective way of removing this oil smoke is by burning it in a combustion chamber or in a tall cupola stack. Since a combustion chamber suitable for burning this oil smoke is a necessary part of the heat recovery type of cupola hot blast air heater system, the economics of such a system should be carefully studied.

The above type of hot blast system may increase cupola melting rates as much as 20 per cent, and the coke consumption per ton of iron melted may decrease as much as 15 per cent. Fortunately the wider range of cupola hot blast system sizes now available makes their installation practical on smaller diameter cupolas than has been the case in the past. The hot blast heat exchanger may be followed by either a medium or high efficiency cupola dust collector system for which the heat exchanger will serve as the primary collector for the coarse dusts.

Conclusion

If an inconspicuous cupola stack discharge is desired, a collector efficiency greater than 95 per cent by weight will be needed because of the extremely small size of the metal oxide fumes. If a clear stack discharge is desired, collector efficiencies better than 99 per cent will undoubtedly prove necessary. In order to maintain such high efficiencies, continuous dust discharge designs will be needed for all types of cupola dust collectors.

REFRACTORIES FOR INDIRECT-ARC ELECTRIC FURNACES FOR NON-FERROUS MELTING

By
Martin G. Dietl*

Of all the problems that beset a foundryman, perhaps none is so vexing as the interrupted flow of ready metal caused by failure of the furnace lining. At that time it is not only important to hold the workers to a continued high standard of performance, but also that the immediate problems associated with furnace repair be solved.

At one time fireclay brick type refractories were supplemented with mullite-type refractories in a furnace to produce a "balanced" unit; one in which failure of all parts occurs simultaneously. The objective was to produce maximum tonnage or output at lowest refractory cost per ton. In recent years, however, due to high labor rates and the desire for longer life and furnace availability, the 100 per cent mullite-type of refractory has gained a great deal in popularity, and in most cases has shown economy.

Refractory Selection Factors

With so many different refractories on the market, the furnace operator should be able to select the one, or a combination, that will do the best job. The various types of refractories are made with differences in grain sizing, structure and firing temperatures. Even after long experience both the manufacturer and user of refractories cannot make definite recommendations due to variations in service conditions—not the least of which is the human element. The best refractory for the job can be determined by considering the following factors:

1) What is the furnace operating temperature? High—above 2600 F; moderate—2300 to 2600 F; low—below 2300 F.

2) Are the refractories heated only from one side?

3) Is melting continuous or intermittent? Are temperature fluctuations rapid or slow?

4) Is there any direct flame impingement upon the refractory? Are the hot gases laden with fluxes or dust? What is the gas velocity?

5) Is the furnace atmosphere oxidizing or reducing?

6) Are the refractories acted upon chemically by molten metals or metallic oxides, or by fluxes such as slags?

7) Are the refractories subjected to heavy loads or abrasions at high temperatures?

8) Is the heat to be conducted or retained?

9) Need electrical conductivity or dielectric strength be considered?

10) Possible effect of furnace design. Is the construction good or bad?

The best material for a particular purpose is generally determined by one or two essential requirements, while the others assume a minor role.

Testing of Refractories

Tests have been devised for measuring the resistance of refractories to the many factors which cause their destruction. Again, no test has been developed which will simulate service conditions and account for the human element. Refractoriness in itself is measured in two ways: 1) as P.C.E. or softening point; 2) As refractoriness under load.

If the refractory is largely crystalline material, like silica, mullite, or sillimanite, the P.C.E. alone will give sufficient indication of the refractoriness. On the other hand, refractory materials have no definite melting point, as metals have, but soften gradually under heat. For that reason the so-called melting points of clay-base refractories, containing amorphous or glassy constituents, is merely the temperature at which a definite degree of softening has occurred. In such cases both P.C.E. and load tests are essential if serviceability is to be predicted with any degree of accuracy.

Pyrometric cone equivalent (P.C.E.) is a measurement of softening behavior as indicated by the temperature at which a cone of the refractory touches the base in which it is mounted when heated at a given rate, as compared with a standard cone.

Deformation under load is determined by the percentage subsidence after heating a whole brick uniformly, at a predetermined temperature and under a given load, usually 25 or 50 psi. In general furnace construction the brick are usually heated on only one face; the cooler portions will carry a load up to

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1,000 psi without settling, provided the wall is not insulated.

Refractories which are predominantly crystalline, such as silica, sillimanite, or mullite, silicon carbide and corundum, are usually outstanding for ability to carry heavy loads at high temperatures without subsidence. Having a sharper softening range, they can be insulated without suffering as much loss of either hot strength or load-carrying ability. The development of a crystalline, mullite structure in fireclay brick by special heat treatment increases the rigidity under load.

Shrinkage is an important cause of failure and can be tested at any desired temperature. By proper grain sizing, selection of bond and proper burning treatment, manufacturers have reduced or eliminated residual shrinkage in many of the commercial products. The super-duty fireclay brick is a case in point.

Spalling of Refractories

Spalling is divided into three classes: 1) thermal; 2) structural; 3) mechanical. The spalling properties of refractories have been studied extensively and are not only well understood, but also means for testing have been so well developed that noteworthy improvements have been made in several classes of available products. It has been shown repeatedly that thermal spalling is a function of thermal expansion, elasticity, plastic flow, and heat transfer. If the brick had no expansion upon being heated, it could not spall thermally, since no stresses would be produced.

The application of heat to one end or face of a brick causes the heated area to expand. If the brick does not have sufficient elasticity to conform to this increase in size of the heated end, it will crack or spall under the strains that are set up. Again, if the refractory possessed a higher degree of heat transfer, the difference in temperature between any two points or planes would be minimized. Any difference in size would be reduced, thereby modifying the strains which cause spalling. With ordinary fireclay brick and shapes containing plastic or semi-plastic clays as bonding agents, the usual method of increasing the spalling resistance is to use larger portions of raw or calcined flint clay.

Structural spalling covers the loss of refractories caused by a change in their structure during service. This is an important point to remember when checking spalling of refractories in the linings of indirect-arc furnaces. Shrinkage is one of the many conditions produced by service, usually as the result of a reaction at the hot face of the refractory and the furnace charge or slag. The difference in dimension between a slag soaked surface and the remainder of the brick may cause a separation, more pronounced in furnaces which are brought up rapidly to a high temperature.

Mechanical spalling may occur as a result of lack of provision for the expansion of brick work.

It is characteristic of the plastic and semi-plastic clays used in super-duty fireclay brick to develop rather large percentages of glass when fired, or used at high temperatures. This is caused by impurities or fluxes present in the raw material and the fineness of the grind. It is this vitrification which results in brittleness when subjected to spalling strains and which produces shrinkage through surface tension.

The development of the non-spalling super-duty brick is the result of the elimination of these vitrifying clays, using pure grades of flint clay instead, and carefully controlled grain sizing and particle shape. The bond in this brick is obtained by close packing due to scientific grain sizing, higher molding pressures, and hard burning. The porosity is naturally low, and density or weight per cubic foot is high. The hot-load strength is only fair, however, as the grains are bonded only at contact faces or points.

Attack of Refractories by Slag

Slag attack is one of the most destructive forces determining the life of foundry refractories. The oxides of metals attack refractories more readily than the metals themselves. The more common fluxing oxides encountered are:

FeO and Fe_2O_3	ZnO	MgO
CaO	PbO and Pb_2O_3	Na_2O
CuO	B_2O_3	K_2O

All of these react with aluminum silicate refractories to form low melting compounds. Slag reaction usually causes the formation of such compounds. The phenomenon begins in the charge itself, and the slag formed progressively dissolves the constituents of the refractory. Ultimately an equilibrium would be reached, but metallurgical processes are seldom long enough for this. An equilibrium established at a given temperature will be disturbed as the temperature increases. Since most heats are brought up to temperature as rapidly as possible, the slag is always changing composition. As a rule it becomes more fluid and active with regard to the refractory lining.

If the slag is turbulent, it washes the lining and the reaction products are carried off, exposing new surfaces to attack. A viscous slag will not destroy the refractory as rapidly as one which is fluid, other conditions being equal. The reaction products will not diffuse through the viscous slag as readily as through a fluid slag. This tends to retard the rate of attack due to the establishment of a partial equilibrium.

With the foundry industry constantly seeking materials of better life, less maintenance, and lower refractory costs per ton melted, improved furnace linings were developed. In the production of the mullite or sillimanite type of refractory the kyanite rock is crushed and then calcined in a rotary calciner at a temperature of 3000 to 3100 F to convert it to mullite. The calciner product is then further crushed and graded into several grain sizes, the coarsest material being about 4 mesh.

In compounding a batch the required weight of each grain size is drawn from storage tanks and placed in a mixer or blunger according to the method of forming to be employed. In the case of dry mixing, the bond is then added to the mullite and the batch mixed carefully for a definite length of time, and tempered with water to contain the proper amount of moisture for pressing, ramming or soft-mud forming in plaster molds. After forming the refractory by any of these methods, it is carefully dried to a maximum of 1.5 per cent moisture, and then fired in a

continuous tunnel kiln to a temperature of 2960 F.

After firing, standard brick and shapes are ready for inspection and shipping. Some special shapes and close tolerance items must be finished after firing by grinding to meet specifications and assure a proper fit of furnace lining components. In all these steps careful control must be exercised throughout, beginning with raw material analysis and on down through calcining, mixing, grain size control, forming, drying, and firing.

A typical analysis of kyanite-mullite is as follows:

Formula $Al_2O_3SiO_2$—kyanite	
Al_2O_3	59.4 per cent
SiO_2	38.4 per cent
TiO_2	0.9 per cent
CaO	0.6 per cent
Fe_2O_3	0.5 per cent
MgO	Trace
Specific Gravity	3.56-3.67

Kyanite inverts to mullite ($3Al_2O_3SiO_2$) and siliceous glass at a temperature beginning at 2460 F with a formation of about 14 per cent siliceous glass, a drop in specific gravity to 3.1, and an increase in volume. Conversion is slow at lower temperatures, and in commercial practice the temperature should be at least 2910 F. This material has found widespread refractory use because of its ability to withstand high furnace temperatures without reacting with the material being melted or with fluxes, or furnace gasses. It makes a superior refractory because of absence of softening below the melting point of 3300 F, high mechanical strength, good insulation at high temperatures, resistance to shock, and low, uniform thermal expansion. The strength is due to long interlocking crystals which provide, in a mullite-type body, excellent resistance to deformation under load at elevated temperatures (Table 1).

TABLE 1—CHEMICAL ANALYSIS AND PHYSICAL PROPERTIES OF VARIOUS GRADES OF MULLITE-TYPE PRODUCTS

	Standard Grade	67% Grade	72% Grade
Al_2O_3	61-62	67-68	71-72
SiO_2	34-36	29-31	24-25
Fe_2O_3	0.50-0.70	0.65-0.85	0.40-0.60
TiO_2	1.25-1.50	1.35-1.65	0.50-0.75
MgO	0.20-0.30	0.20-0.30	trace
CaO	0.20-0.30	0.20-0.30	trace
Alkalies	0.30-0.50	0.30-0.50	0.60-0.80
Weight per ft³	148 lb	150 lb	145 lb
P.C.E. Value cone	37 plus	38 plus	39 plus
Reheat shrinkage at 3000 F	0.35	0.0	expansion 0.52
Apparent porosity, %	21.5	19.7	28.7
Bulk Sp. Gr.	2.38	2.40	2.31
Spalling resistance	Good	Good	Fair

In the past dozen years or so, the indirect-arc rocking furnace has undergone many improvements. The adoption of the shell with conical ends has increased furnace capacity and improved refractory service as the arc has been moved away from the lining. Developed for melting brass and other copper-base alloys, the newer furnaces also are finding a ready acceptibility in the ferrous melting field. Since the roof area of the lining above the arc is not protected by the metal bath and thus is subjected to the direct radiation of the arc, a fundamental weakness of the furnace develops.

The refractory in this portion of the lining may run as much as 300 to 500 F hotter than the metal. Lining breakdown is imminent. Therefore a mullite or sillimanite type of lining is preferred over the super-refractory clay-type lining. The increase in price of the high temperature lining is actually a saving when examined in the light of today's high labor costs. Also, lining life can be prolonged by careful patching with suitable refractory cements of comparable composition. At one time, many old-design, straight-shell indirect-arc furnaces were operated with completely rammed linings; however, the cost and nuisance of installing and burning in such one-piece, or monolithic, linings, plus the aforementioned improved furnace design, lead to the development of superior bricks and special shapes.

These refractory improvements resulted from graduated grain sizing, controlled particle shape, better methods of packing and molding practices, and higher burning treatments. The use of super-duty refractories, 72 per cent Al_2O_3, is highly questionable in the indirect-arc furnace. Normally the furnace operates at temperatures too low to allow the thorough vitrification of the mortar to take place. A good joint will result only if such a setting takes place at 2800 to 2900 F. However, the super-duty refractory is needed to withstand metal, slag, and fume attack. On the other hand, a monolithic lining is thermoplastic at the lower temperatures, and also affords a better continuous bond instead of the brick joints.

Installation of Refractory Lining

The actual installation of a special shapes lining should be performed with care. Obviously the steel shell ifself, in order to take the new lining, must be in a clean and well repaired condition. Then only can the new lining be fitted with a minimum of grinding and cutting of the last brick. As the type LFC shell stands on end, the lining installation begins with the laying up on the insulation, either cut from straight brick, or consisting of properly marked and indexed shapes to suit the particular furnace size.

The use of the new castable insulation will be described later. The larger furnaces, like type AA conical shell, having no removable end plates, lie in the horizontal position; all the work being done through the charging door. If the insulation is placed in the furnace with great care, it may stay in the unit to receive several new refractory linings. As a rule the fitting of the insulation is not too difficult. It is advisable to allow a completed insulating lining to dry for 24 hr before work is begun on the placing of the refractory. In the case of the type LFC, and LFY furnaces, the refractory end wall is a large, single-piece disc.

After being placed in position, it should be rammed solid with dry ramming cement. Proper grooves on the outside diameter of the end wall receive the supplementary grooves of the special shapes that constitute the barrel of the furnace. The lining fits together in much the same way as the segments of an

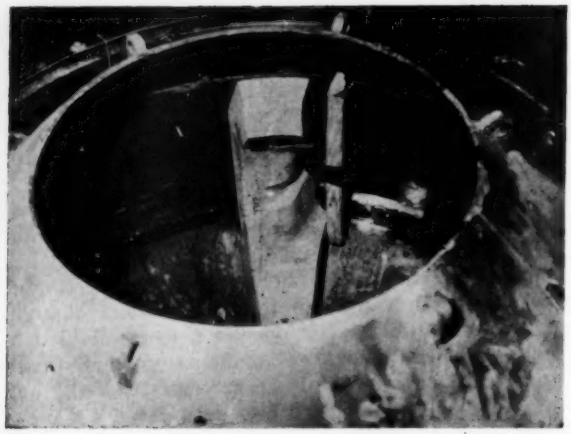


Fig. 1—The first sillimanite course sector has been placed beginning at the door sill. Proper placement of this block is important.

orange. The centers of the end discs are punched out to receive the electrode with its electrode sleeve. Again, great care is to be exercised in buttering all sides of all refractory shapes in such a manner that a tight joint is produced every time.

In spite of the fact that the refractory supplier does his best to supply a well fitting lining, more often than not it becomes the duty of the workman to fit the last lining segment. Usually a bricklayer's hammer and a grinding wheel are used to whittle the piece to fit. The door jambs are best supported by a wooden block until the completed lining has set. Finally, the second end wall, having been mortared into place, is also rammed tight with dry cement. The end-wall insulation is cut from regular straight brick and laid into place. The steel end plate is replaced and wedged tightly.

Type LFC, LFN, and LFY furnaces of more recent manufacture are supplied with two rows of drilled and tapped holes around the cylindrical part of the shell. These are used for supporting studs for the refractory as shown in Figure 2. If the shell does not have these holes, they should be provided before proceeding. The holes can be located by loosely setting in place the refractory shapes, and drilling and tapping two holes for ⅜-in. bolts behind each circular course as illustrated. These holes will also serve as vents when drying and preheating the new lining. The thickness of the outer lining behind the end disc is 2½-in. for the above types of furnaces. This thickness for the lower end disc can be provided in several ways:

If a few 9x4½x2½-in. insulating straight brick are available, the end wall can be laid up in the aforementioned manner by cementing in place with a suitable cement. In this case the brick need not fit the circle of the shell as the castable material will later fill any voids at the outer edges.

A pre-cast insulating end disc 2½-in. thick with approximately 24-in. outside diameter and 8-in. inside diameter may be used by making up such in a simple ring form. A coat of cup grease applied to the surface of the form will make it easier to withdraw after the castable material has set. A suitable amount of the castable material should be mixed according to the manufacturer's instructions and poured into the form. Allow the cast to stand at least 4 hr to set up firmly before it is placed in the shell. This piece can be made beforehand.

Also, the insulating end disc can be cast in the shell. A wooden plug or mandrel is inserted in the port hole opening. A few 9x4½x2½-in. insulating brick are broken and placed strategically for spacers in the bottom of the shell, and to serve as a height gage in pouring the castable material. After allowing to stand for one hour, the castable material has developed enough strength to support the refractory end disc, this time being made up of two halves. Care must be taken that the end disc is flat, properly centered, and has a good cement joint.

Even though the complete refractory lining was carefully fitted and assembled in a steel gage prior to shipping, it might be well to install all the circular course sectors loosely in place without cement to insure a good fit. The ⅜-in. bolts fitted in the tapped holes are used as set screws to adjust the sectors in their proper location. With the set screws and a plumb-bob, it is possible to keep the joints vertical and the open end concentric, and of the desired diameter. It is vital that the face joints are kept radial.

The permanent laying up of the circular sectors starts with the door still clamped in place as shown in Fig. 1. It is important to start with this first piece carefully located and in line or trouble will be encountered with each successive piece. Wetting the bonding surfaces of the refractory shapes before applying the cement will prevent rapid drying of the cement and make it easier to slide the blocks in place, and to obtain uniform joints.

A full joint should be troweled on the bonding face and on the end which fits against the end disc. Slide and tap into place, squeeze out the excess cement (Fig. 2). The door jambs are installed last, and wedged securely into place to obtain a tight fit (Fig. 4). A good cement joint must be obtained when the top refractory end disc pieces are placed in position. Waste or rags stuffed around the shapes which make

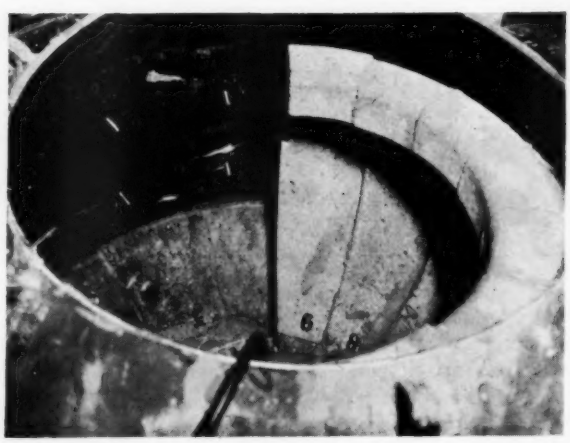


Fig. 2—Sillimanite course sectors in place and bonded. Supporting stud locations are shown.

up the door opening will prevent the castable material from running out when the insulation is cast in place.

A cement mixer can be recommended to mix the castable material due to the large volume involved. Air pockets and any possible voids in the insulation can be worked out by inserting a thin steel rod between the steel shell and the refractory. A wooden plug or mandrel in the top end disc opening will stop the castable material from flowing into the opening when the top end wall is poured. The loose steel end plate can be locked into place as soon as the castable is poured. After the cast insulation has set, the ⅜-in. adjusting bolts and the waste around the door can be removed. The shell should be allowed to set up over night before the door is filled in with refractory patch.

Best results will be obtained in service if the new lining has been thoroughly dried and preheated before charging the first heat. A coke fire or lazy gas torch will help to expel mechanical water before preheating with the arc. Six to 8 hr at 300 to 400 F is sufficient. The furnace should be preheated with the arc according to the following schedule:

5 KWH once each one-half hour for 2 hr
8 KWH once each one-half hour for next 2 hr
12 KWH once each one-half hour for next 2 hr

At the end of this time a total of 100 KWH has been applied and the lining should be a dull red. The power may then be left on continuously at 100 KW input until 20 additional KWH have been applied, or until the lining temperature reaches approximately 2700 F. The power is then turned off, and the lining allowed to cool over a 30-min period to a bright red before the door is removed. The lining is then ready to receive its first charge of metal.

In the case of the type AA conical shell furnace, the barrel sections are split perpendicular to the furnace axis, but are so marked that in assembly the vertical joints are offset. Again, the furnace is placed on end to install the insulating shapes; the refractory lining, however, is installed as the shell lies on its horizontal axis. A helper is required to pass such shapes to the workman inside the furnace. Also, both end walls and the barrel sections are built up together, the furnace being rolled as the work progresses.

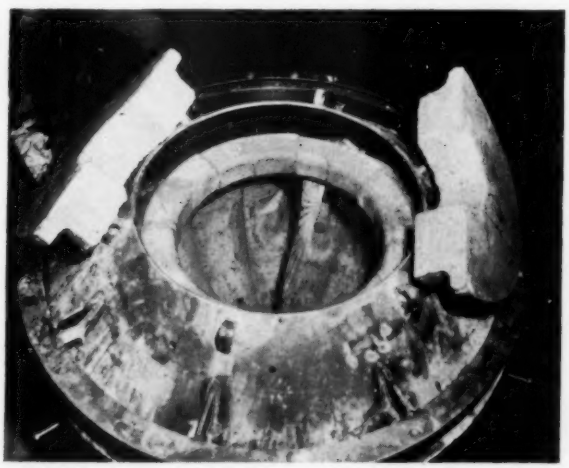


Fig. 3—All sillimanite course sectors and door jambs are securely wedged in place. End discs are ready for placement.

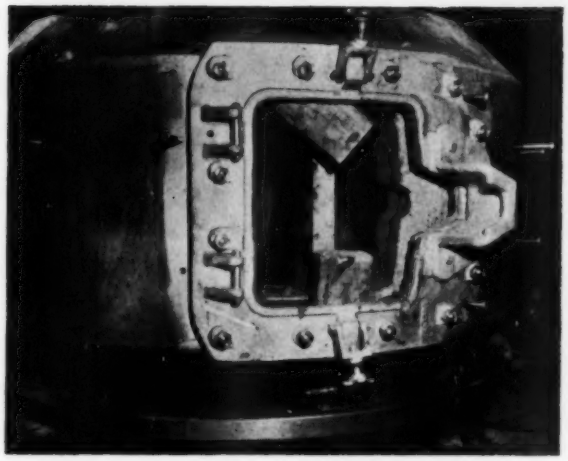


Fig. 4—Door opening before castable refractory is poured and before the door has been sealed.

The conical shell lining does not require electrode sleeves as a tight fit is accomplished with the electrode porthole cooler and the electrode. Some practices claim that silicon carbide (SiC) shapes have proved unexcelled for door blocks, and frequently for the assemblies around the door openings, as this refractory has outstanding resistance to destruction from heat, shock, and mechanical abuse when hot. The writer's experience has been that not enough advantages were realized by this type of practice since even in the smallest lining repair, the door assembly is knocked out to facilitate the repair in the furnace interior.

In the case of preformed and prefired SiC door bricks, enough dimensional variations existed in fitting the brick to the steel door and its bolt holes that a great deal of work was necessary to make a furnace door. Even then, the SiC shape suffered whenever the steel bolts were drawn too tight to permit easy expansion of the door. Usually failure occurred through the bolt holes.

This defect was in part overcome by ramming in a SiC cement shape right on the steel door. Doors were imbedded in the rammed mass which was shaped by a removable steel frame. The SiC rammed in the door was allowed to dry in a hot room, the final firing being performed when the door was placed in use. The completely lined furnace shell should be allowed to air dry for 2 or 3 days before a charcoal fire is introduced. The longer such slow heat can be applied, the slower the lining dries, actual mortar vitrification being deferred until the furnace is installed and used.

Mention has been made that electrode sleeves were not used. Experience has shown that in any sleeve repair such as the removal of entrapped metal spillings, the sleeve is easily cracked. Such cracks further the chances of additional metal leakage to the elec-



Fig. 5—Pouring castable refractory mixture. Limber steel rod or strap is worked through mixture to consolidate lining.

trode. A short circuit results. In place of the sleeves, each electrode is wrapped with heavy wrapping paper at the sleeve section and is clamped in the electrode holder. After the electrode holder brackets are adjusted so that the electrodes meet on center in the middle of the furnace, the sleeves are rammed up with dry ramming cement. During the preheat of the furnace the wrapping burns, leaving the proper electrode clearance. It is advisable that the sleeves always be washed with alundum cement. Higher refractoriness and electrical insulation are gained.

Drying New Refractory Linings

Since the preheating and melting practices are significant factors determining refractory performance, a slower rate of heat input in drying out the new lining will reduce refractory costs. Preheated according to the foregoing schedule, the LFC type furnace should have a refractory life of from 800 to 2000 heats, depending on whether the melting unit produces high-nickel alloys, or red brasses. Similarly, with reasonable refractory repair, the LFY unit should be good for about 3000 heats; and the AA conical shell furnace will produce about 5000 heats before a complete relining is necessary.

In the case of the type AA straight-shell furnace, the refractory problem is somewhat different. Being built completely of straight and arch bricks, this lining has many joints as compared with the same capacity furnace of the type AA conical shell. In the latter, the best super refractory is desired; in the former, a super-duty fireclay brick does a good job because it builds a balanced unit. The many joints are the weak spots in that furnace. Care exercised in laying brick always pays big dividends in longer refractory life.

Wide, poorly filled joints of a doubtful cement or bond are vulnerable spots where slag and flux laden vapors cause premature breakdowns. Bricks should be laid with thin, even joints, using a cement of approximately the same softening point and chemical characteristics as the brick. The air-setting cements containing water glass should be avoided since the possibility exists that the furnace may be reducing. Free silicon will then be liberated. The clean and well repaired furnace shell is lined first with standard insulating brick. The super-duty fireclay bricks are laid in the body and end walls of the furnace.

Depending upon the care and experience of the melter, it may prove desirable to ram the end walls only with a sillimanite type ramming cement before the fire brick is installed in the body of the furnace. Again, the porthole sleeves and door assembly are rammed with cement. Slow and careful preheating practices are to be followed as in the drying and curing of any newly installed furnace lining. In this respect the use of an alternate shell is very advantageous and time saving.

At the time the shell is to be put into operation, the electrodes are again lined up and the sleeves rammed into position. It is very advisable that a water cooling coil, made up of ½-in. pipe, be imbedded in th rammed sleeve. Such practices tend to reduce lining breakdown by lowering the electrode and sleeve temperatures at that point. Reference was made to the door assembly being rammed with a mullite type cement.

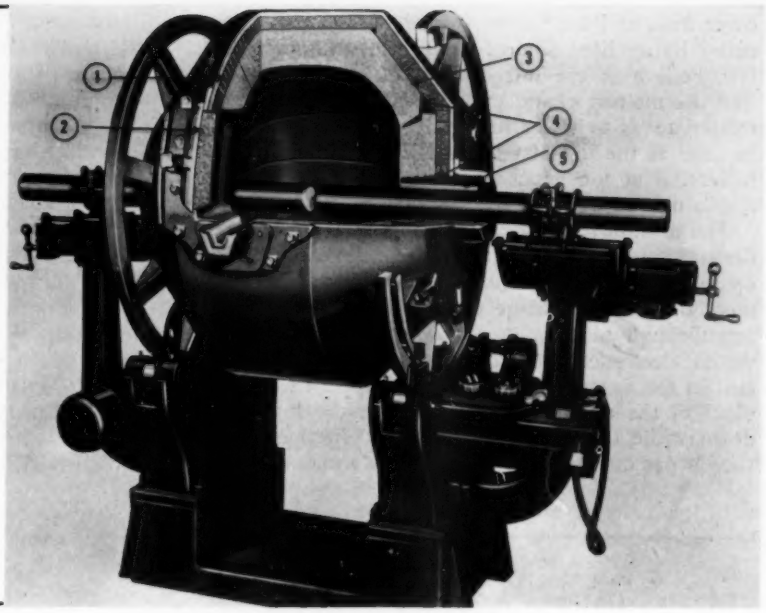
This method exhibited better service characteristics over the conventional brick assemblies because of inherent operational drawbacks of the latter. Primarily the furnace was run on what approaches a continuous melting cycle; scrap, equivalent to the amount of metal tapped out, was charged to the constant bath in the furnace. Since the furnace was rear charged, the door lintel served as a convenient rest for the charging scoops. The excessive abrasion caused a premature breakdown of the door refractory. Six to 8 months of service could be realized with a production of 14,000 lb per day from this type of lining. The lining must be kept in constant repair.

Such a standard brick lining could similarly be installed in the type LFA furnace. However, if the furnace is to be used for such severe service as the



Fig. 6—Castable lining has been completed. Best results are obtained if new lining is dried 24-48 hr and preheated before charging the first heat.

1. Pre-cut insulating shapes
2. Rammed-in door assembly
3. Preformed and prefired refractory shapes
4. Refractory cement
5. Electrode porthole sleeve



Typical refractory and insulation installation for indirect-arc electric furnace.

melting of high-nickel alloys, monel, and pure nickel, it may prove economical to ram up a monolithic lining composed of a mixture of high grade gannister, 3/8-in. mesh and down, together with high quality fire clay. Just enough water is added to produce a good temper for ramming. Some of the nickel alloys are best melted under a heavy bottle glass slag which would readily attack a mullite type lining, and also the gannister.

However, since the latter is relatively inexpensive and easily rammed into the empty furnace shell, using a steel form to make the inside diameter of the lining, a lower refractory cost results even though the gannister lining is completely discarded after 150 to 200 heats of such service. No great patching repair is advised. Also, this type of lining is used without any insulation. Lining life is prolonged by the use of 1/2-in. water cooling coils in the electrode porthole sleeves, and in the rammed-up door assemblies.

Zircon as a Refractory Lining

The quest for better lining and refractory performances may lead to the application of a zircon lining. The zirconium oxide is highly refractory and resistant to thermal shock. The high density of the material lends itself readily to withstand the abrasive action of frequent charging of cold scrap. However, zircon is essentially acid in character, and is readily attacked by slags made of bottle glass. Under those conditions the zircon lining gave a very poor performance. For the melting of red brasses and bronzes, the use of silicon carbide in prolonged contact with the metal is to be avoided, since this will cause silicon contamination. If the refractory is covered with a glaze, no trouble results.

Reduced lining life or premature lining breakdown may be caused by several factors; most prevalent among these are melting malpractices, others cannot be traced directly to the melter. The first and most obvious section of lining failure is caused by the abrasion at the point of impingement of the cold metal charge. This condition is aggravated by the use of charging buckets which literally dump the whole charge into the furnace. Hand charging, or allowing the gates and risers to fall upon a small pile of borings greatly alleviates this trouble.

Another defect which might be attributable to the charging process is the damage suffered by the door assembly. Once a condition has been provoked whereby the charging door no longer properly seals the furnace, leaking hot gasses will further the breakdown of the door assembly with the subsequent burning of the steel shell and door. Without the support of these steel reinforcements, any refractory repair around the door is soon destroyed.

Mention has been made that SiC has proved unexcelled for door blocks and frequently for the shapes around the door openings as this material has outstanding resistance to destruction from heat, shock, and mechanical abuse when hot. But, to repair the inside of the furnace, the workman first chips away the door assembly for the convenience of more work room. The expensive material is thereby destroyed. Another type of failure over which the melter has only partial control is brought about by the melting characteristics of the furnace.

The roof area of the lining is not cooled by the metal during the rocking of the furnace, but is exposed to the direct radiation of the arc. The refractory is overheated at this point causing the natural melt-down or fluxing of the lining to occur, especially so if the metal bath is unnecessarily heated too high above the pouring temperature. Realizing this weak point, the furnace builder has advanced from the LFA type to develop the new conical shell design which removes the refractory from the vicinity of the arc.

Reference has been made to the use of glass as a

cover flux, and the effect of outright fluxes in shortening lining life. Some foundrymen, although very few, believe in the introduction of a stream of air into the melting chamber of the furnace. The extra oxygen tends to lower lining life in much the same manner as the use of oxidizing fluxes promotes slag formation at the expense of the lining. This practice is not widespread.

The most obvious cause of lining breakdown is the destruction brought about by the negligence of the operator. Electrodes broken, either by the impingement of the cold charge or by the premature rocking through too large an angle of rock, not only wastes electrodes, but will also produce a short circuit in the electrode sleeve if that happens to be the place of the rupture. In either case, the furnace is down while the damage is repaired. In the case of the burned-out sleeve, constant trouble may result by liquid metal splashing into the cavity and setting up additional short circuits. The only positive repair consists of knocking out completely the old sleeve and rebuilding it.

Misalignment of the electrodes will produce arcing in the electrode sleeve if the connecting screw between the electrodes should break. Great damage to the furnace end walls will result if the melter, through negligence, allows the arc to burn too far away from the center of the furnace. Preferably, the arc should be so adjusted that the non-automatically fed electrode projects 2 in. beyond the furnace center at the beginning of the heat. The arc will then burn away from the end walls during the melting cycle. Once the side wall is gone, there is great danger of melting out the furnace shell itself, or liquid metal will have a tendency to flow into the electrode sleeve.

Mention should be made at this point concerning

TABLE 2A—INDIRECT-ARC FURNACE MELTING PRACTICES

Response No.	Normal Av. Daily Melt Capacity, lb/day	No., Rate & Capacity of Furnace	Daily Operation, hr	No. of Heats & Tons/Day	Alloy	Pour. Temp. F	Nature of Castings Made	Nature of Charge
1	14,000	4 LFA 350 lb ea.	9	10 1.75T	85-5-5-5 Navy M	2280 2300	Light	Gates Ingot No turnings
2	116,000	4 CC 3000 lb 3 BB 1500 lb 2 LFA 300 lb	8 3CC fce, 24 hr	18 16T	Red Brass Phos. Bronze High Lead Bronze	1950 2300	Light Med.	95% Scrap 3% Ingot Turnings clean & dry
3	25,000	2AA-S 750 lb 1AA-C 1000 lb	8	5 2.75T 3 3.3T 1 .5T	High Pb bearing alloy Red Brass	2225 2300	Light Med.	Virgin Metal Scrap Cu Turnings wet & oily
4	5,000 to 8,000	2 LFY 750 lb	8	4 1.25T	Ni Silver Red Brass Bronze	2900 8300	Light Med.	65% Ingot 35% Gates No turnings
5	4,200	2 LFA 250 lb	8	7 1T	Pure Nickel	3000	Med.	Scrap Virgin Metal
6	9,600	2 LFA 350 lb	8	12 2.5T	85-5-5-5 Navy M	2200 2300	Light	Mostly Ingot Dry turnings
7	30,000	5 LFY 500 lb	8	12 3T	Red Brass Leaded Ni Bronze	2100 2600	Light	40% Ingot 10% Dry turn. 50% Clean Gates
8	4,000	3-125KW 225 lb	8	16 1.5T	Red Brass	2200	Med.	Ingot Gates & Dry Turnings
9	2,000	1 LFY 500 lb	7	7 1T	Brass Bronze Monel	2200 2600	Light Med.	Ingot Gates
10	22,000	1AA 1000 lb 5 LFA 350 lb	8	6 3T 8 7.5T	Leaded Red Brass	2150	Light	Ingot Gates Dry turnings
11	120,000	1 LFA 350 lb 4 LFC 350 lb 3 LFY 500 lb 4AA-C 1000 lb 1-AA-S 750 lb	8 Exclusive of pre- heat	12 1.5T 20 2.5T 15 5.5T 12 7T	Nickel alloys Red brass	2900 2100 to 2400	Light Med. Heavy	Gates Ingot Clean & Dry turnings
12	Not given	1 LFC 1 LFY	8	Not given	Red brass	2200	Light Med. Heavy	60% Gates 40% Ingot

the use of dirty borings in the metal charge. Not only will such practice fall below the accepted metallurgical standards, but also such usage will produce excessive slag build-up in the furnace. As the capacity of melting chamber is reduced, the liquid metal line is raised till it reaches the electrode portholes. Lining failure will occur in the sleeves.

Proper preheating of a newly installed lining must take place in that the binder used in the construction of the furnace lining or in the laying-up cement has been sufficiently stabilized or removed. Sometimes, in the interest of more production, such preheating is cut short. Excessive melting of the lining results; perhaps to such a degree that a regular sheet of solid slag covers the entire metal bath. This same situation may result if the furnace is allowed to melt without rocking over an extended period of time.

All refractory repair naturally is an attempt to prolong lining life and to maintain production. Cases have been cited whereby an unlimited lining life has been approached through the use of adroit repair practices. The author claims that while perhaps the original lining in these cases has not been replaced; melting is done on a patched refractory which is constantly renewed.

The practice of lining repair may fall into several distinct classifications. Shallow patching, performed daily, of so-called trouble spots, or incipient failures; weekly repair consisting of a more thorough deep patching; and the alternate shell method whereby the furnace shell is removed from its base so that the lining can be completely overhauled. The secret of any successful patching technique is the cleanliness of the area to be repaired.

The weekly repair is best preceded by a slagging of the furnace whereby the open door is rotated downward so that the fluid slag runs from the furnace. The arc will accomplish this in about 75 KWH. As a rule, deep patching is performed with a large grog size cement; shallow patching is best done with a fine grog size material. After all slag and adhering metal has been removed, the edges of the repair must be undercut so that the new patch can be keyed into place after the old surface has been moistened slightly with cement slurry.

As a rule repairs with feather edges will be short-

TABLE 2B—INDIRECT-ARC ELECTRIC FURNACE OPERATING PRACTICE

Response No.	Slag Build-up Precautions, Remedies	Nature of Refractory Insulation	Patching Material Used	Method of Patch	No. of Heats Before Replacement	Method of Seasoning New Lining	Use of Fluxes
1	None, drain after each heat	Mullite type. 2½" end walls 1½" on barrel	High Al_2O_3 Material	Hard Ram	2300	Slow heat up with arc	None
2	Slag build-up in corners of barrel	Missouri Clay Fire Brick	Mullite Type Sillimanite Type	Weekly around door	880	Wood fire 10 hr 300 KWH 30 hr	None
3	Slag out at end of day	First quality fire-clay brick SiC door assembly. 4" Insulation.	SiC	Cold patch Key in	1000	Wood fire 24 hr Preheat 9 hr with arc	20 lb borax per heat
4	Very little	SiC 2½" Insulation	Mullite type	Dry mix rammed hard, thin layers	1000	Air dry 72 hr 2 short 15 KW runs, 7 hr apart	None
5	Melt out Bake out	Mullite type brick 4½" Insulation	Mullite type	Hand ram	500	2 weeks air dry	Charcoal
6	Very little	Sillimanite type 1" Insulation	Sillimanite type	Weekly	1100	Charcoal fire, 24 hr	None
7	Little Scrape out after each heat	Sillimanite type shapes 2½" Insulation	Plastic sillimanite type	Air ram Dry slowly Wash with Cement slurry	1400	Coke fire, 24 hr Preheat slowly 6 hr	None
8	No build-up	Sillimanite type. 1" Insulation	Plastic sillimanite type	Ram in	1000	Torch dry 4 hr Low arc	Copper oxide
9	No build-up	Fire brick 4" Insulation	SiC	Ramming	800	One day	Commercial flux
10	Very little	Sillimanite type lining. 2½" Insulation	Sillimanite type ram cement	Week ends	2500	Coke fire 72 hr	None
11	Fair Drain furnace when needed	Mullite or sillimanite type with standard recommended insulation. Nickel alloys, use gannister	Mullite or sillimanite type of wet or dry ram cement	Daily, Weekend, alternate shell	1000 to 5000	Air dry. Slow gas torch for 96 hr Prescribed arc	Nickel alloys use bottle glass
12	Little Drain at end of day.	Sillimanite type with standard insulation	Sillimanite type ram cement	Air ram	3000 to 6000	Charcoal fire for 12 hr Slow drying with arc	None

lived since the metal has a chance to lift the patch off the original lining. Here again, not enough can be said of the precautions of preheating the repaired lining. The patching cement must go through the same process of drying and vitrifying as the original lining. Otherwise the patch will fail. The alternate shell method of repair has the advantages of allowing a competent workman to perform a good repair job, plus allowing enough time to dry out and preheat the lining thoroughly. Such a repair consists of chipping out completely the slagged and burned portion of the refractory face. By means of a dry ramming cement and air rammer, the lining is built up to its original thickness.

This type of repair will consume 1200 to 1500 lb of cement, and will produce from 800 to 1000 trouble-free heats in the case of the AA conical shell furnace. Occasionally, a lining failure will take place during the melt cycle. In order to save the heat, or to prevent the steel shell from melting out; a "hot-patch" technique may constitute an emergency measure. Ramming cement is mixed with water in a suitable container till a slurry is formed. The furnace, with the door removed, is rolled so that the trouble spot is clear of metal. The slurry is then dumped into the hole in the lining. The quick patch is allowed to set for 15 minutes before melting is resumed. This kind of repair is not long-lasting.

TABLE 2C—INDIRECT-ARC ELECTRIC FURNACE OPERATING PRACTICE

Response	Wash or Glaze New Lining	Point of Lining Breakdown	Cost to Install New Lining	Cost to Patch, $/ton
1	None	Roof Shoulders	Not given 40 hr to chip	Not given Not given
2	Brush with air-setting cement	Roof above door	out old lining. 40 hr to install new lining.	
3	100-120 lb borax at high heat & full rock. Wash heat of scrap	Roof above door	0.52	0.39
4	Mullite type slurry brush on	Electrode sleeves Roof above door	1.43	1.10
5	None	Roof Door	12 hr labor	2 hr weekly
6	Patch cement slurry brush on	Over door	—	Total 6.50
7	None	Around door	0.67	0.50
8	Water glass solution	Roof	1.00	0.50
9	Copper wash heat	Door Electrode ports	0.31	0.30
10	Cement slurry wash	Around door	—	LFA 0.41 AA-C 0.19
11	Alundum cement in sleeves	Roof above door	LFA, 1.75	LFA, 1.80
		Point of impingement of charge	LFC, 0.97	LFC, 3.10
			LFY, 0.86	LFY, 1.05
		Electrode portholes	AA-C, 0.34	AA-C, 0.93
			AA-S, 0.25	AA-S, 0.88
12	None	End walls	Labor 24 hr	3 hr weekly

The effect of the human element on the success of lining repair work cannot be overemphasized. Good refractory service can be realized if the lining repair is made the responsibility of a small group of experts. The moment the furnace repair gang is augmented with disinterested labor, such as would result if just any workman is detailed to repair furnaces, the desire for overtime pay becomes the man's objective. Good linings have been ruined by overrepair.

In order to present a cross section of current operating practices, the author sent questionaires to 24 users of indirect-arc furnaces. Twelve responses acknowledged and returned the canvassed information. The information gained is listed in Table 2.

Unfortunately, no response reported separate costs for each type of furnace. All collected figures represent averages of a total operation.

Questionnaire on Refractory Life

The questionnaire is reproduced to help clarify the foregoing tables:

1) Indicate the size of your foundry by stating, in pounds of daily melt, the normal average melt capacity.
2) State the number, rating, and capacity of the indirect-arc furnaces.
3) Hours of daily operation of these furnaces.
4) The number of heats, and the daily tonnage produced.
5) State the alloys produced each day.
6) Indicate a pouring temperature, or range, suitable for your operation.
7) State the nature of castings made. Also indicate the approximate weight as light, medium, or heavy.
8) What is the nature of the charge?
9) What difficulty do you experience with slag build-up? What precautions and remedies are established to overcome this problem?
10) Indicate the nature of the refractory and insulation used in the furnaces. If possible state manufacturer's trade name or item number.
11) What kind of patching material do you use?
12) How is the patching cement applied? Please indicate how often the furnace needs repair.
13) What is the ultimate lining life before a new lining must be installed?
14) Please indicate method of curing the new lining before it is placed in production.
15) Are any fluxes used?
16) Do you wash or glaze a new lining before charging the first heat of metal?
17) At which point does the lining show the first breakdown?
18) If possible state the cost per ton of metal (labor and material) of installing a new lining for each furnace.
19) What is the cost per ton of repair patching (labor and material) per melting unit?

In analyzing the responses it becomes evident that none of the non-ferrous foundries achieves, or tries to achieve, an indefinite lining life. Indications are that with the improved fit of the linings and their ease of installation, the foundryman is able to apply the law of diminishing returns to his patching schedule and discard the lining after a predetermined number of heats have been melted. Consider the grouping of type AA-C furnaces shown in Table 3.

It would seem from the above table that a medium service condition is most economical. If the daily

TABLE 3—INDIRECT-ARC FURNACE TYPE AA-C LINING COSTS

Response No.	No. heats/day	Daily tonnage	No. Heats Before Replacement	Cost of Installing New Lining, $/ton	Cost of Patching, $/ton
3	3	3.3	1000	0.52	0.39
					Total
10	6	3.0	2500	—	0.19
11	12	7.0	5000	0.34	0.93

TABLE 4—INDIRECT-ARC FURNACE TYPE LFY LINING COST

Response No.	No. Heats/day	Daily Tonnage	No. Heats Before Replacement	Cost of Installing New Lining, $/ton	Cost of Patching, $/ton
4	4	1.25	1000	1.43	1.10
7	12	3.0	1400	0.67	0.50
9	7	1.0	800	0.31	0.30
11	18	5.5	3000	0.86	1.05

INDIRECT-ARC FURNACE TYPES

Type	KW Rating	Rated Capacity	
		Cold Scrap, lb	Molten Metal, lb
GMS	30	60	100
LF	100	200	325
LFC	125	350	500
LFN	150	500	750
LFY	175	700	1250

tonnage is too low, response No. 3, the successive heating and reheating of the refractory proves ill advised. This foundry discards a lining after only 1000 heats which, when examined in the light of response No. 10, may not be the ultimate, or least expensive. On the other hand, response No. 11, tries to achieve indefinite lining life under severe operating conditions with a corresponding high patching cost.

Similar conclusions are borne out when a breakdown of type LYF furnace users is examined, Table 4.

It would seem that the foundry industry has learned to overcome the secondary influences of furnace lining breakdowns. Surely such items as lost furnace capacity, and interrupted production schedules are not reflected in the refractory cost, although such cost must necessarily be included in the final cost of the product. The firm that has learned the technique of keeping its furnace linings in good repair enjoys not only the lowest refractory cost, but also a better maintained production schedule.

Of all the different types of linings used in the furnaces, the preformed and prefired shapes of the mullite or sillimanite type predominate. A few users, especially those having extraordinary refractory problems in the sense of furnace design or service conditions, believe that super-duty fireclay brick gives excellent service. One user reports a SiC refractory in melting nickel silvers and red brasses. All but two concerns, however, prefer to use a mullite type of ramming cement for repair patch work.

It is interesting to compare the greatly diversified refractory costs of several concerns with respect to the different metallurgical practices. Response No. 6 (Table 2C), using mostly ingot and dry turnings with no flux in its melting practices, is operating with a high refractory cost when compared with response No. 10 melting ingots, gates, and dry turnings with no flux. The total cost comparison is $6.50/T as against $0.19/T. In this same analysis, the use of commercial fluxes does not influence the low refractory costs of responses Nos. 7, 8, 9, and 10.

Even though the smaller capacity furnaces, types LFA and LFC, can be maintained much easier than the large capacity melting units, types AA and CC, the number of heats per lining and total lining cost per ton of metal produced show up very favorably for the larger units (responses Nos. 6, 10, and 11 as compared to responses Nos. 3, 10, and 11).

In reviewing the tremendous diversity of refractory cost per ton of metal, and examining the many factors which make up these costs, a necessary conclusion must be drawn that the human element responsible for the lining upkeep is of utmost importance. The indirect labor that has the knack of patching a furnace efficiently and with assurance that the melting unit will perform well is invaluable to a foundry operation. Even though the simple rules for patching a refractory are well known and can be supplied by the manufacturer, the lining results, and cost per ton is a convenient factor for judging such results, are out of all proportions to the severity of the service.

Acknowledgment

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CENTRIFUGAL CASTING OF ALUMINUM ALLOYS

By

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PART IV—Gating Design†

The research investigation on centrifugal casting of light alloys was undertaken to study the possibilities of the commercial uses of this casting method in light alloy foundries. The investigation was started in 1947 under the sponsorship of the Centrifugal Castings Committee†† of the American Foundrymen's Society. An earlier project undertaken by this Committee was a cooperative attempt to compare sand castings produced statically and centrifugally in various U. S. and Canadian commercial foundries. The alloys used were ASTM aluminum alloy SG70A (Al-7%Si-0.3% Mg) and ASTM magnesium alloy AZ92A (Mg-9%Al-2% Zn). The results obtained on 27 castings, produced in the shape of a 13-in. diam spoked wheel, were considered so encouraging that it was decided to continue the investigation in a most systematic way. It was agreed to investigate primarily the centrifuging method, that is, the casting of irregular, unsymmetrical products in rotating sand molds, as more significant for commercial applications than the "true" centrifugal casting of cylindrically-shaped products.

A detailed program for the preliminary stage of the project, including specific recommendations on sand mixture, gating, pouring techniques, casting temperatures, spinning speeds, etc., was established by the Steering Committee. A second stage of the project was proposed for magnesium alloys, to be carried out if the first stage were successful. The Canadian Department of Mines and Technical Surveys agreed to carry out the investigation as a part of its broader foundry research program. The American Foundrymen's Society provided metal (ASTM alloy C4A: Al+4.5% Cu) and sand for the investigation, all other expenses of the project being borne by the Canadian Department of Mines and Technical Surveys.

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† For Parts I, II, and III see TRANSACTIONS, vol. 57, pp. 602-631 (1949).

†† Members of this Committee are: W. E. Sicha, *Chairman*, K. B. Bly, Walter Bonsack, M. E. Brooks, R. F. Cramer, Nathan Janco, Wm. E. McCullough, J. W. Meier, H. J. Rowe and Sam Tour.

Summary of Earlier Results

The experimental work actually carried out may be divided into two phases:

1. Investigation along the lines recommended by the Steering Committee.
2. Design and development of a gating system suitable for the casting technique of centrifuging.

First Phase—The first phase of the investigation was reported in April, 1949, and published in AFS TRANSACTIONS, vol. 57, pp. 602-631 (1949). The experimental work performed in that phase consisted of adaptation and preparation of foundry equipment; investigation of 24 different molding sand mixtures; and a study of the effect of mold geometry and gating specified by the Steering Committee, on the quality of centrifuged castings.

Considerable work was carried out to determine a suitable sand mixture to withstand the heavy internal pressure of the metal at high revolving speeds and to avoid penetration. Baked sand mixtures with oil binder failed in all cases, due to a pronounced tendency to burn. Finally, baked molds using a dry sand mixture without oil, and a suitable mold wash were developed and proved satisfactory throughout the investigation. As recommended, straight runners were radially arranged and delivered the metal from the sprue towards mold cavities located on the periphery of the rotating mold.

The recommended method of gating did not permit of a thorough study of the effect of all the factors connected with the centrifuging technique on the properties of the castings. However, it did show that the effect of turbulence of the incoming metal dominated all other factors, resulting in generous unsoundness of the test panels. The pattern of shrinkage and porosity as shown by x-ray examination revealed the outline of the flow of metal and its turbulent entrance into the mold cavities.

The pattern of disturbance shown did not change with variation of sprue or different geometrical arrangement of the runners in relation to mold cavities, provided that the runners remained straight and the test panels were horizontal.

Positioning the test panels vertically resulted in a considerable improvement of their structure. Although shrinkage, porosity and some oxide inclusions were still encountered, no characteristic pattern of unsoundness showing the turbulent influx of the metal was visible.

The results of the first phase of the investigation may be summarized as follows:

1. A dry sand mixture with consistent and fairly satisfactory characteristics was developed for use at rotating speeds up to 800 rpm.
2. A mold wash was established which proved satisfactory from the point of view of permeability and resistance against metal penetration.
3. The investigation underlined the necessity of fundamental research on the fluid flow and solidification mechanism in rapidly rotating molds.
4. The results obtained showed that special gating design is essential for centrifuged castings.
5. At the conclusion of this first phase of the investigation it was realized that the relationship between the characteristics of the alloy and the peculiarities of the centrifuging technique is very complex.

Consideration of Further Work—Theoretical considerations show that the following characteristics of light alloys adversely affect their adaptability to centrifuging casting:

1. The low density of the metal.
2. The chemical reactivity of the metal.
3. The almost equal densities of the oxides and the metal.

Production of sound centrifuged castings requires that a sufficiently high internal hydrostatic pressure be developed and maintained inside the liquid metal against the moving front of solidification. The necessary magnitude of that pressure for an alloy of lower density is maintained by a higher revolving speed or, at the same speed, by increased radius of rotation. High rotational speeds cause high linear velocities of the liquid stream, resulting in turbulent flow of the metal in the mold passages and on entering the mold cavities. Turbulent flow of metals of high chemical

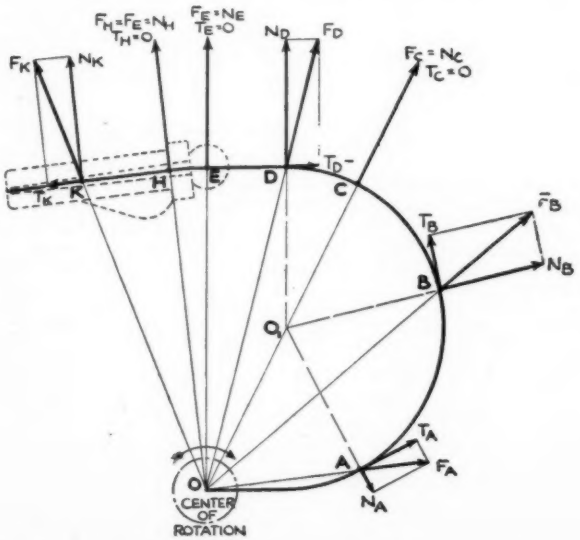


Fig. 1—Geometry of the runner and gating system.

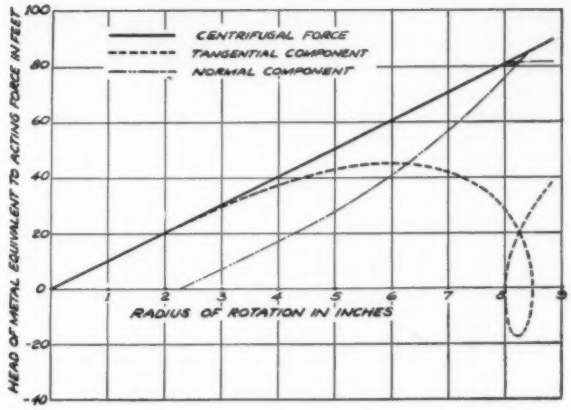


Fig. 2—Magnitude and direction of component forces along the runner.

activity results in progressive oxidation, due to continuous breaking of the protective oxide film and exposure of fresh particles of metal to air in the mold.

The difficulty of separation by centrifugal force of metal and metal oxides of almost equal density in the very short time available results in the dispersion of oxide inclusions throughout the casting. It is logical, therefore, to assume that the only way to avoid oxide inclusions in centrifuged light alloy castings is to create mold filling conditions which will prevent any oxidation of metal. Undisturbed and steady metal flow into mold cavities, free from air aspiration, and adequate feeding during solidification are, therefore, the major factors affecting the quality of the casting.

The proposal of a more systematic investigation of various factors affecting the quality of centrifuged castings on small-scale laboratory equipment was not accepted, at this time, by the Steering Committee, which requested a continuation of experimental work on a "trial and error" basis on commercial size equipment.

In carrying out this request of the Steering Committee, the fundamental research on the fluid flow and the solidification mechanism in rapidly rotating molds had to be postponed, although some preliminary work on this phase of the project had already been started. Therefore, only the study of a proper gating design was undertaken.

Gating and Feeding Design

The development of gating was based on the assumption that, more than any other consideration, the character of metal flow through the mold passages and in entering the mold cavity influences the quality of casting.

The character of this metal flow was considered as being dependent mainly on two factors:

1. The velocity of the metal flow,
2. The geometrical shape and relative arrangement of the mold passages.

In centrifuging casting metal is poured into a central sprue, from whence it flows through runners, with gates delivering the metal into mold cavities located on the periphery of the mold. The centrifugal force, and therefore the velocity of metal flow, at any point in the mold is dependent on the revolving speed and

the radius of rotation at this point. Thus, for any established revolving speed and radius of rotation the velocity of metal flow is fixed and, for all practical purposes, greatly exceeds the limiting velocity for non-turbulent flow (approximately 7 fps for aluminum).*

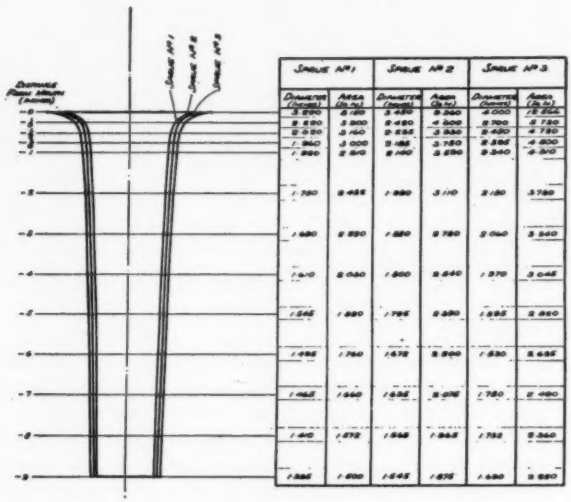


Fig. 3—Cross-sectional dimensions of the long sprues.

This applies, however, only in the case of radial runners, where the centrifugal force acts directly along the path of metal flow. By designing runners of non-radial form, the linear velocity of metal flow along the same channel can be controlled, dependent on the resolved component of the centrifugal force in the path of metal flow at that particular point in the channel.

The centrifugal force F may be resolved into its components: tangential force T, which acts along the path of the metal flow and directly affects the velocity of flow, and normal force N, which acts as a frictional force reducing the velocity of metal flow. It is apparent from Fig. 1 that the tangential component T, which positively affects the velocity of the metal, is not only reduced in magnitude, in comparison to the centrifugal force F, but, also, at some range (see point D) can be made to change its sign and act against the direction of flow to provide some control of inertia effects. Figure 2 shows the magnitude of the centrifugal force and its components, along the entire length of the delivery channels.

The magnitude of the forces acting along the passages was the main factor in establishing their cross-sectional dimensions along the path of the metal flow. The design of passages was based on the requirement of smooth and undisturbed flow of metal, without air aspiration from outside. Two conditions were considered as most important from the point of view of those requirements:

1. Streamlined design,
2. Constant rate of discharge of metal at every point along the path of the metal flow.

All parts of the sprue and runner system along the path of flowing metal were designed with close attention to these two points.

Sprue—The geometrical shape of the sprue was established on the basis of a mathematical equation deduced from the requirement of a constant rate of discharge of metal at any cross-section of the sprue, namely:

$$X^4 = \frac{D^2}{2\pi^2 gy} = \frac{D^2}{12 \times 634.96y}, \quad \ldots\ldots\ldots\ldots (1)$$

in which

D = rate of discharge in cu in./sec,
$2x$ = sprue diameter in inches,
y = distance from the top of the sprue in inches, and
g = 32.2 fps^2.

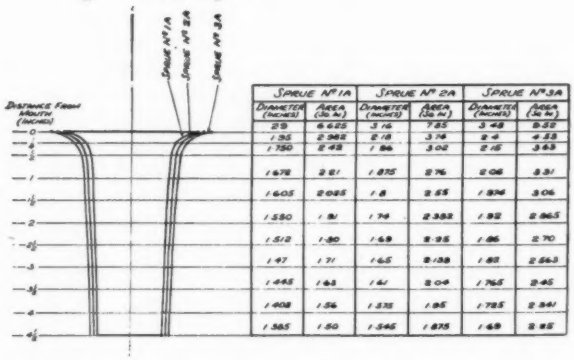


Fig. 4—Cross-sectional dimensions of the short sprue.

* At 600 rpm and 8-in. radius, the theoretical velocity of flow reached is 72.5 fps.

TABLE 1—VOLUMETRIC DISCHARGE AND FILLING TIME FOR THE SPRUES WITH SPRUE BASE DESIGNED FOR 600 RPM.

Sprue No.	Length, in.	Diameter of Outlet, in.	Area of Outlet, sq in.	Total Runner Area at Sprue Base at 1 in. Radius	Ratio	Mean Head of Metal (Aluminum),* in.	Volumetric Discharge, cu in./sec	Total Volume of Mold Cavities, cu in.	Filling Time,** sec
(1)	(2)	(3)	(4)	(5)	(4) : (5)	(7)	(8)	(9)	(10)
3	9	1.69	2.25	1.14	1.97	11	207	50	0.48
2	9	1.55	1.88	1.14	1.65	11	173	50	0.48
1	9	1.39	1.5	1.14	1.32	11	138	50	0.48
3A	4.5	1.69	2.25	1.14	1.97	6.5	160	50	0.62
2A	4.5	1.55	1.88	1.14	1.65	6.5	133	50	0.62
1A	4.5	1.39	1.5	1.14	1.32	6.5	106	50	0.62

* Allowing for metal in pouring basin.

** Calculated: $(10) = \frac{(9)}{(8)} \times (6) = \frac{\text{Total volume of mold cavities}}{\text{Volumetric discharge}} \times \frac{\text{Area of outlet}}{\text{Total runner area at sprue base}}$

Six sprues with cross-sectional dimensions calculated from equation (1) were established for different rates of discharge. Figures 3 and 4 show the dimensions and the shape of the sprues, and their main characteristics are listed in Table 1.

The sprues were cast in aluminum-nickel bronze and polished inside. They are interchangeable, being mounted and cemented on the bottom of a pouring basin.

Pouring Basin—Two pouring basins were designed for two different lengths of sprues. Figure 5 shows the details of the pouring basins.

The pouring basin was designed with a large cross-sectional area close to the bottom, to give a relatively small difference in the head of the metal at the beginning and at the end of pouring and thus reduce to a minimum the difference in the output of the metal.

Figure 5 shows that only the upper part of the sprue was cemented inside the pouring basin (6⅞ in. of the total length of 9-in. for the long sprue, and 2⅜ in. for the short sprue). The lower part of the sprue was molded in the cope. This was necessary to allow the mold to rotate while the pouring basin remained stationary, and for other reasons described later.

Stopper—The stopper which prevented metal in the pouring basin from entering the mold until required, was manufactured from graphite. The shape of the lower part was machined for perfect fit with the upper part of the sprue. The upper part of the stopper was streamlined in order to prevent excessive turbulence of the metal by upward movement of the stopper. Figure 6 shows the shape of the stopper.

Sprue Base—A properly designed sprue base should provide means for a smooth transition from vertical to horizontal flow and, in addition, should maintain a constant rate of discharge even though the metal in the sprue base is being accelerated rapidly by centrifugal force.

The speed of the metal in the vertical direction developed by gravity at the end of the long sprue is approximately 7.7 fps. The velocity of metal flow in the horizontal direction developed by the centrifugal force at a radius of rotation of 1 in. at 600 rpm is approximately 26 fps.

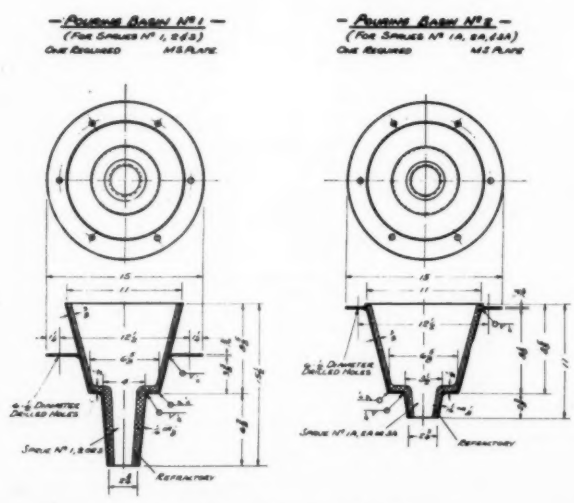


Fig. 5—Pouring basin details.

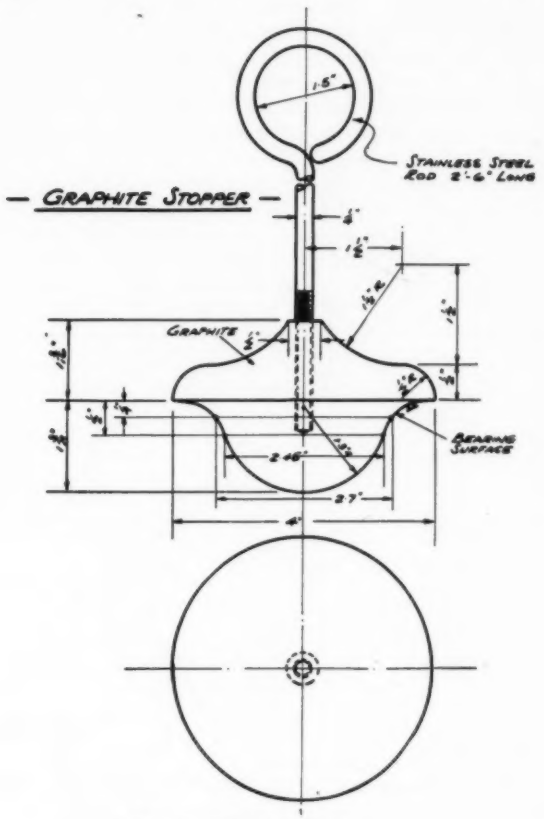


Fig. 6—Graphite stopper.

The second requirement is expressed by the equation:

$$V_1 \times F_1 = V_2 \times F_2;\ F_2 = \frac{V_1 F_1}{V_2} \quad \ldots\ldots\ldots (2)$$

where:

V_1 — speed of the metal at the end of the sprue (vertical flow),

V_2 — speed of the metal at the sprue base (R = 1 in.) (horizontal flow),

F_1 — discharge area at the end of the sprue, and

F_2 — discharge area at the sprue base.

Sprue base designs to meet these requirements of smooth transition and equal rates of discharge are shown in Figs. 7 and 8.

The Runners—The geometrical shape of the runners as shown in Fig. 1 was designed to reduce and control the velocity of the metal stream in the runners, and particularly in the region of the gates.

Tables 2 and 3 show the design data obtained from theoretical calculation of the cross-sectional dimensions at different points along the runner (equation 2), and the dimensions actually used in these experiments. The calculations are based on the chosen initial revolving speeds during the filling of the mold, 600 and 100 rpm.

After the mold was filled (0.5 sec, approximately) the initial revolving speed was accelerated as rapidly

as the machine would allow, to speeds in the region of 600-900 rpm to obtain the necessary hydrostatic pressure during solidification of the metal.

Figure 9 shows the resolved forces at various points along the path of metal flow at 600 rpm. Figures 10 and 11 show the plan view of the runners, based on the above calculations listed in Tables 2 and 3.

The Cylindrical Gate Wells—In order to check the speed of the metal immediately before it enters the mold cavities, a deep cylindrical well was provided at the end of each runner at the point where the tangential component reaches, for the second time, zero value (point *E* in Fig. 1). These wells were provided also as cavities for a loosely packed steel wool strainer to remove dross, etc. Figures 12 and 14 show the shape and the dimensions of the well. Subsequent ex-

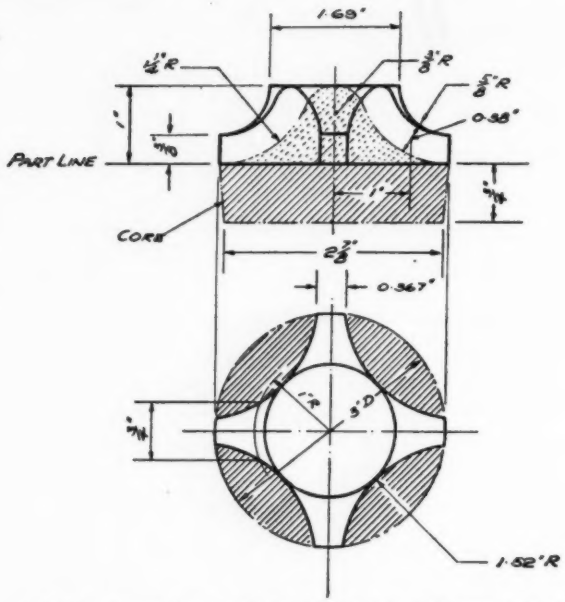


Fig. 7—Sprue base design details for 600 rpm initial speed.

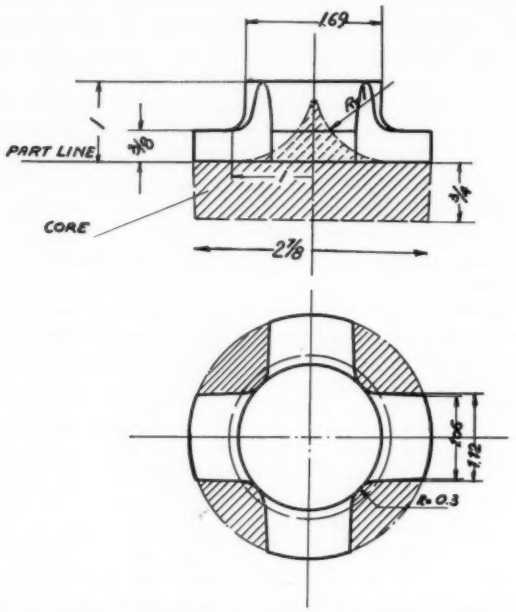


Fig. 8—Sprue base design details for 100 rpm initial speed.

TABLE 2—CROSS-SECTIONAL DIMENSIONS OF THE RUNNER FOR 600 RPM AND SPRUE DISCHARGE OF 207 CU IN./SEC.

Radius of Rotation, in.	Centrifugal Force, ft-lb*	Head of Metal (Aluminum) Equivalent to Centrifugal Force, ft**	Head of Metal (Aluminum) Equivalent to Tangential Force, ft	Theoretical Speed of Metal Stream due to Tangential Force, fps	Total Area of Discharge, sq in.	Cross-Sectional Dimensions for one Runner, in.	
						Calculated	Applied
Sprue Outlet		0.915		7.68	2.25	Diameter 1.69	Diameter 1.69
1.0	328m	10.2	10.2	25.6	0.675	3/8 x 0.45	0.38 x 0.75
1.5	492m	15.2	15.2	31.4	0.55	3/8 x 0.367	3/8 x 0.367
2.0	656m	20.2	20.2	36.0	0.48	3/8 x 0.318	3/8 x 0.318
3.0	985m	30.6	29.5	43.5	0.397	3/8 x 0.262	3/8 x 0.262
3.95	1300m	40.4	37.0	48.8	0.354	3/8 x 0.233	3/8 x 0.233
4.85	1595m	49.5	43.0	52.6	0.328	3/8 x 0.219	3/8 x 0.219
5.69	1870m	57.0	45.0	53.9	0.32	3/8 x 0.213	3/8 x 0.213
6.43	2118m	65.8	44.0	53.5	0.322	3/8 x 0.215	3/8 x 0.213
7.08	2330m	72.4	41.2	52.0	0.332	3/8 x 0.221	3/8 x 0.213
7.60	2500m	77.5	35.5	47.8	0.361	3/8 x 0.25	3/8 x 0.213
8.0	2630m	81.6	27.5	42.0	0.41	3/8 x 0.273	3/8 x 0.219
8.30	2730m	84.7	17.5	33.5	0.516	3/8 x 0.344	3/8 x 0.245
8.45	2780m	86.5	6.5	20.4	0.846	3/8 x 0.564	3/8 x 0.272
8.47	2782m	86.5	—5.5				3/8 x 0.385
8.35	2740m	85.0	—16.5				3/8 x 0.524
8.15	2680m	83.2	—15.0				3/8 x 0.58
8.02	2632m	81.6	—4.5				3/8 x 0.645
8.0	2630m	81.6	0				3/8 x 0.645

* Centrifugal Force $F = mRw^2$ (for 600 rpm $F = 400\pi^2 mR$) (R in feet)

** Head of metal in feet equivalent to Centrifugal Force

$$H = \frac{mRw^2}{mg} \quad \text{(for 600 rpm } H = 12.4\pi^2 R\text{)}$$

where m is mass of metal, w is angular velocity, and R is radius of rotation.

periments showed that these cylindrical gate wells (and also the gates) were not effective, and their elimination caused improvement in the castings. This was thought to be due to the change in direction of metal flow which, although designed to check the speed of the metal stream, also introduced turbulent effects.

The Gates—Cross-sectional dimensions of the gates were calculated from the requirement of a constant discharge rate at all points in the system. The tangential component of the centrifugal force has been considered as the driving force, pushing the metal towards the mold cavity. Table 4 shows calculated and practically applied cross-sectional dimensions for the gates, for the chosen revolving speed of 600 rpm. Figure 12 shows the design details of the gates.

Mold Cavities (Test Panels)—It was decided to use panels 4 in. × 3¼ in. × ¾ in., the rectangular shape having been decided upon by the Steering Committee.

Risers—The design of the riser is shown in Fig. 13. The riser is located so that the centrifugal force favorably influences the feeding. On the other hand, since the risers are located closer to the center of rotation than the mold cavities, the incoming metal could enter the riser only under the pressure of metal still in the runner acting against the centrifugal force in the mold cavity. The first castings made with risers, as shown in Fig. 13, showed that the pressure of the

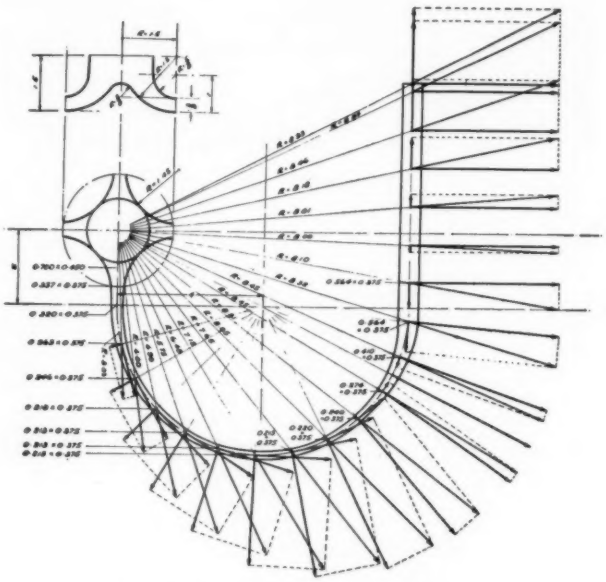


Fig. 9—Runner design for 600 rpm.

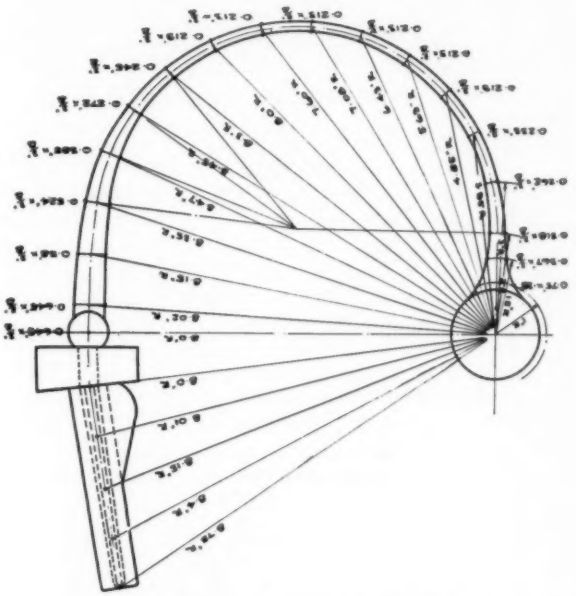


Fig. 10—No. 1 runner for 600 rpm.

TABLE 3—CROSS-SECTIONAL DIMENSIONS OF THE RUNNER FOR 100 RPM AND SPRUE DISCHARGE OF 160 CU. IN./SEC.

Radius of Rotation, in.	Centrifugal Force, ft-lb*	Head of Metal (Aluminum) Equivalent to Centrifugal Force, ft	Head of Metal (Aluminum) Equivalent to Tangential Force, ft	Theoretical Speed of Metal Stream due to Tangential Force, fps	Total Area of Discharge, sq in.	Cross-Sectional Dimensions for one Runner, in.	
						Calculated	Applied
Sprue Outlet	—	0.54	—	5.9	2.25	Diameter 1.69	Diameter 1.69
1.0	9.15m	0.824	0.824	7.26	1.84	⅜ x 1.22	⅜ x 1.25
1.5	13.7m	0.965	0.965	7.89	1.67	⅜ x 1.06	⅜ x 1.06
2.0	18.3m	1.11	1.11	8.39	1.6	⅜ x 1.04	⅜ x 1.04
3.0	27.4m	1.39	1.34	9.3	1.43	⅜ x 0.95	⅜ x 0.95
3.95	36.1m	1.66	1.52	9.9	1.35	⅜ x 0.89	⅜ x 0.86
4.85	44.4m	1.92	1.62	10.4	1.28	⅜ x 0.86	
5.69	52.0m	2.16	1.7	10.5	1.27	⅜ x 0.845	⅜ x 0.85
6.43	58.7m	2.36	1.6	10.3	1.29	⅜ x 0.86	⅜ x 0.85
7.08	64.6m	2.54	1.4	9.5	1.4	⅜ x 0.93	⅜ x 0.85
7.60	69.5m	2.60	1.2	8.8	1.52	⅜ x 1.01	⅜ x 0.85
8.0	73.1m	2.81	0.95	7.84	1.7	⅜ x 1.13	⅜ x 0.85
8.30	75.8m	2.89	0.6	6.2	2.15	⅜ x 1.43	⅜ x 0.85
8.45	77.2m	2.93	0.22	3.77	3.54	⅜ x 2.36	⅜ x 0.85
8.47	77.5m	2.94	—0.19				⅜ x 0.85
8.35	76.4m	2.92	—0.57				⅜ x 0.85
8.15	74.5m	2.86	—0.52				⅜ x 0.85
8.02	73.2m	2.82	—0.16				⅜ x 0.85
8.0	73.1m	2.81	0				⅜ x 0.85

* Total of Centrifugal Force and Static Force, since the latter is significant at low rotational speeds.

TABLE 4—CROSS-SECTIONAL DIMENSIONS OF THE RUNNER FOR 600 RPM AND SPRUE DISCHARGE OF 207 CU IN./SEC.

Radius of Rotation, in.	Centrifugal Force, ft-lb	Head of Metal (Aluminum) Equivalent to Centrifugal Force, ft	Head of Metal (Aluminum) Equivalent to Tangential Force, ft	Theoretical Speed of Metal Stream due to Tangential Force, fps	Total Area of Discharge, sq in.	Cross-Sectional Dimensions for one Runner, in.	
						Calculated	Applied*
8.0	2630m	81.6	0	—	2.25	1.25 x 0.45	1.25 x 0.4 x 0.3
8.0	2630m	81.6	0	—	2.25	1.25 x 0.45	1.10 x 0.35 x 0.15
8.01	2632m	81.6	5.5	18.8	0.916	0.95 x 0.24	0.95 x 0.34 x 0.14
8.15	2680m	83.2	15.5	31.6	0.546	0.62 x 0.22	0.62 x 0.32 x 0.12
8.40	2760m	85.9	26.0	40.8	0.422	0.3 x 0.2	0.03 x 0.3 x 0.12
8.75	2876m	89.3	35.5	47.7	0.361		

* Height × Width at top × Width at bottom.

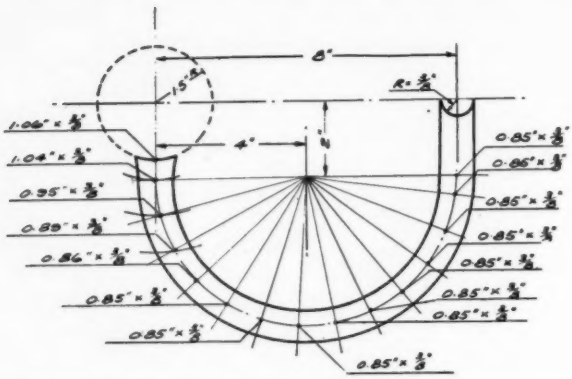


Fig. 11—Cross-sectional dimensions of No. 2 runner for 100 rpm.

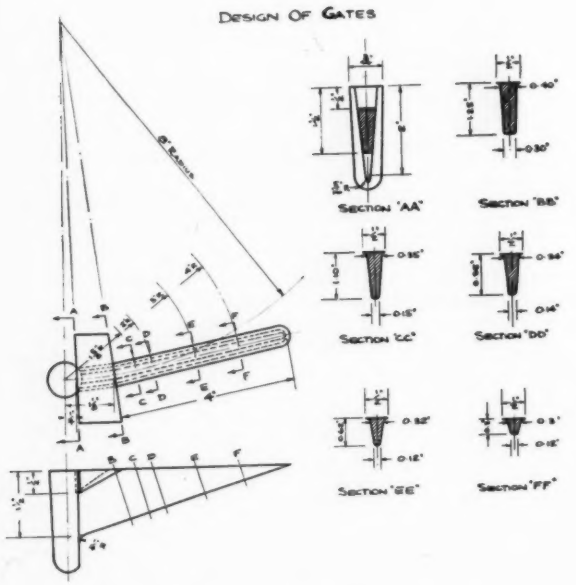


Fig. 12—Design of cylindrical gate well and gate.

metal in the runner was not sufficient to fill the riser. The risers, consequently, were filled only partially, and feeding was inadequate with resulting large shrinkages and cavities in the test panels in the region adjacent to the riser. This was remedied by the provision of a secondary riser which caught metal from the trapping well, subsequently described, and supplied it to the riser in the test panel.

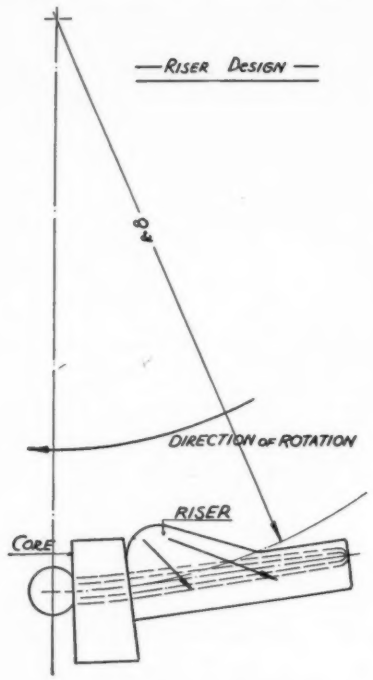


Fig. 13—Riser Design.

Secondary Riser—Figure 14 shows a view of the secondary riser, and other parts of the runner and gating system. The relative arrangement of each is clearly shown in Fig. 16. This secondary riser, although primarily intended to supply metal to the riser on the test panel, must be considered as an integral part of the riser because of its large size and its relatively large cooling factor. It should be appreciated that this secondary riser is filled only after the mold cavity is filled, in order that the metal may be introduced into this member under the full centrifugal force without introducing turbulence in the test panel.

The dimensions of the secondary riser are as follows:

larger end — 1⅜ in. × 1⅜ in., with fillets;
smaller end — 1 1/16 in. x 1⅜ in., with fillets;
length — changeable, depending on the radius of the trapping well.

Trapping Well—It was found through experimentation that with the bottom pouring technique, as employed in the investigation, a vortex is always formed

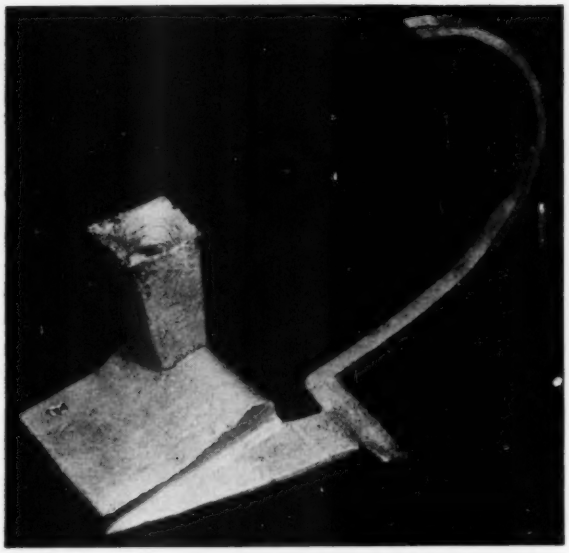


Fig. 14—Runner, cylindrical gate well, gate, panel, riser, and secondary riser.

as soon as the level of the metal in the pouring bowl drops to approximately 1.5 in. above the top of the sprue. The presence of a vortex leads to a turbulent flow inside the sprue and results in oxidation of the metal, gas entrapment, etc. It was therefore important to design a device for trapping this last amount of oxidized metal and thus prevent its entrance into the mold cavities.

This was accomplished by melting a larger amount of metal than required to fill the actual volume of the

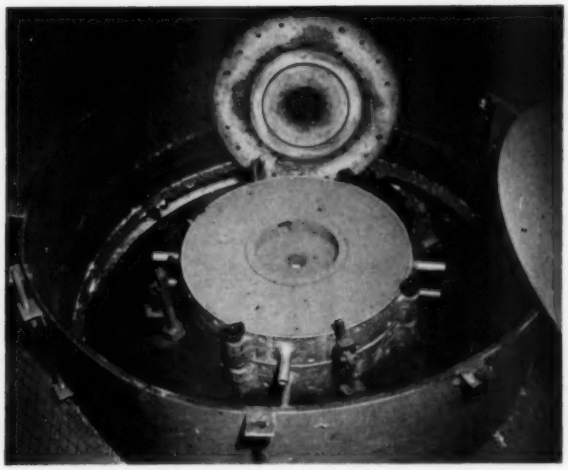


Fig. 15—General view of the machine and mold assembly.

mold cavities, and trapping the excess in a special well molded on the top of the cope. This necessitated dividing the sprue into two parts: the sprue proper, which remains stationary; and the sprue extension, which rotates with the mold. The total height from sprue base to sprue entrance was not changed, however, and the theoretical calculations were not affected.

The metal thus trapped in the well provides a source for feeding the risers with hot metal, through the secondary risers situated on the periphery of the trapping well. Figure 15 shows a view of the trapping well with the sprue extension on the top and the end of one secondary riser visible on the side of the well. A sectional view of the assembly is shown in Fig. 16.

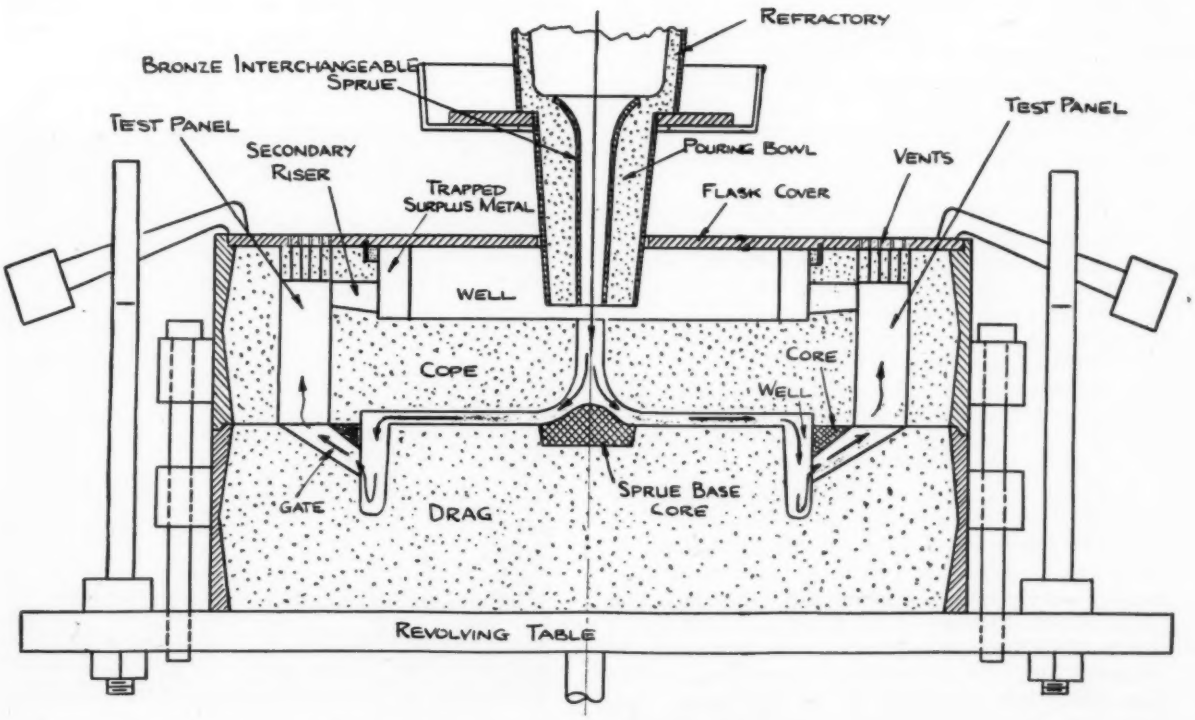


Fig. 16—Sectional view of the machine and mold assembly.

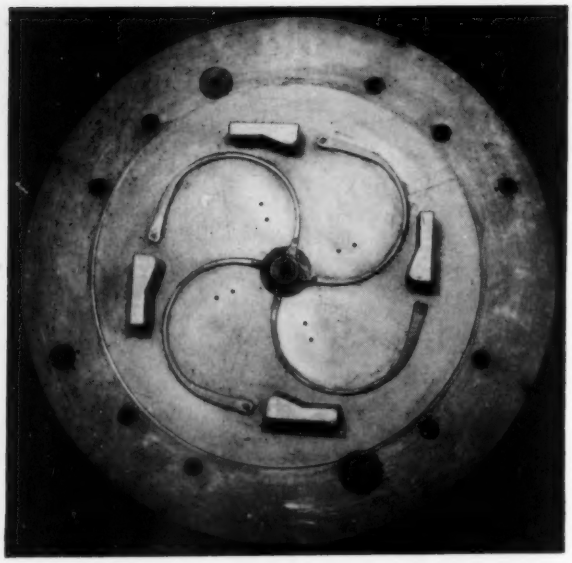


Fig. 17—Match plate with runners for 600 rpm (cope).

Venting—Direct venting of each mold cavity was secured by eight small holes drilled through the top cover, as can be seen in the blank areas between the large holes on the cover in Fig. 15. Wire of $\frac{1}{32}$-in. diam was pushed through these holes to pierce the sand, thus venting the cavities. General venting was secured by drilling large holes on the sides of the flask and on the top cover. Sufficient venting proved to be essential from the point of view of reducing porosity and gas inclusions. Photographs of the match plates used to make up the molds are shown in Figs. 17, 18, and 19.

Equipment—The following equipment was designed and manufactured:

1) self-centering, aluminum alloy, molding flasks (Fig. 15);
2) top cover with guard ring (Fig. 15);
3) set of two pouring basins, accommodating longer and shorter sprues (Fig. 16);
4) set of six interchangeable Al-Ni bronze sprues (Figs. 3 and 4);
5) magnesium alloy match plate with two sets of runners with wells, gates, sprue bases, test panels, etc. for 600 and 100 rpm (Figs. 17-18-19);
6) graphite stopper (Fig. 6);
7) molding facilities;
8) centrifugally operated, self-locking and holding device for flasks (Figs. 15 and 16).

Foundry Practice

The general foundry equipment, sand mixtures and washes used in this investigation were described in the 1949 report. Rigid control of sand moisture, permeability, and drying conditions was maintained throughout the investigation. Metal was poured into warm molds. Melting of the aluminum was carried out in Tercod crucibles in either gas or electrical resistance furnaces. Care was taken not to exceed a temperature of 720 C (1318 F) in melting. Domal fluid flux (50% $MgCl_2$ — 20% KCl — 15% CaO.MgO — 15% CaF_2) was used in melting, and melts were chlorinated before pouring. Pouring temperatures were kept as close as possible to the required range. The temperature of the metal was measured inside the pouring basin with a chromel-alumel thermocouple.

Spinning of the mold was maintained for 3 min after filling. Shaking out the molds was done not sooner than 15 min after pouring. Spinning speed was maintained close to the range of 600-700 rpm for pouring with rapidly rotating molds and in a range of 50-100 rpm for pouring with slowly rotating molds. Immediately after pouring (0.5 sec approximately), the machine was accelerated to a speed of 600-900 rpm, which took 45 to 60 sec. This ensured the necessary hydrostatic pressure during the solidification of all castings.

Examination of Castings

All castings were examined visually for surface appearance and signs of turbulent metal flow inside the mold cavity. X-ray examination was carried out on all castings. Test panels were later machined, as shown in Fig. 20, and etched for macrographic examination.

Mechanical tests were limited only to some of the better castings as experience showed that surface examination and macro-etching gave a good indication of the quality of the casting.

Figure 20 shows the way in which test panels were cut and sampled for tensile test bars and macro-etching.

Factors Affecting Soundness of Castings

Sixty-five castings were made in the course of the investigation. Since experience in centrifuging casting was limited and there is little published informaton, the work was based on the "trial and error" method, and each casting was made after some study of the preceding one. Thus, in almost every casting some changes were introduced to one or other of the factors, which was considered to be affecting the qual-

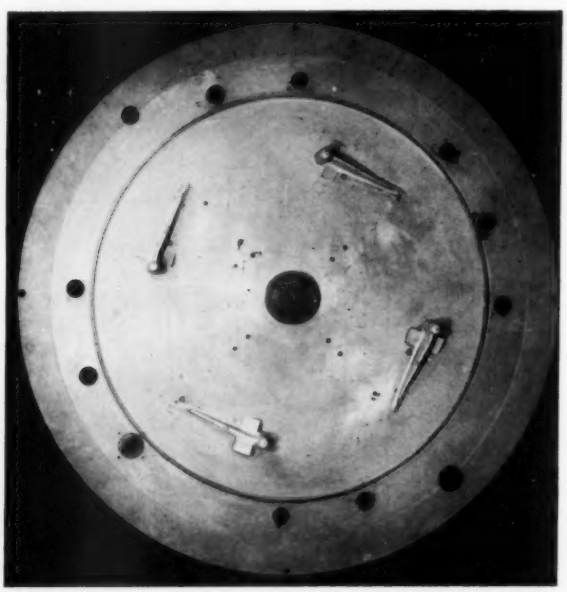


Fig. 18—Match plates with gates and cylindrical wells (drag.)

ity of the casting. The main purpose of the work was to obtain a sound, dense structure of the test panel casting and to investigate the possibilities and peculiarities of centrifuging casting of aluminum alloys into sand molds.

The investigation proved that the initial assumption that the quality of the casting was dependent on the character of the metal flow was justified. It was found that the velocity of the metal flow and the geometrical shape and relative arrangement of the flow passages affected the internal soundness of the castings to such a degree that the effect of other factors was insignificant.

Casting into the mold revolving at 600-700 rpm (the minimum speed calculated to provide sufficient internal pressure) led to general unsoundness of the test panels. Although all attempts undertaken at 600 rpm failed to produce a completely sound casting, it was apparent, nevertheless, from the results obtained, that:

1. The soundness of the test panels improves with decreasing initial revolving speed, i.e. the speed at the moment of pouring.

2. The soundness of the test panels improves with simplified gating. The panels cast with gating consisting only of the runners delivering the metal straight into mold cavities, with cylindrical gate wells and gates omitted, were sounder than those cast with application of the whole gating system.

3. Casting temperature within the range of 30-60 C above the liquidus (54-128 F) shows no noticeable influence on the soundness of the panels cast at 600-700 rpm.

4. Application of secondary risers leads to an increase in general unsoundness, i.e. distributed oxide and gas inclusions and microporosity. On the other hand, it considerably improves feeding and reduces concentrated shrinkage porosity.

5. Insufficient venting of the mold cavities tends to increase the general unsoundness.



Fig. 19—Match plate with runners for 100 rpm (cope).

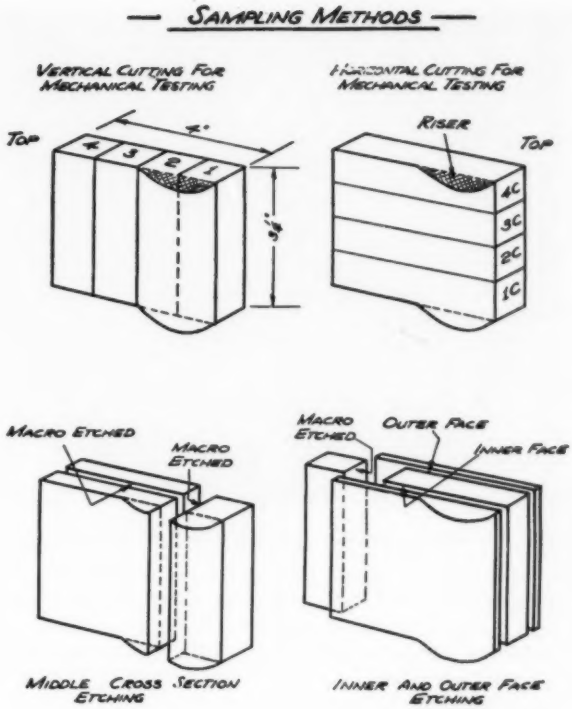


Fig. 20—Sectioning of test panels for examination.

6. Chilling favorably affects the soundness of the adjacent areas, in that it tends to reduce microporosity.

7. The direction of rotation at initial speeds of 600 rpm does not influence the magnitude of general unsoundness, and leads only to some changes in the general shape of its pattern. This would appear to be rather surprising, in view of the lower relative velocity of metal flow in molds rotated in such a way that the mold is moving in the same direction as the metal stream. However, it seems that in both cases the relative velocity of flow is greater than that required for nonturbulent flow.

Casting into slowly rotating molds (50-100 rpm) with the spinning speed accelerated in approximately 45 sec (shortest possible time with available machine) to the required range of 600-700 rpm led to an improvement in general soundness even though the runner system was designed for an initial casting speed of 600 rpm. The investigation was therefore continued with the runner system designed for 100 rpm. The following factors were under consideration from the point of view of their influence on the general soundness of the casting poured at an initial speed of 100 rpm, and lower:

1. Runner and gating design, and initial speed.
2. Casting temperature.
3. Discharge rate.
4. Range of solidification.

Runner and Gating Design, and Initial Speed—Runner and gating designed for 100 rpm proved to be satisfactory from the point of view of improving the general soundness of the test panels. Test panels cast at initial spinning speeds close to 100 rpm were sounder than those cast at 600 rpm. Dropping the

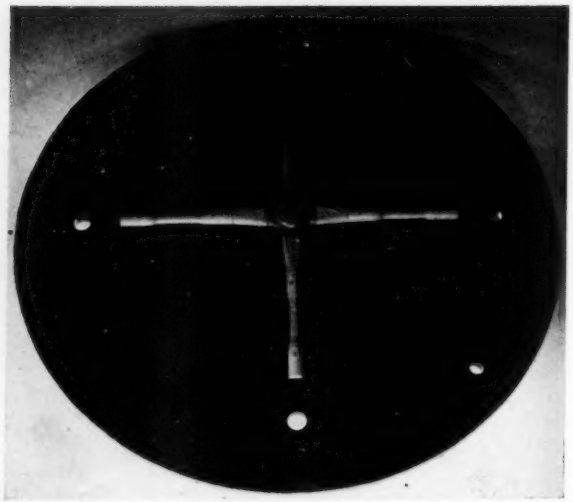


Fig. 21—Match plate with cast-to-shape test bars (cope).

initial spinning speed below 50 rpm appreciably improved the quality of test panels. An increase of the speed beyond 100 rpm led to a marked deterioration of the soundness. Even under the best conditions, runner and gating designed for 100 rpm failed to produce completely sound casting.

The simplified design, with only runners acting (cylindrical gate wells eliminated), further improved the quality of the castings. This failed to produce a sound structure in ASTM alloy C4A, but one sound panel was produced with an alloy of a shorter solidification range (ASTM alloy S5A).

Casting Temperature—Nearly all C4A alloy panels cast at lower temperature (30-40 C, 54-72 F above liquidus) at initial speeds around 100 rpm showed some superiority in general soundness over those cast at higher temperatures (60 C, 128 F above liquidus).

Discharge Rate—Although six sprues were tried, each one at an initial speed of 100 and 200 rpm, at casting temperatures of 680 and 710 C (1256 and 1310 F) the volumetric discharge rate and filling time appeared to have no influence on the quality of castings.

Range of Solidification—In order to find the influence of the range of solidification on the general adaptability of an alloy to centrifuging casting, several alloys of different solidification ranges were tried under the same conditions. The following alloys were cast: C4A, S5A, SC51A, SG70A, and commercially pure aluminum. The casting temperature for all alloys was maintained close to 40 C (72 F) above the liquidus. A series of such castings was made under these conditions, and the radiographic examination showed some progressive increase in soundness as the solidification range of the alloy decreases.

A similar series was cast in which Dow test bars of standard design were also cast statically at the same time the panels were cast centrifugally. Table 6 summarizes the results of x-ray examination and gives the casting conditions. It should be noted that the radiograph in Fig. 22 shows the one completely sound test panel produced in the whole investigation. Table 7 lists some physical properties of the alloys. Table 8 gives mechanical properties of cast-to-shape Dow test bars and of test bars cut from the centrifuged panels.

The investigation, carried out on a very small scale, did not provide sufficient data to reach some definite conclusion as to the influence of the range of solidification on the adaptability of an alloy to centrifuging casting. However, from Table 6 it is apparent that alloy C4A, which has the largest solidification range (i.e. a long "pasty" stage), proved to be the most difficult to cast with a sound internal structure. Casting No. 135 (alloy C4A) shows medium unsoundness dispersed throughout the whole body, with a concentration under the riser. Casting No. 134 (alloy S5A) shows no porosity at all; all other castings show fine porosity with a concentration only under the riser.

This concentration of unsoundness under the riser, together with increased shrinkage in the riser for alloys with a shorter range of solidification (maximum shrinkage in the riser was noticed for a eutectic alloy),*

* Alloy S12A (12%Si) used in some experiments.

TABLE 5—MECHANICAL PROPERTIES OF TEST CASTINGS (ALLOY C4A).

Cast No. 105;								
Test Bar No.*	105-1	105-2	105-3	105-4	105-1c	105-2c	105-3c	105-4c
Ultimate Tensile Strength, kpsi	28.3	32.8	26.3	38.5	28.3	31.2	34.5	37.5
Elongation, % in 1 in.	2.0	2.0	2.0	4.0	2.0	4.0	4.0	6.0
Cast No. 106:								
Test Bar No.*	106-1	106-2	106-3	106-4	106-1c	106-2c	106-3c	106-4c
Ultimate Tensile Strength, kpsi	29.4	30.8	24.1	27.3	30.5	25.3	28.0	32.3
Elongation, % in 1 in.	6.0	5.0	3.0	5.0	7.0	5.0	7.0	8.0
Cast No. 107:								
Test Bar No.*	107-1	107-2	107-3	107-4	107-1c	107-2c	107-3c	107-4c
Ultimate Tensile Strength, kpsi	27.0	23.0	19.0	30.0	21.0	18.3	20.7	30.4
Elongation, % in 1 in.	6.0	Nil	1.0	5.0	2.0	Nil	Nil	12.0
Cast No. 108:								
Test Bar No.*	108-1	108-2	108-3	108-4	108-1c	108-2c	108-3c	108-4c
Ultimate Tensile Strength, kpsi	26.9	21.2	18.9	27.0	21.6	23.6	15.5	31.9
Elongation, % in 1 in.	3.0	1.0	3.0	4.0	2.0	Nil	Nil	9.0
Cast No. 101:								
Test Bar No.*	101-1	101-2	101-3	101-4	101-1c	101-2c	101-3c	101-4c
Ultimate Tensile Strength, kpsi	27.6	20.0	27.7	35.5	26.6	32.7	26.6	42.2
Elongation, % in 1 in.	2.0	2.0	3.0	3.0	Nil	2.0	4.0	7.0
Cast No. 102:								
Test Bar No.*	102-1	102-2	102-3	102-4	102-1c	102-2c	102-3c	104-4c
Ultimate Tensile Strength, kpsi	19.2	18.2	17.3	8.5	20.1	.9	27.7	23.4
Elongation, % in 1 in.	Nil	Nil	Nil	Nil	Nil	Nil	Nil	Nil

* See Fig. 20.

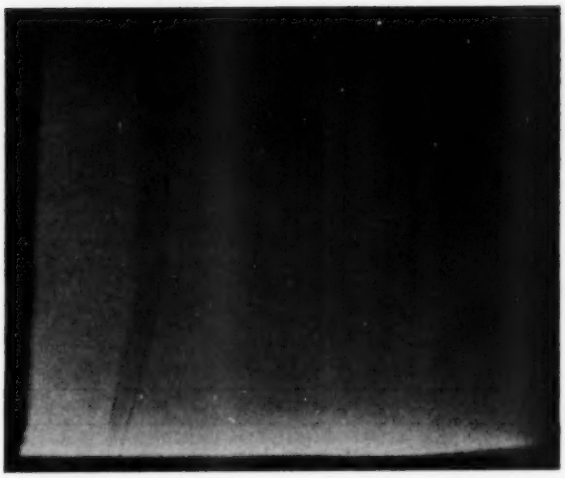


Fig. 22—Exograph of Casting No. 134; Alloy S5A; Sprue No. 3A; Runners No. 4**; Casting temperature, 670 C (1240 F); Initial speed 50 rpm, increased to 795 rpm in 60 sec.*

* Sprue designations as shown in Figs. 3 and 4.
** Runner designations: No. 1—Runners with cylindrical gate wells and gates, 600 rpm. No. 2—Runners without cylindrical gate wells and gates, 600 rpm. No. 3—Runners with cylindrical gate wells and gates, 100 rpm. No. 4—Runners without cylindrical gate wells and gates, 100 rpm. No. 5—Runners blocked. Poured through the trapping well and secondary risers (Fig. 16). No. 5A—As No. 5, with shorter secondary risers.

shows that general feeding from the riser is most efficient when the solidification range is short. Thus, the results of x-ray examination showed that under favorable conditions of proper gating and feeding, combined with favorable geometrical shape of the casting and a short solidification range, the centrifuging casting method could be superior to static casting from the point of view of increased soundness, obtained through the action of the increased hydrostatic pressure due to centrifugal force.

With reference to Table 8, it should be appreciated that the Dow test bars were tested as-cast while the centrifuged bars were machined from the test panels to ¼ in. diameter. Thus, the results of the centrifuged castings may be considered to be biased on the low side, since these bars were without the benefit of the cast skin and were much smaller in diameter.

In continuation of this work, test bar patterns were mounted on a match plate for centrifugal molds, as shown in Fig. 21, so that identical castings could be poured both statically and centrifugally, eliminating factors of cavity shape, etc.

Table 9 shows the mechanical properties of these aluminum alloys as obtained from test bars cast-to-shape centrifugally and statically from the same pot and at the same temperature of casting.

The x-ray examination showed that, generally speaking, all centrifugally-cast test bars contained less unsoundness than the equivalent static castings. This would be expected to reflect in superior mechanical properties, but, as can be seen from Table 9, this was not always the case. The explanation for this discrepancy is thought to be as follows:

TABLE 6—CASTING CONDITIONS AND SUMMARY OF X-RAY RESULTS FROM STATIC AND CENTRIFUGALLY CAST TEST BARS.

Casting Conditions									Test Panels Cast Centrifugally	Dow Test Bars Cast Statically
Casting No.	ASTM Alloy	Casting Temperature, C	Casting Temperature, F	Solidification Range, C	Solidification Range, F	Sprue No.*	Runner No.**	Spinning Speed, rpm	X-ray Investigation	X-ray Investigation
135	C4A	680	1255	95	170	3A	4	50 795	Medium porosity throughout. Heavier under riser.	No porosity, all bars sound.
134	S5A	670	1240	55	170	3A	4	50 760	Metal sound, no porosity.	No porosity.
142	SC51A	665	1230	45	80	3A	4	50 750	Fine porosity under riser.	Heavy porosity in all bars.
143	SG70A	650	1200	30	55	3A	4	50 750	Shrinkage, decreasing porosity.	Heavy porosity throughout bars.

* Sprue Nomenclature—see Figs. 3 and 4.
** Runner Nomenclature: No. 1 —Runners with cylindrical gate wells and gates, 600 rpm.
No. 2 —Runners without cylindrical gate wells and gates, 600 rpm.
No. 3 —Runners with cylindrical gate wells and gates, 100 rpm.
No. 4 —Runners without cylindrical gate wells and gates, 100 rpm.
No. 5 —Runners blocked. Poured through trapping well and secondary risers (Fig. 16).
No. 5A—As No. 5, with shorter secondary risers.

TABLE 7—PHYSICAL PROPERTIES OF VARIOUS ALUMINUM ALLOYS.

ASTM Alloy	Approximate Liquidus-Solidus Temperatures, C	Approximate Liquidus-Solidus Temperatures, F	Solidification Range, C	Solidification Range, F	Casting Temperature, C	Casting Temperature, F	Superheat above Liquidus Temperatures, C	Superheat above Liquidus Temperatures, F	Specific Gravity, g/cm³
C4A	645-550	1190-1020	95	170	680	1255	35	65	2.80
S5A	630-575	1165-1065	55	100	670	1240	40	75	2.68
SC51A	625-580	1155-1075	45	80	665	1230	40	75	2.71
SG70A	610-580	1130-1075	30	55	650	1200	40	75	2.68

TABLE 8—MECHANICAL PROPERTIES OF CENTRIFUGALLY-CAST TEST PANELS AND STATICALLY CAST TEST BARS.

		Mechanical Properties							
		Ultimate Tensile Strength, kpsi		0.2% Proof Stress, kpsi		Elongation, % in 1 In.		Brinell Hardness (500 kg load)	
ASTM Alloy	Cast and Bar Number	Centrifuged Panels	Static Cast Bars	Centrifuged Panels	Static Cast Bars	Centrifuged Panels	Static Cast Bars	Centrifuged Panels	Static Cast Bars
	135-1	29.0	26.5		21.7	2.0	2.0	85.7	81.75
	-2	26.5	27.3		22.8	Nil	2.5	71.6	83.50
	-3	30.5	28.7		24.7	2.0	2.5	75.5	84.0
	-4	29.9	27.2		23.1	2.0	2.0	71.6	83.5
C4A-T6	-1C	32.6		Not determined		3.0		78.2	
	-2C	28.8				2.0		68.0	
	-3C	28.2				Nil		70.3	
	-4C	32.9				4.0		75.5	
	Average	29.8	27.4		23.1	1.9	2.3	74.6	83.2
	134-1	20.1	20.3	9.2	7.3	6.0	11.0	42.4	42.1
	-2	19.5	20.0	9.5	7.9	5.0	11.5	42.4	40.5
	-3	19.9	20.1	9.0	7.4	9.0	12.5	42.4	41.3
	-4	19.9	20.2	10.0	8.0	9.0	10.5	42.4	41.1
S5A-F	-1C	20.1		10.1		9.0		38.6	
	-2C	20.1		10.1		7.0		38.6	
	-3C	19.4		9.6		6.0		38.6	
	-4C	19.2		8.0		6.0			
	Average	19.8	20.2	9.7	7.6	7.0	11.4	40.8	41.2
	142-1	34.6	37.9		28.1	2.0	4.0	84.2	84.0
	-2	32.1	36.3		28.4	1.0	2.5	85.7	88.3
	-3	32.6	38.5		27.9	1.0	4.0	85.7	89.3
	-4	36.0	37.6		27.4	1.0	4.0	85.7	87.3
SC51A-T6	-1C	34.2		Not determined		1.0		81.1	
	-2C	31.8				1.0		84.2	
	-3C	31.1				1.0		81.1	
	-4C	33.3				1.0		81.3	
	Average	32.2	37.6		28.0	1.1	3.6	83.6	87.3
	143-1	34.1	34.0		28.9	2.0	2.0	76.8	87.3
	-2	32.5	35.4		29.5	2.0	3.0	78.2	87.8
	-3	19.8	34.0		29.0	Nil	2.0	78.2	86.8
	-4	32.7	35.0		27.7	2.0	3.0	82.6	87.0
SG70A-T6	-1C	33.0		Not determined		1.0		76.8	
	-2C	15.7				1.0		71.1	
	-3C	33.4				Nil		78.2	
	-4C	22.2				2.0		78.2	
	Average	29.2	34.6		28.8	1.2	2.5	80.0	87.3

It was noticed that many of the centrifugally-cast bars were fracturing on the shoulder closest to the periphery of the spinning mold, and that these fractures were associated with shrinkage, particularly in the case of alloys SC51A and SG70A. This in itself is rather contradictory, in that these alloys have the shortest freezing range and therefore would be expected to be least subject to shrinkage.

However, it must be remembered that if the facilities for feeding are inadequate, an alloy with a short solidification range shows concentrated severe shrinkage, while an alloy with a long solidification range shows distributed shrinkage which may not have such a severe effect on mechanical properties. It must be remembered, also, that if at any time bridging occurs, say in the gage length of the test bar, then the centrifugal force will tend to feed the larger section at the grip end from the still liquid metal beyond the bridge.

The centrifugal force, i.e. the feeding force, is also greater here since the radius of rotation is large. This would be expected to result in concentrated shrinkage in the case of short solidification range alloys, while, because of the more advanced stage of solidification, the shrinkage may not be so severe, and would be more widespread with the long solidification range alloys. It is thought that some combination of factors such as these may be responsible for the anomalies between the x-ray and tensile results of the centrifugal and static test bar castings. It was therefore apparent that only by the elimination of these other factors could the influence of the solidification range of the alloy be established.

Grain Size

In the course of the main investigation, several test panels were cut as shown in Fig. 20, macroetched, and examined for grain size and grain distribution.

The examination revealed that grain size differs considerably on the outer and inner faces of the test panels and that the distribution of the grains on the examined surfaces depends on such factors as: alloy employed, revolving speed, runner and gating system, direction of rotation in relation to the runner, position of the riser, and other factors which influence

TABLE 9—MECHANICAL PROPERTIES OF TEST BARS CAST CENTRIFUGALLY AND STATICALLY.

ASTM Alloy	Cast and Bar Number	Mechanical Properties: Ultimate Tensile Strength, kpsi		0.2% Proof Stress, kpsi		Elongation, % in 1 In.		Brinell Hardness (500 kg load)	
		Centrifuged Bar	Static Cast Bar	Centrifuged Bar	Static Cast Bar	Centrifuged Bar	Static Cast Bar	Centrifuged Bar	Static Cast Bar
C4A-T6	136-1	33.9	28.8	20.5	22.9	5.5	1.0	74.1	82.6
	-2	30.5	33.4	21.2	24.3	3.0	3.0	72.8	82.6
	-3	29.2	31.2	20.0	22.4	4.0	2.5	72.8	82.6
	-4		31.3		22.3		4.5		82.6
	Average	31.2	31.2	20.8	23.0	4.2	2.8	73.2	82.6
S5A-F	137-1	22.6		8.8		10.0		42.4	
	-2	23.0	20.7	11.6	9.1	15.5	16.0	42.4	38.6
	-3	22.3	20.8	8.2	9.9	10.0	16.0	42.4	38.6
	-4	21.2	21.0	8.4	7.5	7.0	16.0	42.4	38.1
	Average	22.3	20.8	9.2	8.8	10.5	16.0	42.4	38.4
SC51A-T6	138-1	33.6	37.6	21.9	30.9	4.0	2.5	74.1	85.8
	-2	36.7	37.8	22.1	30.7	8.0	2.0	69.1	89.0
	-3	32.6	38.5	21.9	31.8	3.0	3.0	71.6	89.0
	-4	35.3	37.6	21.3	30.8	5.0	2.5	69.0	90.8
	Average	34.6	37.9	21.8	31.1	5.0	2.5	70.9	88.6
SG70A-T6	139-1	26.3	34.6	12.0	24.8	17.5	6.0	44.5	72.8
	-2	26.5	34.1	12.3	25.3	20.0	5.0	46.1	74.1
	-3	25.9	33.5	12.5	23.8	19.0	5.0	43.8	74.1
	-4	25.8	32.6	11.9	23.9	13.0	5.5	43.2	72.8
	Average	26.1	33.7	12.2	24.5	17.3	5.4	44.2	73.2

TABLE 10—GRAIN SIZE FOR TEST BARS CAST STATICALLY AND CENTRIFUGALLY.

Casting No.	ASTM Alloy	Static Casting A	Static Casting B	Centrifuged Casting A	Centrifuged Casting B	Casting Temp., C	Casting Temp., F	Spinning Speed, rpm
		Grain Size, Mean Diameter in 0.001 Inch						
136	C4A-T6	10	10	7	7	678	1250	600/800
137	S5A-F	20	14	10	7	670	1240	600/820
138	SC51A-T6	20	20	10	10	670	1240	600/650
139	SG70A-T6	20	18	7	7	650	1200	600/770

the entrance of the metal into the mold cavity and the feeding during solidification. Figures 23 to 27 show cross sections of some of the test panels.

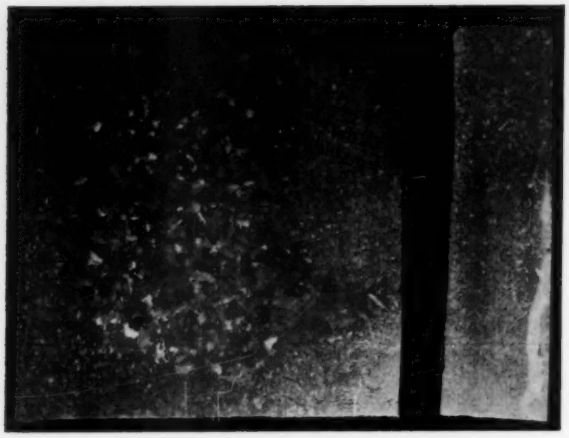
Table 10 lists grain sizes of sections cut from test bars cast statically and centrifugally. Grain size was measured close to the shoulders of the test bar in the grip section. The letter "A" corresponds to the shoulder neighboring the sprue, and the letter "B" corresponds to the shoulder from the other end of the test bar. The values given are mean values, taken from four test bars.

The grain size of the centrifugally-cast test bars is consistently finer than that of corresponding test bars cast statically. After heat treatment, grain size is uniform in both ends of the test bars, both for static and centrifugal methods of casting, but still shows the difference between the two methods of casting. Grain sizes differ for both ends for the test bars in the non-heat treatable alloy S5A. Grains from test bars and grains from test panels for the same alloy and for the same conditions of pouring differ considerably both in shape and in size.

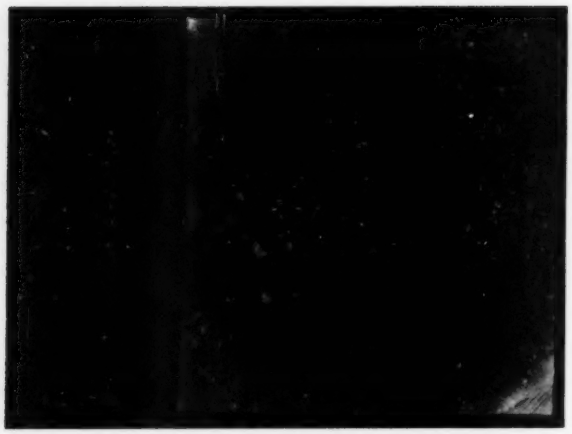
In castings with small cross sections perpendicular to the direction of the flow, such as the test bars, the grains are much smaller and uniform throughout the entire length of the casting. Freezing starts from numerous nuclei and proceeds very rapidly due to the chilling effect of the walls of the mold cavity. Feeding, of course, is restricted, resulting in shrinkages in the case of the test bars, mostly around the shoulder situated farther from the sprue, where larger cross sections are being fed by metal flowing from narrower passages.

In castings with large cross sections perpendicular to the direction of the flow of incoming metal and dimensionally short in radial direction, such as the test panels, feeding is much better, although formation of bridges leading to shrinkage is always possible due to the disturbed entry of the metal and the uneven distribution of the cold and hot metal inside the mold cavity. In castings of this type, there is always a large shrink in the middle, due to solidification proceeding from the walls of the mold cavity, and, therefore, appropriate risering is necessary.

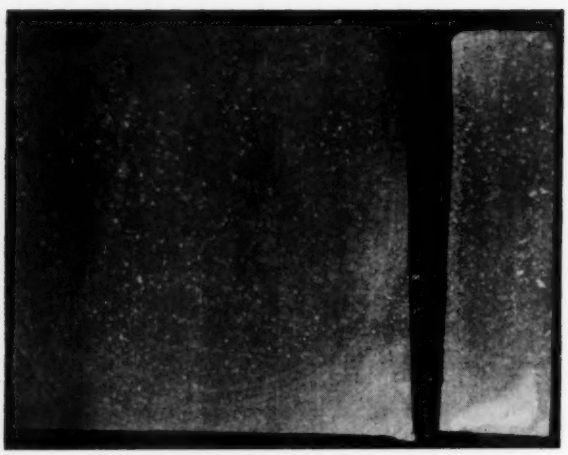
In such castings there is a wide distribution in the size and shape of the grains, changing from very small, close to the outside wall of the mold cavity and close to the sides, to very large on the inside surface of the casting which, because of the action of the centrifugal force, is filled last with relatively hot metal. Lower



A—Inner Face

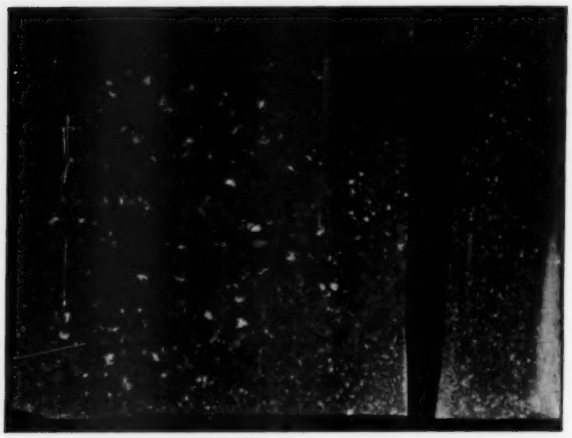


B—Middle Cross Section



C—Outer Face

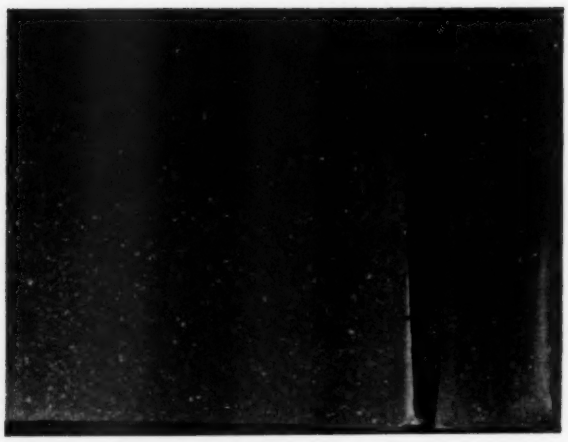
Fig. 23—Cross section of test panel; Casting No. 118; Alloy C4A; Sprue No. 3A; Runners No. 3; Casting temperature, 680C (1255 F); Initial casting speed 200 rpm, increased to 810 rpm in approximately 60 sec.



A—Inner Face

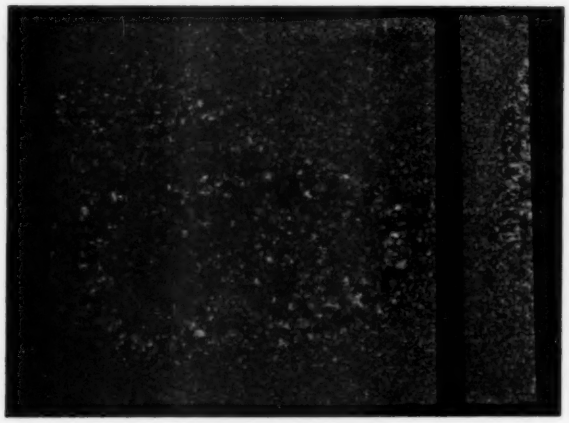


B—Middle Cross Section

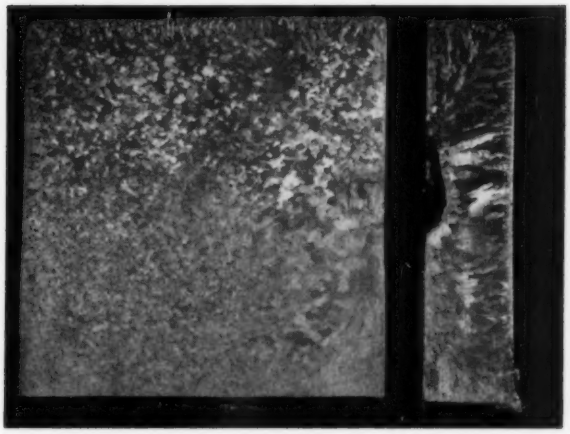


C—Outer Face

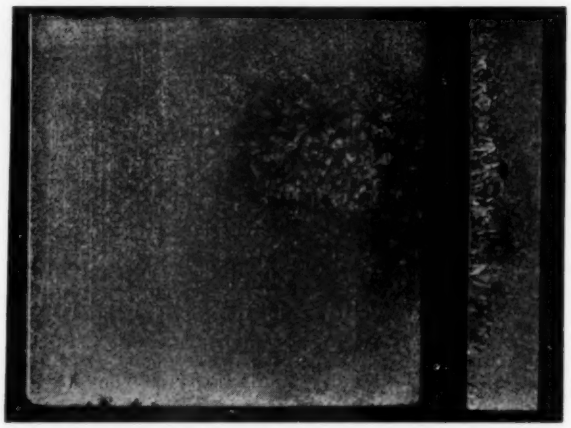
Fig. 24—Cross section of test panel; Casting No. 114; Alloy C4A; Sprue No. 3A; Runners No. 3; Casting temperature, 710 C (1310 F); Initial casting speed 100 rpm, increased to 800 rpm in approximately 60 sec.



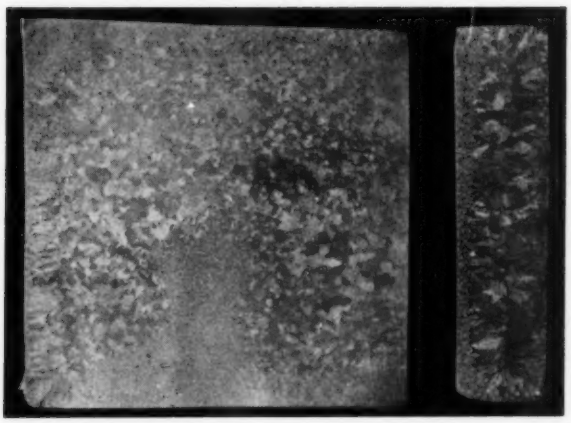
A—Inner Face



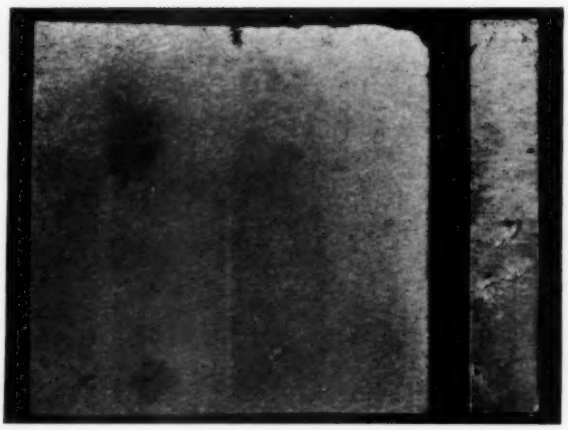
A—Inner Face



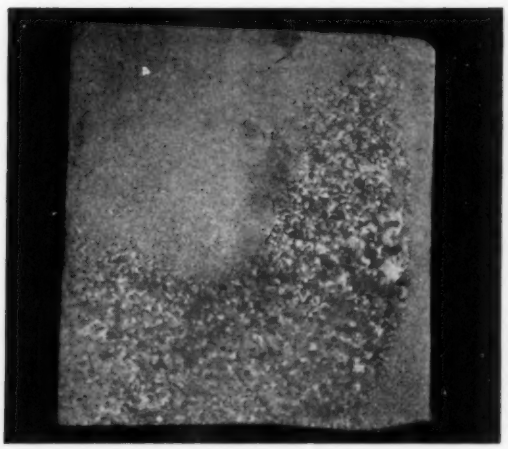
B—Middle Cross Section



B—Middle Cross Section



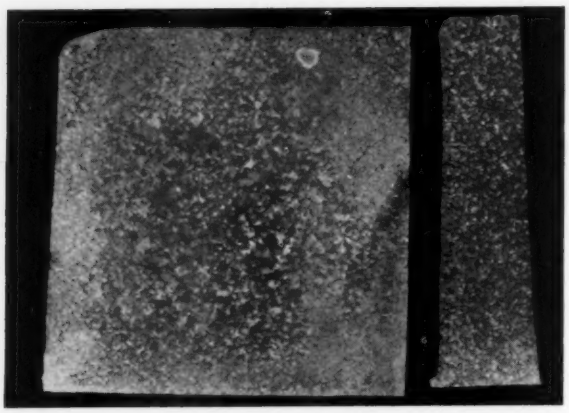
C—Outer Face



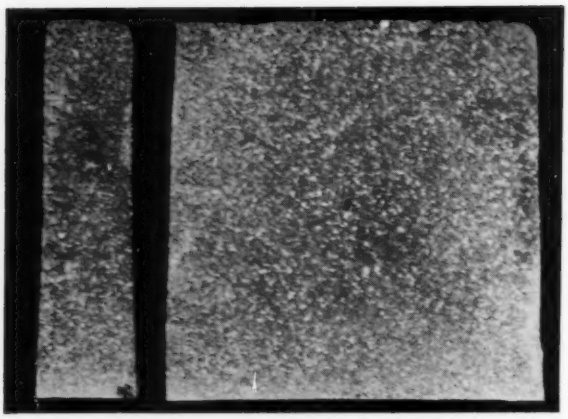
C—Outer Face

Fig. 25—Cross section of test panel; Casting No. 89; Alloy C4A; Sprue No. 1; Runners No. 1; Casting temperature, 680 C (1255 F); Casting speed, 600 rpm.

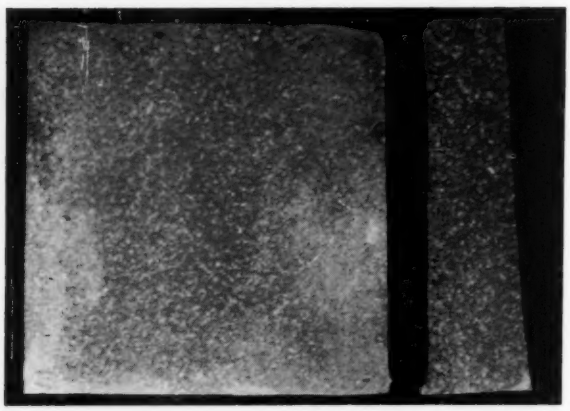
Fig. 26—Cross section of test panel; Casting No. 100; Alloy 99.95 Aluminum; Sprue No. 1; Runners No. 1; Casting temperature, 680 C (1255 F); Casting speed, 600 rpm.



A—Inner Face



B—Middle Cross Section



C—Outer Face

Fig. 27—Cross section of test panel; Casting No. 134; Alloy S5A; Sprue No. 3A; Runners No. 4; Casting temperature, 670 C (1240 F); Initial casting speed 50 rpm, increased to 760 rpm in approximately 60 sec.

initial speed, by decreasing the disturbance of the inflowing metal inside the mold cavity, tends to decrease the difference in grain size.

Density

No significant difference in density was observed between centrifuged and static castings. Some test bars, which had been cut from test panels cast centrifugally at higher revolving speeds, showed even slightly lower density than those cast from the same pot statically. Test bars cut from test panels cast at lower initial speeds (50-100 rpm), and with speeds increased to 600-800 rpm during solidification, showed slight increase in density compared to bars centrifugally cast at an initial speed of 600 rpm.

Discussion of Results

All attempts to obtain a sound structure throughout the whole body of the test casting at a high spinning speed (600-700 rpm), necessary from the point of view of sufficient hydrostatic pressure, failed for all aluminum alloys employed in this investigation.

As shown in Fig. 22, a completely sound casting was obtained in alloy S5A, using an initial spinning speed of 50 rpm and increasing it in 60 sec to a maximum speed of 760 rpm.

Casting at low initial speeds, with a sufficiently high acceleration ratio to the maximum spinning speed, proved to be, for the designed gating, much more successful as compared with casting at high initial speeds. Theoretical considerations of the adaptability of aluminum alloys to centrifugal casting confirm this result.

The following advantages of centrifugal casting are the result of the peculiarities of the method itself, independently of the metal employed:

1. Increased yield of metal,
2. Closer tolerances,
3. Better feeding,
4. Lower temperature of casting.

Other advantages are the result of a favorable combination of the peculiarities of the casting technique and the characteristics of the metal. These advantages are:

1. Refining action due to centrifugal force,
2. Degassing action due to internal pressure.

These two last advantages influence positively the mechanical properties of the metal. Since light alloy characteristics (low specific weight, high chemical activity, and small differences in oxide and metal density) are not favorable for the centrifugal casting technique, both of these advantages seem to be doubtful. Refining action could not take place, due to the nearly equal densities of the oxides and the metal and the very short time available for refining. The favorable influence of degassing action by hydrostatic pressure is completely overshadowed by the unfavorable effects of flow turbulence caused by high revolving speeds.

The structure of centrifugal castings differs from that of static castings in grain size and distribution. This difference is more marked in thick, open castings, where the effect of nucleus migration due to the centrifugal force is more pronounced. As shown in Figs. 23 to 27 inclusive, columnar crystallization

occurs at the inward surfaces, reaching quite deeply inside the casting, depending on the temperature of casting and the alloy employed. Grain size decreases in the outward direction, reaching at the outer surface a very fine equiaxial structure, finer than found normally in a static casting. This phenomenon has its unfavorable effect on the uniformity of the mechanical properties of centrifuged castings.

The distribution of porosity follows in general the pattern of the grain size distribution. Porosity is concentrated mainly close to the center and the inner zone of the casting. The outer zone and parts of the castings farther away from the riser were generally sound.

The mechanical properties varied for different parts of the test panels cast at high initial speeds. Although tensile strengths of 30 per cent above the value guaranteed for static castings were obtained, these high mechanical properties were not uniform throughout the casting and, in some cases, the spread of properties was considerable. These differences in mechanical properties were not so pronounced in castings which have been cast at low initial speeds where the mechanical properties, although lower, were more uniform throughout the whole test panels. The available time of acceleration to the maximum speed was probably too long and the effect of the corresponding higher hydrostatic pressure and centrifugal force has been lost because the metal was already solidified.

The high mechanical properties obtained from time to time from sound parts of the castings which had been cast at high rotating speeds proved that with the metal delivered into the mold cavity without disturbances, or oxidation, and in a manner favorable for inward progressive solidification, a uniform increase in mechanical properties could be obtained, provided the centrifugal force and the hydrostatic pressure are sufficiently high.

The factor of shrinkage porosity is of prime importance and has already been discussed in some detail. Distribution of shrinkage porosity, solidification range of the alloy, directional solidification, design of risers, and action of centrifugal force and hydrostatic pressure—all are interrelated, and it becomes extremely difficult to distinguish the effect of any one individual factor, say the solidification range. It was apparent that some of these confusing factors would have to be eliminated, and, in order to do this, a special electrical resistance furnace was designed and constructed which was capable of melting the charge in a cylindrical mold while in motion on a rotating plate.

Since melting and cooling of the charge was accomplished while the furnace and cylindrical mold are rotating with the revolving table, the metal, being substantially stationary within the mold, is subjected to all the advantages of centrifugal force without the disturbing effects of turbulent flow through runners, etc. Progressive solidification from the outside towards the center, and feeding against solidification shrinkage, is secured by a specially arranged air cooling system acting on the bottom of the mold. The first runs with the furnace proved that it operated satisfactorily, and further experimenal work on this aspect is in progress.

Summary

To summarize the results of all phases of the work to date, the following conclusions may be drawn:

1. **Sand.** The sand mixture and the mold wash to be used for high speed centrifuging must withstand high metal pressure and the resultant tendency to heavy metal penetration. A suitable dry sand mixture and mold wash were developed and used successfully throughout the present investigation.

2. **Gating.** To avoid turbulence of the metal flow and dross inclusions, the gating must be designed in accordance with the following requirements:
   (a) It must decrease the high linear velocity of the metal by resolving of the acting centrifugal force, and
   (b) It must conform to the general principles of fluid mechanics for steady and undisturbed flow.

3. **Feeding.** The centrifuging technique is characterized by better feeding conditions as compared with a similar static casting because of the mechanical action of the centrifugal force, and, additionally, also due to segregation and the consequent tendency of the remaining liquid of lower freezing temperature and higher density to move outwards and solidify last.

4. **Casting Shapes.** The application of the centrifuging technique to light alloy castings is limited to such shapes which allow the avoidance of segregation and non-uniformity of grain size. Open shape castings with heavy cross-sections perpendicular to the radial arrangement in the mold should be avoided.

5. **Alloy.** The "castability" of the alloys is a very important factor in centrifuging, and poor casting characteristics of an alloy are much more accentuated in this casting method.

6. **Pouring Temperature.** The effect of pouring temperature on mechanical properties and on the casting structure has been studied, and it was found that the grain size, as well as the tendency towards axial crystal growth and segregation, decreases with lower pouring temperature.

7. **Spinning Speed.** It was found that castings poured at a low initial spinning speed and accelerated rapidly to a higher speed showed considerably better properties as compared with castings poured at high rotating speeds.

Conclusions

Casting into sand molds using high speed centrifuging has so far not proved to be practical for light alloys. More effort towards the development of gating design, as well as casting and pouring techniques, is needed before all the difficulties encountered in connection with turbulent flow and jet-like influx of the metal will be eliminated. The tendency to columnar crystallization and the resulting non-uniformity of grain size throughout the casting require further study of the factors affecting adversely these phenomena.

Shapes such as the test panels are not adaptable to centrifuging methods, due to the resulting differences in grain size, low mechanical properties, and the necessity of risering.

Using low initial centrifuging speeds, casting into sand molds presents a field of commercial applications

for light alloys, where increased yield of metal, closer tolerances, lower temperature of casting and other advantages affecting favorably the cost of production are most important. This application is limited to castings which have shapes and cross sections favoring non-turbulent filling of the mold cavity, progressive solidification, and uniform grain size distribution. Molding practice, wherever possible, should take advantage of the possibilities of christmas tree and cluster formation, presented by the central location of the sprue with mold cavities located on its periphery.

Results of the present investigation showed that centrifuging may be used in the light alloy foundry, but it illustrated also the limitations of this method regarding casting shapes, alloys, and spinning speeds.

The investigation on the design and development of a gating system suitable for the production of centrifuged aluminum alloy castings was not completed, because of the necessity of interruption of further experimental work due to assignments of higher priority, and the whole project had to be postponed for the time being at least. It is hoped that the AFS Committee on Centrifuging Casting of Light Alloys will be able to continue this research project and that the work will include a more fundamental study on the fluid flow and the solidification mechanism in rapidly rotating molds, as proposed in the conclusions of the Progress Report published in AFS TRANSACTIONS, vol. 57, pp. 629-631 (1949).

SUBJECT INDEX

A

Acid cupola slags and some relationships with melting conditions . . . 527-539
Acid electric furnace roof, Advantages of insulating an 593-594
Agar gels in plaster-bonded investment . . . 347-351
Air furnace bottom temperature, Correlation of, to refractory and operating practice in a cupola-air furnace duplex system . . . 479-489
Air furnace, Construction and operation of coal-fired . 634-639
Air furnace design and operation . . . 516-522
Air pollution and public relations . . . 662-663
Air pollution control equipment for melting operations 467-478
Air-set strength and dry properties vs squeeze pressure . 496
Albany sand, Geology of . . . 435
Aluminum alloy molding sand . . . 432
Aluminum alloys, Centrifugal casting of . . . 744-762
Aluminum-4% copper alloys, Effect of casting "fiber" on mechanical properties of . . . 664-673
Annealability of malleable iron . . . 107-130
Annealing gray iron for:
machinability . . . 5, 651-653
stress relief . . . 5
Annealing of malleable iron, Influence of furnace atmosphere on . . . 291-301
As-cast stress, Magnitude of initial . . . 648
Aspiration of air in straight sprues . . . 91
Atmosphere, Influence of furnace, on the annealing of malleable iron . . . 291-301
Atmosphere, melting furnace, Effects of, on casting properties and annealability of malleable iron . 107-130
Atmospheric pollution:
codes . . . 467
defined . . . 467
Austenite formation, Mode of . . . 428

B

Baking oil-bonded cores, Induced flow process of . . 279-290
Balling up of molding sand . . . 433
Base sand, Effect of, in shell molding . . . 21
Basic cupola:
melting . . . 52-60
operation, Phosphorus removal in . . . 546-551
water-cooled . . . 58, 59
Basic-lined cupolas . . . 4
Bentonite and fireclay, Use of, in molding . . . 436
Binders and mold materials, Effect of, on macro-gas porosity in 85-5-5-5 bronze . . . 330
Binders, Control tests for shell molding . . . 368
Blast furnace linings . . . 640
Bleed tests, Opinions on . . . 604
Brinell hardness test . . . 659
Brittle fracture, Metallurgical aspects of . . . 246
Brittle fracture of cast and rolled steels . . . 243-262
Bronze:
A study of segregation in, using a radioactive technique . . . 352-354
Spherical type macro-gas porosity in 85-5-5-5 . . 327-346
Bulge tests . . . 250

C

Camber in large castings . . . 41-42
Carbon as a refractory . . . 640-642
Carbon equivalent and chill test depth of gray cast irons . . . 387-393
Cast iron:
Ductile, Prediction of mechanical properties from chemical composition for fully-annealed . . . 510-515
Cast iron (Cont.):
Flame hardening of . . . 657-661
Heat treatment of . . . 643-661
Metallurgy . . . 3, 658
Stress relief heat treatment of . . . 646-650
Casting design . . . 38
Cement, Use of, in molding . . . 436
Centrifugal casting:
aluminum alloys . . . 744-762
defined . . . 702
Engineering aspects of . . . 701-718
non-ferrous metals . . . 711
range of solidification . . . 754
True . . . 702
Centrifuged castings . . . 716
Centrifuging defined . . . 702
Charcoal, Use of, in molding . . . 436
Chemistry of core drying . . . 280
Chill size, Effect of . . . 616
Chill test depth and carbon equivalent of gray cast irons . . . 387-393
Chills, Effect of . . . 688
Chills, Use of, to increase feeding distances . . . 74-76
Chvorinov's rule of heat transfer . . . 686
Clay balls, How to reduce . . . 434
Coal, Fineness of . . . 519
Coal, Volatile matter in . . . 519
Coal-fired air furnaces, Construction and operation of 634-639
Codes, Atmospheric pollution . . . 467
Coke size, Effect of . . . 537
Coke, Types of, for 24-in. cupola . . . 531
Cold melt iron . . . 126
Combination system, Sand reclamation with . . . 222-227
Combustion control in air furnace . . . 638
Contour patching of cupola . . . 380
Control tests for shell molding binders . . . 368
Controlling costs, Use of standards in . . . 457-466
Conveyor molding standards . . . 263-271
Copper-base alloy molding sands . . . 432
Core:
Bonding principles of oil and water in a green . . 279
classification . . . 357
department costs . . . 525
department, Layout of mechanized . . . 36
drying, Chemistry of . . . 280
drying, Thermodynamics in . . . 280
finishing, Standard data tables for . . . 355-366
mixtures . . . 150
Cores:
Baked and hot properties of . . . 151
Induced flow process of baking oil-bonded . . 279-290
in shell molding . . . 15-16
Sand, for semi-permanent mold work . . . 505
Veining tendencies of . . . 150-155
Cost control for indirect operators, Wage incentive for 561-569
Cost system, A simplified, for small jobbing foundries 600-602
Costs, Are your, reliable? . . . 523-526
Costs, Use of standards in controlling . . . 457-466
"C" process . . . 7-28
Crack propagation tests . . . 253
Crankcase sealer as a pattern coating . . . 580
Croning, Johannes and the "C" process . . . 7
Crystallographic orientation . . . 671
Cupola:
Acid, slags . . . 527-539
applications of carbon as a refractory . . . 640
dust collecting systems . . . 731-732
fume collection equipment . . . 469-473
lining, Gun placed silica . . . 378-386
operation, Basic, Phosphorus removal in . . . 546-551

Cupola (Cont.):
slag control . . . 527
Cupola-air furnace duplex system . . . 479-489
Curing time, Effect of, in shell molding . . . 20
Cylinders, Solidification of . . . 142-149

D

Definitions:
Atmospheric pollution . . . 467
Centrifugal casting . . . 702
Centrifuging . . . 702
Fluidity . . . 399
Indirect work . . . 561
Pyrometric cone equivalent, P. C. E. . . . 733
Reclaimed sand . . . 196
Reconditioned sand . . . 196
Semi-centrifugal casting . . . 702
Sound . . . 699
Work units . . . 562
Deformation of green molding sand . . . 159-165
Deformation test . . . 159
Degassifiers and deoxidizers, Effect of, on macro-gas porosity in 85-5-5-5 bronze . . . 337
Denseners, Use of, in gray iron practice . . . 46, 47
Density of centrifugal castings . . . 760
Design considerations for permanent mold castings . . 506
Design of castings . . . 38
Design of patterns . . . 38, 39
Desulfurization of gray iron in basic cupola operation . 52
Dimensional tolerance in shell molding . . . 14
Direct labor costs . . . 458
Dry reclamation of molding sand for steel castings . 200-205
Dry mulling plus aeration of sand . . . 193
Drying of molds . . . 46, 47
Ductile cast iron:
Hardening characteristics of induction heated . 422-431
Prediction of mechanical properties from chemical composition for fully-annealed . . . 510-515
Duplex iron . . . 126
Duplexing furnace, Layout of a . . . 480
Dust collecting systems, Trends in cupola . . . 731-732
Dust control trends . . . 394-398

E

Educational program for foundries . . . 5
Efficiency, Noise effects on work and . . . 695
Electric furnace roof, Advantages of insulating an acid 593-594
Electric furnaces, Refractories for indirect-arc, for nonferrous melting . . . 733-743
Endothermic and exothermic oxidation . . . 111
Epoxy resins used in shell molding . . . 8, 9
Equipment for shell molding . . . 9
Eutectic modification, Effects of magnesium on, and graphitization of certain Fe-C alloys . . . 674-677
Evaluation of resins for shell molding . . . 8
Exothermic and endothermic oxidation . . . 111
Expansion test data on sand mixtures . . . 96

F

Feed metal flow, Characteristics of . . . 68
Feeding of gray iron . . . 44
Feeding range and riser adequacy . . . 61-80
Feeding range of joined sections . . . 76-79, 302-308
"Fiber," Effect of casting, on mechanical properties of aluminum-4% copper alloys . . . 664-673
Fineness of coal . . . 519
Finger-gate systems . . . 446, 447
Finish, Casting surface, Effect of sand flowability on . 574-579
Finishing department costs . . . 525
Flame hardening of cast iron . . . 657-661
Flow, metal, in gating system, Velocities and volume rates of . . . 439-450
Flowability of sand . . . 156-158
Flowability, sand, Effect of, on casting surface finish . 574-579
Flowability test . . . 157
Fluidity:
defined . . . 399
of malleable iron and furnace atmosphere . . . 113
Fluidity (Cont.):
Slag . . . 534
test pieces . . . 400-404
testing of molten metals . . . 399-410
Foundry characteristics of magnesium alloy ZK61 . . . 726
Foundry Industry, Outstanding opportunities for the . 1-6
Foundry noise and its control . . . 694-700
Fracture, Brittle:
at high temperature . . . 255
Mechanical aspect of . . . 244
Metallurgical aspects of . . . 246
of cast and rolled steels . . . 243-262
Fracture types . . . 244, 669
Freezing mechanism, Factors influencing . . . 610
French molding sand . . . 435
Fundamental concepts of sand reclamation . . . 192
Furnace, Air, design and operation . . . 516-522
Furnace atmosphere:
and fluidity of malleable iron . . . 113
and mottling tendency in malleable iron . . . 112
Influence of, on annealing of malleable iron . . 291-301
melting, Effects of, on casting properties and annealability of malleable iron . . . 107-130
Furnace gases, Typical data on . . . 470
Furnace, Refractories for indirect-arc electric, for nonferrous melting . . . 733-743
Furnace roof, Advantages of insulating an acid electric 593-594

G

Gases, Typical data on furnace . . . 470
Gating:
and feeding centrifuged castings . . . 745
and heading for shell molding . . . 12-14
of gray iron castings . . . 42, 43
Principles of ratio . . . 623-627
systems, Velocities and volume rates of metal flow in 439-450
to control pouring rate . . . 416-421
vertical . . . 81-95
German process of centrifugal casting . . . 705
Glossary of sound terminology . . . 699
Gradient conditions required for feeding to soundness . 72
Grain size of centrifugal castings . . . 756
Graphite formation characteristics . . . 643
Graphite formation, Slow cooling effect on . . . 644
Graphitic nickel alloy castings, Properties of . . . 552-560
Graphitization, Effects of magnesium on eutectic modification and, of certain Fe-C alloys . . . 674-677
Graphitization in the malleable iron industry . . . 228-242
Gray iron:
Annealing for machinability . . . 5, 651-653
Elements in . . . 132
feeding . . . 44
Gating of . . . 42, 43
Hardening, with minimum distortion . . . 654-656
Heat treatment of . . . 643-661
Low-sulphur . . . 3
Martempering . . . 5
pouring rate . . . 43
practice, Effect of head pressure in . . . 45
practice, Gases in . . . 132-135
Relation of chill test depth and carbon equivalent of . . . 387-393
Risering of . . . 131-141
Stress relief heat treatment for . . . 5, 646-650
Use of denseners in . . . 46, 47
Green hardness vs ramming pressure in molding . . . 490
Green molding sand, Deformation of . . . 159-165
Green properties of reclaimed sand . . . 215
Green properties vs squeeze pressure in molding . . 493
Gun placed silica cupola lining . . . 378-386
Gun tubes, Manufacture of . . . 703

H

Hardening characteristics of induction heated ductile iron . . . 422-431
Hardening, Flame, of cast iron . . . 657-661
Hardening gray cast iron with minimum distortion . 654-656
Hardness testing . . . 659
Head pressure, in gray iron practice . . . 45

Heat flow in sand 456
Heat transfer 603-622
Heat treatment of gray cast iron 5, 643-661
Heat treatment of ZK61 magnesium alloy 721
Hexamethylenetetramine resin used in shell molding . . 8
Hot compressive strength of sand mixtures 98-101
Hot cure test for sand-resin mixtures 184
Hot deformation vs squeeze pressure in molding . . . 497
Hot rigidity test for sand-resin mixtures 185
Hot strength vs squeeze pressure in molding 497
Hot tearing:
  Analogy of mechanics of 181
  film stage concept 180
  Influence of molding materials on 167-183
  of malleable iron 115-119, 128
  Strain theory of 181

I

Incentive, Basis for supervisory 562
  Indirect labor 459
  Procedure for applying indirect 562
  system, Effect of good 457
  Wage, for cost and production control for indirect operators 561-569
Indirect-arc electric furnaces, Refractories for, for non-ferrous melting 733-743
Indirect labor incentives 459
Indirect work defined 561
Induced flow process of baking oil-bonded cores . . 279-290
Induction furnace, gas-atmosphere, for melting titanium 540
Induction heated ductile iron, Hardening characteristics of 422-431
Inhibitor evaluation (for shell molds) 185
Insulating an acid electric furnace roof, Advantages of 593-594
Insulators, Effect of 688
Internal stresses, Nature of 647
Investment, Agar gels in plaster-bonded 347-351
Isothermal transformation 423, 654

J

Jobbing foundries, A simplified cost system for small 600-602

L

Lining, Gun placed silica cupola 378-386
Low-sulphur gray iron 3
Lucite, Preparation of 94

M

Machinability, Annealing iron castings for 651-653
Machinability of gray iron, Annealing for 5
Machine tool castings, Efficient methods of production of 29-51
Macro-gas porosity in 85-5-5-5 bronze 327-346
Magnesium alloy permanent mold and semi-permanent mold castings 502-509
Magnesium casting alloy ZK61:
  Characteristics of high strength 719-728
  Effect of rare earth additions on 723
  Foundry characteristics of 726
  Heat treatment of 721
Magnesium, Casting, in shell molds 184-190
Magnesium, Effects of, on eutectic modification and graphitization of certain Fe-C alloys 674-677
Magnesium sand casting alloys containing thorium . 315-326
Magnesium, shell cast, Metallurgy of 187
Malleable, Heavy-section 3
Malleable iron:
  Annealability of 119-128
  Cause of pearlitic rim in 299
  industry, Graphitization in 228-242
  Effects of melting furnace atmosphere on casting properties and annealability of 107-130
  Hot tearing of 115-119, 128
  Influence of furnace atmosphere on annealing of 291-301
  Manganese content in 111
  Pearlitic, Development of 3
  Silicon content in 110
Malleable shop, Multiple molding in a 628-633
Malleabilizing heat treatment 120
Manganese in malleable iron 111
Martempering gray iron 5
Mechanics of shell molding process 9
Mechanization in shell molding 16-17
Mechanization, Mechanics of foundry 272-278
Mechanized core department, Layout of 36
Melamine-formaldehyde resin used in shell molding . 8, 9
Melamine-urea-formaldehyde resins used in shell molding 8
Melting department costs 523
Melting furnace atmosphere, Effects of, on casting properties and annealability of malleable iron . . 107-130
Melting gray iron 48-51
Melting operations, Air pollution equipment for . . 467-478
Melting practice, Effect of, on macro-gas porosity in 85-5-5-5 bronze 328
Melting, Refractories for indirect-arc electric furnaces for non-ferrous 733-743
Metal flow in gating systems, Velocities and volume rates of 439-450
Metallurgical bases for a titanium castings industry . 678-685
Metallurgy of cast iron 3, 658
Metallurgy of shell cast magnesium 187
Metallurgy and shell molding 18, 19
Methods, Efficient, of production of machine tool castings 29-51
Methods engineering increases efficiency 5
Micro-chemistry in the foundry industry 4
Microstructure of cast titanium-carbon alloys . . . 413-414
Microstructure of centrifugally-cast tubes 709
Microstructure of nodular iron 424
Mold closing in shell molding 15
Mold coating, Effect of, on macro-gas porosity in 85-5-5-5 335
Mold for titanium casting 589
Mold materials, Evaluation of, for titanium castings 540-545
Mold materials for titanium casting 679
Mold variables 615
Mold department costs 524
Mold materials, Influence of, on hot tearing . . . 167-183
Molding mixture for shell molding, Tests for . . 166, 370-374
Molding, Multiple, in a malleable shop 628-633
Molding sand, Deformation of green 159-165
Molding sand mixtures for non-ferrous casting . . . 436-437
Molding sands, Non-ferrous 432-438
Molding sands, Ramming of 490-501
Molding standards, Conveyor 263-271
Molds, plugs and stool inserts 640
Molten metals, Fluidity testing of 399-410
Monolithic patches for cupola 384
Mottling tendency in malleable iron and furnace atmosphere 112, 127
Mullite-type refractories 735
Multiple molding in a malleable shop 628-633

N

Natural sands 433
Nickel alloy castings, Properties of graphitic . . . 552-560
Nodular iron:
  in basic cupola 55
  Research in 3
Noise:
  effects on work and efficiency 695
  Foundry, and its control 694-700
  levels, Permissible 695
Non-ferrous:
  casting, Molding sand mixtures for 436, 437
  melting, Refractories for indirect-arc electric furnaces for 733-743
  metals, Centrifugal casting of 711
  molding sands 432-438

O

Oil-bonded cores, Induced flow process of baking . . 279-290
"One-purpose sand," Feasibility of 434

P

Parting agents used in shell molding, Evaluation of . 9
Pattern coating:
  Guide to choosing 585
  Research in, produces quality castings 580-586
Pattern design 38, 39

Pattern equipment for shell molding . . . 11-12, 729-730
Patterns for shell molding . . . 595-599
Pearlitic malleable, Development of . . . 3
Pearlitic rim in malleable iron, Cause of . . . 299
Permanent mold and semi-permanent mold castings, Magnesium alloy . . . 502-509
Permanent mold casting design considerations . . . 506
Permanent mold magnesium castings:
- Inserts for . . . 505
- Section thickness of . . . 505
- Size and weight of . . . 505

Permanent mold tolerances . . . 506
Permeability of shell molds . . . 14
Permeability test for sand-resin mixtures . . . 185
Phenol-formaldehyde resin used in shell molding . . . 8
Phosphorus removal:
- by oxidation in basic cupola melting . . . 52
- in basic cupola operation . . . 546-551

Pitch, Use of, in molding . . . 436
Plaster-bonded investment, Agar gels in . . . 347-351
Pneumatic method of sand reclamation . . . 195
Polytetrafluoroethylene as a parting agent in shell molding 9
Porosity, Macro-gas, in 85-5-5-5 bronze . . . 327-346
Pouring basin for centrifugal casting . . . 747
Pouring directly down the sprue . . . 91
Pouring rate as affected by:
- Sprue cups . . . 416
- Sprue post diameter . . . 417
- Sprue post length . . . 417
- Runner area . . . 418
- Runner chokes . . . 419
- Ladle height . . . 419
- Skim cores . . . 421

Pouring rate, Gating to control . . . 416-421
Pouring rate of gray iron . . . 43
Protective equipment . . . 571
Pyrometric cone equivalent, P.C.E., defined . . . 733

R

Radioactive technique, A study of segregation in bronze using a . . . 352-354
Ramming of molding sands . . . 490-501
Rare earth additions, Effect of, on magnesium alloy ZK61 723
Ratio gating, Principles of . . . 623-627
Reclaimed sand:
- Cost of . . . 227
- defined . . . 196
- Green properties of . . . 215
- Surface of . . . 215

Reclamation:
- A wet method of sand . . . 206-210
- Dry, of molding sand for steel castings . . . 200-205
- methods of sand, Past . . . 193
- of foundry sand . . . 191-227
- of sand by pneumatic dry scrubbing . . . 196-199

Reclamation, Sand:
- Benefits of . . . 222
- Fundamental concepts of . . . 192
- in a steel foundry . . . 211-221
- with the combination system . . . 222-227

Reclamation units operating precautions . . . 219
Reconditioned sand defined . . . 196
Reconditioning of sand . . . 193
Refractories, Attack of, by slag . . . 734
Refractories of indirect-arc electric furnaces for non-ferrous melting . . . 733-743
Refractories, Spalling of . . . 734
Refractories testing . . . 733
Refractory and operating practice in a cupola-air furnace duplex system . . . 479-489
Refractory, Carbon as a . . . 640-642
Refractory composition control . . . 379
Refractory lining, Installation of . . . 735
Refractory lining, Zircon as a . . . 739
Refractory linings, new, Drying . . . 738
Refractory maintenance in basic cupola operation . . . 54
Refractory selection factors . . . 733
Relaxation allowances . . . 357
Resin properties and characteristics . . . 369
Resins and sands for shell molding . . . 367-377
Resins, Evaluation of, for shell molding . . . 8
Resins, Phenolic, in shell molding process . . . 1
Resins, sand-conditioning, Function of . . . 1
Resins used in shell molding . . . 8
Retained strength vs squeeze pressure in molding . . . 498
Riemann temperature, Computation and significance of . 147
Riser adequacy and feeding range . . . 61-80
Riser behavior, Fundamentals of . . . 688-693
Riser dimensioning . . . 66
Riser equation . . . 687
Riser, Function of . . . 61, 133
Risering of gray iron castings . . . 131-141
Risers, Top-blind, on steel castings . . . 687, 688
Rockwell hardness test . . . 659

S

Safety, Plant, in shell molding . . . 18
Safety program, Positive benefits of a . . . 570-573
Sand coating characteristics . . . 198
Sand control . . . 39-41
Sand flowability . . . 156
- Effect of, on casting surface finish . . . 574-579

Sand for shell molding . . . 376
Sand mixture, All purpose . . . 197
Sand mixture, Factors involved in making a . . . 309-314
Sand, reclaimed, green properties of . . . 215
Sand, reclaimed, Surface of . . . 215
Sand reclamation:
- A wet method of . . . 206-210
- Dry, of molding sand for steel castings . . . 200-205
- Fundamental concept of . . . 192
- in a steel foundry . . . 211-221
- with the combination system . . . 222-227

Sand reconditioning . . . 193
Sand-resin mixtures, Tests for . . . 184
Sand toughness, STN . . . 163
Sands and resins for shell molding . . . 367-377
Sands, Non-ferrous molding . . . 432-438
Sand, Ramming of molding . . . 490-501
Sands, steel, Properties of, at elevated temperatures . 96-106
Scabbing tendency of sand mixtures . . . 104
Sclerescope hardness test . . . 659
Sections, Feeding range of joined . . . 302-308
Segregation in bronze, A study of, using a radioactive technique . . . 352-354
Segregation of non-metallics in centrifugal casting . . 708
Semi-centrifugal casting . . . 715
Semi-centrifugal casting defined . . . 702
Semi-permanent and permanent mold castings, Magnesium alloy . . . 502-509
Semi-synthetic sands . . . 433
Shell cast magnesium, Metallurgy of . . . 187
Shell core breakdown evaluation . . . 187
Shell molding:
- Applications and economics of . . . 23
- binders, Control tests for . . . 368
- Comparison of mechanical properties . . . 21, 22
- Cores in . . . 15, 16
- Dimensional tolerance in . . . 14
- Effect of base sand in . . . 21
- Effect of curing time in . . . 20
- Effect of shell thickness in . . . 19
- Equipment for . . . 9
- Evaluation of resins for . . . 8
- Gating and heading for . . . 12-14
- materials testing . . . 166
- Mechanics of . . . 9
- Mechanization in . . . 16, 17
- Method of casting production . . . 7-28
- Mold closing in . . . 15
- Parting agents used in . . . 9
- Pattern equipment for . . . 11-12, 595-599, 729-730
- Plant safety in . . . 18
- process . . . 1-3
- Raw materials used in . . . 8
- Resin-sand blending for . . . 11
- Resins and sands for . . . 367-377

Shell molds, Casting magnesium alloys in . . . 184-190
Shell molds, Permeability of . . . 14
Shell thickness, Effect of, in shell molding . . . 19
Shellac as a pattern coating . . . 580

Silica flour, Use of, in shell molding 8
Silicon carbide, Use of, in molding 436
Silicon content in malleable iron 110
Silicone as a parting agent in shell molding 9
Sillimanite-type refractories 735
"Skull" melting of titanium 678
Slag basicity in basic cupola operation 55
Slag composition and appearance in basic cupola operation 56
Slag control 379
Slag fluidity, Some causes and effects 534
Solidification:
at corner and core positions 451-456
curves explain feeding 610
factors related to risering 610
of cylinders 142-149
of steel 62
time of castings 611
Spalling of refractories 734
Sprue design for centrifugal casting 746
Sprue designs in vertical gating permanent molds . . . 82
Sprue-runner systems 441
Sprue size, Effect of, on turbulence 91
Straight sprues, Aspiration of air in 91
Standard data tables for core finishing 355-366
Standards, Conveyor molding 263-271
Standards, Use of, in controlling costs 457-466
Steel castings, Dry reclamation of molding sand for . 200-205
Steel foundry, Sand reclamation in a 211-221
Steel sands, Properties of, at elevated temperatures . 96-106
Steel, Solidification of 62
Strain theory of hot tearing 181
Stress due to uneven cooling 651
Stress in chilled structure 651
Stress relief annealing of gray iron 5
Stress relief heat treatment of gray cast iron . . . 646-650
Stresses, internal, Nature of 647
Stress-strain and expansion tests on sand mixtures . . 96
Strickle setting in pit mold 42
Superheat, Effect of 689
Surface finish, Effect of sand flowability on casting . 574-579
Surface of reclaimed sand 215
Surface smoothness of permanent mold magnesium castings 505
Synthetic sands 432

T

Temperature measurement 407
Temperature, Method of measuring 481
Tempering process 644
Thermal methods of sand reclamation 193, 194
Thermal studies 441
Thermodynamics in core drying 280
Thermodynamics of chemical reactions 112
Thorium, Magnesium sand casting containing . . . 315-326
Time keeping procedure 563
Time studies, taking 356
Titanium, A technique for casting 587-592
Titanium alloy types 679
Titanium-carbon alloys, Mechanical properties of cast 411-415
Titanium castings industry, Metallurgical bases for . 678-685
Titanium castings:
Evaluation of mold materials for 540-545
Mechanical properties of 683, 684
Mold materials for 679
Shrinkage characteristics of 683
Soundness of 682
Surface of 683
Titanium, Hardness of cast 679
Titanium sponge 588
Transverse strength test for sand-resin mixtures . . . 185
Turbulence, Effect of sprue size on 91
Twin structure, Mode of growth 671

U

Urea-formaldehyde resin used in shell molding . . . 8, 9

V

Veining tendencies of cores 150-155
Velocities and volume rates of metal flow in gating systems 439-450
Venting centrifugal casting molds 752
Vertical gating 81-95

W

Wage incentive for cost and production control for indirect operators 561-569
Water-cooled basic cupolas 58, 59
Watertown process of centrifugal casting 703
Wear tests 559
Wet method of sand reclamation 194, 206-210
White iron structure 124
Windsor Lock molding sand 435
Wood flour or cellulose materials, Use of, in molding . 436
Work analysis 356
Work units defined 562

X

X-ray unit, Flash 441

Z

Zinc, Effect of, on magnesium alloy ZK61 721
Zircon as a refractory lining 739
Zircon flour, Use of, in shell molding 8
Zirconium, Effect of, on magnesium alloy ZK61 . . . 719

AUTHOR INDEX

ADAMS, C. M., REYNOLDS, C. C. and TAYLOR, H. F.—Prediction of Mechanical Properties from Chemical Composition for Fully-Annealed Ductile Cast Iron . . . 510
ADAMS, JR., C. M. and TAYLOR, H. F.—Fundamentals of Riser Behavior . . . 686
ADAMS, C. M., WALTHER, W. D. and TAYLOR, H. F.—Effect of Casting "Fiber" on Mechanical Properties of Aluminum-4 Per Cent Copper Alloys . . . 664
AFS Committee Reports—
Veining Tendencies of Cores . . . 150
Report of AFS Flowability Committee . . . 156
Deformation of Green Molding Sand . . . 159
Progress Report of Shell Molding Materials Testing Committee . . . 166
AFS Research Reports—
The Study of Vertical Gating . . . 81
Twelfth Annual Report on the Investigation of Properties of Steel Sands at Elevated Temperatures . . . 96
Factors of Melting Furnace Atmosphere on Casting Properties and Annealability of Malleable Iron . . . 107
Risering of Gray Iron Castings . . . 131
Solidification of Cylinders . . . 142
AMES, B. N.—A Survey of the Shell Molding Method of Casting Production . . . 7
ANDERSON, R. J. —Mechanics of Foundry Mechanization and Improved Methods . . . 272
ARMSTRONG, A. A.—Hardening Gray Cast Iron with Minimum Distortion . . . 654
ARNOLD, S. V., MALONE, R. F., CLARK, H. T., and FINLAY, W. L.—Preliminary Survey of Some Metallurgical Bases for a Titanium Castings Industry . . . 678

BAKER, D. F. and WILLIAMS, D. C.—Factors Involved in Making a Sand Mixture . . . 309
BARLOW, T. E. and HUMONT, P. D.—Gun Placed Silica Cupola Lining . . . 378
BERG, C. J.—Research in Pattern Coating Produces Quality Castings . . . 580
BISHOP, H. F., BRANDT, F. A. and PELLINI, W. S.—Solidification at Corner and Core Positions . . . 451
BISHOP, H. F., JOHNSON, W. H. and PELLINI, W. S.—Velocities and Volume Rates of Metal Flow in Gating Systems . . . 439
BISHOP, H. F., MYSKOWSKI, E. T. and PELLINI, W. S.—Feeding Range of Joined Sections . . . 302
BOCK, W. K., SCHWARTZ, H. A. and HEDBERG, J. D.—Graphitization in the Malleable Iron Industry . . . 228
BOLT, J. E. and JEFFERY, W. C.—Phosphorus Removal in Basic Cupola Operation . . . 546
BRANDT, F. A., BISHOP, H. F. and PELLINI, W. S.—Solidification at Corner and Core Positions . . . 451
BRANDT, F. A., PELLINI, W. S. and LAYNE, E. E.—Performance of Cast and Rolled Steels in Relation to the Problem of Brittle Fracture . . . 243
BRYCE, J. T., HERNANDEZ, A. and ROTE, F. B.—Influence of Furnace Atmosphere on the Annealing of Malleable Iron . . . 291
BUEG, O. C.—Pattern Equipment for Shell Molding . . . 729
BUSBY, WM.—Use of Standards in Controlling Costs . . . 457

CANNON, J. A.—Dry Reclamation of Molding Sand for Steel Castings . . . 200
CAPLAN, K. J.—Trends in Dust Control—Past, Present and Future . . . 394
CARLSON, R. and CARTER, S. F.—Acid Cupola Slags and Some Relationships with Melting Conditions . . . 527
CARTER, S. F.—American Experiences with Basic Cupola Melting . . . 52
CARTER, S. F. and CARLSON, R.—Acid Cupola Slags and Some Relationships with Melting Conditions . . . 527
CLARK, H. T., MALONE, R. F., ARNOLD, S. V. and FINLAY, W. L.—Preliminary Survey of Some Metallurgical Bases for a Titanium Castings Industry . . . 678
CONGER, G. A.—Progress Report of Shell Molding Materials Testing Committee . . . 166
CURTIS, G. H.—Sand Reclamation with the Combination System . . . 222

DEMMLER, A. W.—Annealing Iron Castings for Machinability . . . 651
DIETERT, H. W. and GRAHAM, A. L.—Ramming of Molding Sands . . . 490
DIETL, M. G.—Refractories for Indirect-Arc Electric Furnace for Non-Ferrous Melting . . . 733
DRUMM, M. F.—Resins and Sands for Shell Molding . . . 367

EASH, J. T. and LEE, G. L.—Properties of Graphitic Nickel Alloy Castings . . . 552
EDELMAN, R. E. and SIMMONS, O. W.—Mechanical Properties of Cast Titanium-Carbon Alloys . . . 411

FAIRFIELD, H. H. and MCCONACHIE, J.—Effect of Sand Flowability on Casting Surface Finish . . . 574
FINLAY, W. L., MALONE, R. F., CLARK, H. T. and ARNOLD, S. V.—Preliminary Survey of Some Metallurgical Bases for a Titanium Castings Industry . . . 678
FISHER, G. A., JOHNSON, H. H. and MCCLEERY, R. Y.—Sand Reclamation in a Steel Foundry . . . 211
FISCHER, R. B.—Some Factors Affecting Spherical Type Macro-Gas Porosity in 55-5-5-5 Bronze . . . 327
FLOWABILITY COMMITTEE—Report of AFS Flowability Committee . . . 156

GANTZ, JR., M. E., GINGERICH, E. M. and WOODS, R. T.—Magnesium Alloy Permanent Mold and Semi-Permanent Mold Castings 502
GINGERICH, E. M., GANTZ, JR., M. E. and WOODS, R. T.—Magnesium Alloy Permanent Mold and Semi-Permanent Mold Castings 502
GRAHAM, A. L. and DIETERT, H. W.—Ramming of Molding Sands . . . 490
GRUBE, K., KURA, J. G. and JACKSON, J. H.—The Study of Vertical Gating . . . 81

HATFIELD, M. R., WILDE, R. J. and NOLAN, V. J.—Carbon As A Refractory . . . 640
HEDBERG, J. D., SCHWARTZ, H. A. and BOCK, W. K.—Graphitization in the Malleable Iron Industry . . . 228
HEINE, R. W. and LANGE, E. A.—Effects of Melting Furnace Atmosphere on Casting Properties and Annealability of Malleable Iron . . . 107
HEINE, R. W. and WEART, H. W.—Effects of Magnesium on Eutectic Modification and Graphitization of Certain Fe-C Alloys . 674
HERNANDEZ, A., BRYCE, J. T. and ROTE, F. B.—Influence of Furnace Atmosphere on the Annealing of Malleable Iron . . . 291
HUMONT, P. D. and BARLOW, T. E.—Gun Placed Silica Cupola Lining . . . 378

JACKSON, J. H., GRUBE, K. and KURA, J. G.—The Study of Vertical Gating . . . 81
JACKSON, J. H., KURA. J. G. and LANG, R. M.—Evaluation of Mold Materials for Titanium Castings . . . 540
JACOBS, F. W. and LAWRENCE, J. S.—Correlation of Air Furnace Bottom Temperature to Refractory and Operating Practice in a Cupola-Air Furnace Duplex System . . . 479
JEFFERY, W. C. and BOLT, J. E.—Phosphorus Removal in Basic Cupola Operation . . . 546
JOHNSON, H. H., MCCLEERY, R. Y. and FISHER, G. A.—Sand Reclamation in a Steel Foundry . . . 211
JOHNSON, W. H., BISHOP, R. Y. and PELLINI, W. S.—Velocities and Volume Rates of Metal Flow in Gating Systems . . . 439

KANE, J. M.—Air Pollution and Public Relations . . . 662
KROPKA, J. G.—Multiple Molding in a Malleable Shop . . . 628
KRYNITSKY, A. I.—Progress Made in Fluidity Testing of Molten Metals During the Last Ten Years . . . 399
KURA, J. G., GRUBE, K. and JACKSON, J. H.—The Study of Vertical Gating . . . 81
KURA, J. G., LANG, R. M. and JACKSON, J. H.—Evaluation of Mold Materials for Titanium Castings . . . 540
KYLE, P. E., THORPE, R. G., ROBINSON, H. and WISNIEWSKI, P. J.—Twelfth Annual Report on Investigation of Properties of Steel Sands at Elevated Temperatures . . . 96

LABELLE, J. E.—Fundamentals of Heat Treatment of Gray Cast Iron . . . 643
LANG, R. M., KURA, J. G. and JACKSON, J. H.—Evaluation of Mold Materials for Titanium Castings . . . 540
LANGE, E. A. and HEINE, R. W.—Effects of Melting Furnace Atmosphere on Casting Properties and Annealability of Malleable Iron . . . 107
LAWRENCE, J. S. and JACOBS, F. W.—Correlation of Air Furnace Bottom Temperature to Refractory and Operating Practice in a Cupola-Air Furnace Duplex System . . . 479
LAYNE, E. E., PELLINI, W. S. and BRANDT, F. A.—Performance of Cast and Rolled Steels in Relation to the Problem of Brittle Fracture . . . 243
LEE, G. L. and EASH, J. T.—Properties of Graphitic Nickel Alloy Castings . . . 552
LEWIS, R. T.—Are Your Costs Reliable? . . . 523
LIBSCH, J. F., ROWADY, E. P. and MURPHY, W. J.—Hardening Characteristics of Induction Heated Ductile Iron . . . 422
LIPSON, S. and ROSENTHAL, H.—Agar Gels in Plaster-Bonded Investment . . . 347
LORIA, E. A.—Trends for the Relation of Chill Test Depth and Carbon Equivalent of Gray Cast Irons . . . 387
LUCAS, L. L.—Practical Foundry Applications of Principles of Ratio Gating . . . 623

MALONE, R. F., CLARK, H. T., ARNOLD, S. V. and FINLAY, W. L.—Preliminary Survey of Some Metallurgical Bases for a Titanium Castings Industry . . . 678
MAREK, C. T. and WIMBERT, R. J.—Induced Flow Process of Baking Oil-Bonded Cores . . . 279
MARTIN, L. L.—Developing Standard Data Tables for Core Finishing . . . 355
MCCLEERY, R. Y., JOHNSON, H. H. and FISHER, G. A.—Sand Reclamation in a Steel Foundry . . . 211
MCCONACHIE, J. and FAIRFIELD, H. H.—Effect of Sand Flowability on Casting Surface Finish . . . 574
MCCURDY, H. R. and SIMMONS, O. W.—A Technique for Casting Titanium . . . 587
MCDONALD, F. J.—Gating to Control Pouring Rate and Its Effect on the Casting . . . 416
MCDONNELL, R. W.—A Simplified Cost System for Small Jobbing Foundries . . . 600
MEIER, J. W.—Characteristics of High Strength Magnesium Casting Alloy ZK61 . . . 719
MEIER, J. W. and RYLSKI, O. Z.—Centrifugal Casting of Aluminum Alloys . . . 744
MEYER, H. W.—Reclamation of Sand by Pneumatic Dry Scrubbing . . . 196
MIDDLETON, J. M.—The Influence of Molding Materials on the Incidence of Hot Tearing . . . 167
MURPHY, W. J., ROWADY, E. P. and LIBSCH, J. F.—Hardening Characteristics of Induction Heated Ductile Iron . . . 422
MYSKOWSKI, E. T., BISHOP, H. F. and PELLINI, W. S.—Feeding Range of Joined Sections . . . 302

NELSON, K. E.— Magnesium Sand Casting Alloys Containing Thorium . . . 315
NICHOLLS, G. W.—Efficient Methods of Production of Machine Tool Castings . . . 29
NOLAN, V. J., WILDE, T. J. and HATFIELD, M. R.—Carbon As A Refractory . . . 640

OLSON, R.—Patterns for Shell Molding . . . 595

PARKER, W. G.—Deformation of Green Molding Sand . . . 159
PASCHKIS, V.—Solidification of Cylinders . . . 142
PELLINI, W. S.—Factors Which Determine Riser Adequacy and Feeding Range . . . 61
PELLINI, W. S.—Practical Heat Transfer—An Interpretive Report . . . 603
PELLINI, W. S., BRANDT, F. A. and BISHOP, H. F.—Solidification At Corner and Core Positions . . . 451
PELLINI, W. S., BRANDT, F. A. and LAYNE, E. E.—Performance of Cast and Rolled Steels in Relation to the Problem of Brittle Fracture . . . 243
PELLINI, W. S., JOHNSON, W. H. and BISHOP, H. F.—Velocities and Volume Rates of Metal Flow in Gating Systems . . . 439
PELLINI, W. S., MYSKOWSKI, E. T. and BISHOP, H. F.—Feeding Range of Joined Sections . . . 302
PRING, R. T.—Air Pollution Control Equipment for Melting Operations in the Foundry Industry . . . 467
PRUET, C. J.—Wage Incentive for Cost and Production Control for Indirect Operators . . . 561

REHDER, J. E.—Notes on Air Furnace Design and Operation . . . 516
REID, E. C.—Conveyor Molding Standards . . . 263
REYNOLDS, C. C., ADAMS, C. M. and TAYLOR, H. F.—Prediction of Mechanical Properties from Chemical Composition for Fully-Annealed Ductile Cast Iron . . . 510
ROBECK, C. A. and SANDERS, C. A.—A General Look at Non-Ferrous Molding Sands . . . 432
ROBINSON, H., THORPE, R. G., WISNIEWSKI, P. J. and KYLE, P. E.—Twelfth Annual Report on Investigation of Properties of Steel Sands at Elevated Temperatures . . . 96
ROSENTHAL, H. and LIPSON, S.—Agar Gels in Plaster-Bonded Investment . . . 347
ROTE, F. B., BRYCE, J. T. and HERNANDEZ, A.—Influence of Furnace Atmosphere on the Annealing of Malleable Iron . . . 291
ROWADY, E. P., MURPHY, W. J. and LIBSCH, J. F.—Hardening Characteristics of Induction Heated Ductile Iron . . . 422
RYLSKI, O. Z. and MEIER, J. W.—Centrifugal Casting of Aluminum Alloys . . . 744

SANDERS, C. A. and ROBECK, C. A.—A General Look at Non-Ferrous Molding Sands . . . 432
SCHAUM, J. H.—Stress Relief Heat Treatment of Gray Cast Iron . . . 646
SCHMIDT, W. A. and TAYLOR, H. F.—Risering of Gray Iron Castings . . . 131
SCHWARTZ, H. A., BOCK, W. K. and HEDBERG, J. D.—Graphitization in the Malleable Iron Industry . . . 228
SCOTT, M. R.—Flame Hardening of Cast Iron . . . 657
SEMRAU, C. F.—Construction and Operation of Coal-Fired Air Furnaces . . . 634
SHEPTAK, N.—Casting Magnesium Alloys in Shell Molds . . . 184
SHIPLEY, F. W.—Positive Benefits of a Safety Program . . . 570
SHURMER, R. H. and WILL, P. C.—A Wet Method of Sand Reclamation . . . 206
SIMMONS, O. W. and EDELMAN, R. E.—Mechanical Properties of Cast Titanium-Carbon Alloys . . . 411
SIMMONS, O. W. and MCCURDY, H. R.—A Technique for Casting Titanium . . . 587
SMITH, J. H.—Outstanding Opportunities for the Foundry Industry . . . 1
SMITH, K. M.—Trends in Cupola Dust Collecting Systems . . . 731
SPENCER, C. C.—Advantages of Insulating an Acid Electric Furnace Roof . . . 593

TAYLOR, H. F. and ADAMS, JR., C. M.—Fundamentals of Riser Behavior . . . 686
TAYLOR, H. F. and SCHMIDT, W. A.—Risering of Gray Iron Castings . . . 131

TAYLOR, H. F., REYNOLDS, C. C. and ADAMS, C. M.—Prediction of Mechanical Properties from Chemical Composition for Fully-Annealed Ductile Cast Iron 510
TAYLOR, H. F., WALTHER, W. D and ADAMS, C. M.—Effect of Casting "Fiber" on Mechanical Properties of Aluminum-4 Per Cent Copper Alloys 664
THORPE, R. G., ROBINSON, H., WISNIEWSKI, P. J. and KYLE, P. E.—Twelfth Annual Report on Investigation of Properties of Steel Sands at Elevated Temperatures 96
WALLACE, J. F.—Engineering Aspects of Centrifugal Casting 701
WALTHER, W. D., ADAMS, C. M. and TAYLOR, H. F.—Effect of Casting "Fiber" on Mechanical Properties of Aluminum-4 Per Cent Copper Alloys 664
WALWORTH, H. T.—Foundry Noise and Its Control 694
WEART, H. W. and HEINE, R. W.—Effects of Magnesium on Eutectic Modification and Graphitization of Certain Fe-C Alloys . 674
WENNINGER, C. E.—Development of Foundry Sand Reclamation 191
WILDE, T. J., HATFIELD, M. R. and NOLAN, V. J.—Carbon As A Refractory 640
WILL, P. C. and SHURMER, R. H.—A Wet Method of Sand Reclamation 206
WILLIAMS, D. C. and BAKER, D. F.—Factors Involved in Making a Sand Mixture 309
WIMMERT, R. J. and MAREK, C. T.—Induced Flow Process of Baking Oil-Bonded Cores 279
WINEGARD, W. C.—A Study of Segregation in Bronze Using A Radioactive Technique 352
WISNIEWSKI, P. J., THORPE, R. G., ROBINSON, H. and KYLE, P. E.—Twelfth Annual Report on Investigation of Properties of Steel Sands at Elevated Temperatures 96
WOODS, R. T., GANTZ, JR., M. E. and GINGERICH, E. M.—Magnesium Alloy Permanent Mold and Semi-Permanent Mold Castings 502